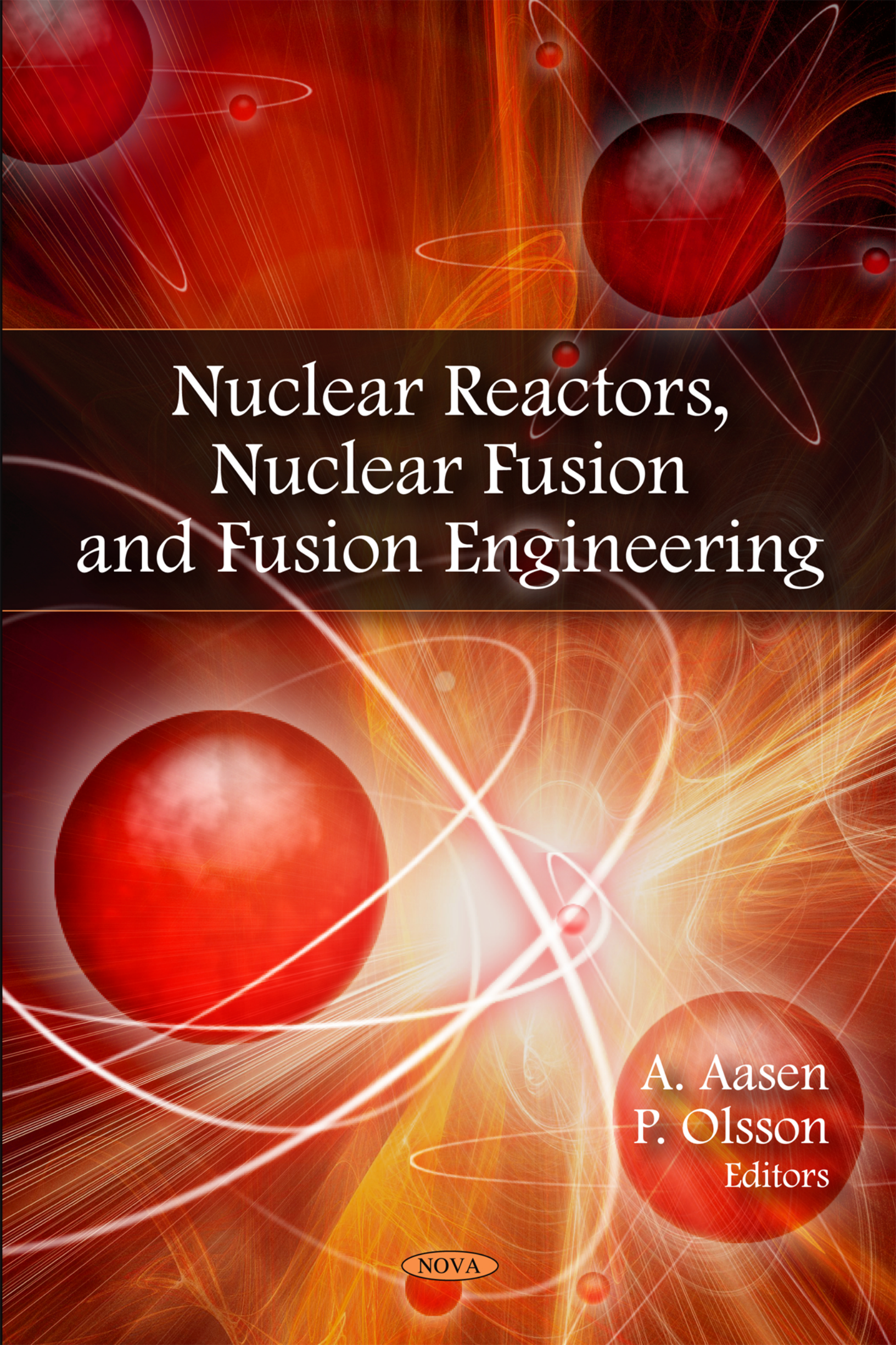




Nuclear Reactors, Nuclear Fusion and Fusion Engineering

A. Aasen
P. Olsson
Editors

NOVA

NUCLEAR REACTORS, NUCLEAR FUSION AND FUSION ENGINEERING

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**A. AASEN
AND
P. OLSSON
EDITORS**

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PREFACE

Nuclear reactors play a key role in 21st Century energy production. This new book provides critical research in both fission and fusion energy production as well as the technology of the reactors. The role of nuclear data in Accelerator Driven Systems in order to reduce the cost for reaching a certain level of safety is presented and a detailed discussion of turbulent mixing and void drift that includes state-of-the-art models is given. Motivation for construction of the fusion reactors, including the laser fusion facilities and other related problems, are addressed. A brief history of magnetic confinement fusion power plant conceptual designs, focusing on tokamaks, is outlined in this book. Furthermore, the progress and state-of-the-art of principal aspects of fusion safety and environment are discussed. Since a high quality of vacuum integrity is required in large tokamak machines, leak detection systems are overviewed and a reasonable leak detection strategy is proposed.

One of the outstanding new developments in the field of Partitioning and Transmutation (P&T) concerns Accelerator-Driven Systems (ADS), discussed in Chapter 1, which consist of a combination of a high-power, high-energy accelerator, a spallation target for neutron production, and a sub-critical reactor core.

The development of the commercial critical reactors of today motivated a large effort on nuclear data up to about 20 MeV, and presently several million data points can be found in various data libraries. Accelerator-driven transmutation will make use of neutrons up to GeV energies. Although only a minor fraction of the neutrons will have such high energies, they nevertheless need to be well characterized. At these high energies, data are scarce or even non-existent.

The nuclear data needed for transmutation in an ADS can roughly be divided into two main areas. First, the initial proton beam produces neutrons via spallation reactions. This means that data on proton-induced neutron production is needed. In addition, data on other reactions are needed to assess the residual radioactivity of the target. Second, the produced neutrons can induce a wide range of nuclear reactions, and knowledge of these is useful in the design of an ADS.

In most cases, direct data determination is not the ultimate goal. The global capacity for such measurements is insufficient to obtain complete coverage of important data. It is even impossible in theory to supply all relevant data. This means that the experimental work must be focused on providing benchmark data for theory development, making it possible to use theoretical models for unmeasured parameters in a core environment. In this respect, accelerator-driven systems are not fundamentally different than critical reactors.

An often overlooked aspect is why nuclear data should be measured in the first place. Nuclear data are not needed for a demonstration of the principle of driving a sub-critical assembly with an external neutron source. The need for nuclear data becomes imminent when a realistic large-scale facility is the goal. With large uncertainties in the nuclear data, large safety margins have to be used, which results in excessive costs. Thus, the role of nuclear data is to reduce the cost for reaching a certain level of safety.

Another important aspect is the trade-off between general and particular information. Below 20 MeV, a single cross section can be of paramount importance to the entire application. Moreover, some cross sections are fundamentally inaccessible to theory, in particular in the resonance region. As a result, at low energies more or less complete data coverage for major elements is required. Above 20 MeV, the situation is fundamentally different. The cross sections are smooth, and the behaviour of the total technical system is always dictated by the sum of a large number of reactions, neither of which strongly dominates the performance. Therefore, getting a grip on the overall picture is more important than precision data on a single reaction.

If the boiling flow through fuel assemblies in a reactor core is to be predicted numerically by means of a sub-channel code, two important lateral exchange processes between neighboring sub-channels have to be taken into account: *turbulent mixing* and *void drift*. Whereas mixing is a kind of turbulent gradient diffusion occurring in both single-phase and two-phase flow, void drift is a two-phase phenomenon which is physically not yet well understood. However, there are a lot of phenomenological attempts to model this superimposed effect, which can act in the same direction as turbulent mixing, but also contrarily depending on sub-channel geometries and flow conditions.

Chapter 2 will classify the physical background of both phenomena including a detailed overview about the flow conditions which have to be existent in order to cause the one or other phenomenon. Furthermore, it will provide a well-structured thread through the whole calculation methodology. Thereby, each single phenomenon will be discussed in detail (physically and mathematically) and an overview about both the particular state-of-the-art models and new approaches in open literature will be given.

In the introduction of Chapter 3 we present the motivation for construction of the fusion reactors including the laser fusion facilities. Then, we consider the solution of the Dirac equation with the periodic potential which is called the Volkov solution. The one-photon and two-photon Compton process follows from this solution. The energy-momentum equation for the multi-photon process is also involved. In the following section, we solve the problem of the interaction of an electron with the Dirac delta-form impulsive force, which is an idealization of experimental situation in laser physics. We elaborate the quantum theory of the interaction of a charged particle with a such impulsive force. We determine the modified Compton formula for final frequency of photons generated by the scattering of the delta-form laser pulse on the electron in a rest. The one-photon Compton process is only special case of the multi-photon interaction of electron with N photons of laser pulse. The multi-photon interaction is nonlinear and differs from the situation where electron scatters twice or more as it traverses the laser focus.

The next problem of the laser physics is the electron interaction with the two electromagnetic waves. This is also the future direction of the laser physics of elementary particles. The two laser beams can be used in the thermonuclear reactor instead of many laser beams.

In the following text we consider the solution with massive photons in the laser beam. The mass of photon is of the dynamical origin corresponding to the radiative corrections. We determine the equation corresponding to the Dirac equation with periodic potential of massive photons. The resulting equation is the Riccati equation which cannot be solved in general. So, we derive only some approximative formulas. While massless photon is described by the Maxwell Lagrangian, the massive photon is described by the Proca Lagrangian from which the field equations follow. The massive electrodynamics can be considered as a generalization of massless electrodynamics.

Massive photons are substantial in the theory of superconductivity, plasma physics, waveguides and so on. The massive photons can be produced in very strong magnetic pulses generated by the Kapitza gigantic solenoid method, or, by the Terletzkii method of the cumulation of the magnetic flux. It means that the deuterium-tritium pellet in the laser ignition facility can be compressed by the massive photons generated simultaneously with the magnetic delta-form pulses.

The presence of the magnetic field in which the pellet can be situated leads to the generation of the synchrotron radiation of charged particles. Motivated by this fact, we derive the synchrotron radiation formulas using the Volkov solution of the Dirac equation and the S-matrix formalism of QED for an electron moving in the constant magnetic field.

As explained in Chapter 4, the Advanced Fuel Cycle Initiative is responsible for the development of advanced nuclear energy systems. One of these nuclear energy systems is the Sodium Fast Reactor (SFR). To maximize the performance of this type of nuclear reactor, it will be important to improve on the performance of the nuclear fuel, i.e., allow for higher fuel burnup and/or operation of the fuel at higher reactor operating temperatures. One type of nuclear fuel currently being evaluated is a metallic U-Pu-Zr alloy, and one phenomenon that could limit the ability of the fuel to perform at temperatures that are higher than what have been typically employed is the chemical compatibility of the fuel and cladding. During irradiation, the fuel swells and eventually contacts the cladding, at which time fuel cladding chemical interaction (FCCI) can occur. During this process, interdiffusion occurs between the fuel, cladding, and fission products, which can result in the formation of interaction zones on the inner surface of the cladding that can become brittle or may contain relatively low-melting phases. The result of this process can be cracking and failure of the cladding. Minimal detailed information on FCCI in irradiated metallic SFR fuels is available in the literature. Thus, in order to facilitate an increased understanding of FCCI, this chapter describes results that have been generated from the destructive examinations of individual fuel elements that were irradiated in the Experimental Breeder Reactor-II over the course of a thirty-year timeframe. This chapter particularly focuses on any interaction zones that developed on the inner surface of the cladding. Three examination techniques were employed to characterize FCCI in these fuel elements: optical metallography, electron probe micro-analysis, and scanning electron microscopy. The results of these examinations were evaluated and compared to provide information about FCCI and what effects it could have on fuel performance. Fuel elements that included U-Zr or U-Pu-Zr alloy fuels and HT-9, D9, or Type 316 stainless steel cladding were assessed. The irradiation conditions, cladding type, and axial location on fuel elements, where the thickest layers could be expected to develop, were identified, and it was found that the largest interaction zones developed at the combined high-power and high-temperature regions of the fuel elements and for the fuel elements with U-Pu-

Zr alloy fuel and D9 stainless steel cladding. The most prevalent, non-cladding constituent observed in the developed interaction layers were the lanthanide fission products.

Microalloying technology is widely used in the steel industry to improve the mechanical properties of structural steels, in which the strength and toughness are improved by the refinement of ferrite grain size. Chapter 5 explains the importance of microalloying elements in controlling the microstructures and properties of quenched and tempered (QT) RPV steels. First, we give a brief description of the physical metallurgy of prior austenite grain refinement in microalloyed steels. The critical microalloying element for prior austenite grain size control is shown to be titanium, which forms carbonitrides, able to pin prior austenite grain boundaries during heat treatment. Secondly, attention is drawn to experimental results from the literature that demonstrate prior austenite grain coarsening in simulated coarse-grained heat-affected-zones (CGHAZs) in certain grades of non-microalloyed RPV steel. Finally, we discuss the microstructures and mechanical properties of simulated heat-affected-zones (HAZs) in A508 and A533 steels.

Chapter 6 provides a brief history of magnetic confinement fusion power plant conceptual designs, beginning with the early development in 1970, focusing on tokamaks. In addition, the evolution of six more magnetic concepts (stellarator, spherical tori, field-reversed configurations, reversed-field pinches, spheromaks, and tandem mirrors) is highlighted. The key issues encountered are discussed, including the technological obstacles and the elements necessary for economic competitiveness. Extensive R&D programs and international collaboration in all areas of fusion research led to a wealth of information generated and analyzed. As a result, fusion promises to be a major part of the energy mix in the 21st century and beyond.

In Chapter 7, higher power LH wave (1.5MW) is injected into the diverted plasma with a slightly asymmetric spectrum. Dominant electron heating and current profile control are investigated with numerical simulation. Plasma heating by electron Landau interaction results in operation scenarios of preferentially dominant electron heating. Due to the off-axis driven current, an optimized q -profile is formed, and an enhanced confinement regime with steep electron temperature gradient is produced. The clear decrease of the electron thermal conductivity in the LH power deposition region shows that an electron-ITB is developed. Establishment of the current profile like in the hybrid scenario is studied under the condition of dominant electron heating in HL-2A. The scenarios with injecting LH and EC waves are under numerical study. Carefully adjusting the position of non-inductive current driven by two groups of gyrotron, an optimized q -profile was obtained with $q_a = 3.78$ and weak shear region extending to $\rho \sim 0.45$ (where ρ is the square-root of toroidal flux normalized to its value at plasma boundary) in low-density discharges of $\bar{n}_e = 1.0 \times 10^{19} m^{-3}$. When 0.5 MW LH power in the current drive mode and 0.95MW EC power mainly for plasma heating are used to control the current profile, a hybrid discharge scenario with weak magnetic shear region extended to $\rho = 0.6$ and $q_a = 3.21$ is established through controlling the EC absorption position. The mechanism of the LH wave absorption in the HL-2A plasma causes interplay of the distribution of the LH driven current with the modification of the plasma configuration, which constitutes non-linearity in the LH wave deposition. Due to the non-linearity the LH wave deposition position changes spontaneously or oscillates. The oscillatory behavior caused by non-linear effect of the LH wave deposition is analyzed.

In Chapter 8, recent years the optical opacity effects in impurity seeded plasmas in fusion devices are discussed. In many practically interesting situations plasmas are far from coronal as well as from local thermo-dynamical equilibria. Impurities in clouds, surrounding diagnostic pellets, in noble gas jets injecting into tokamak etc., are transparent for some lines and opaque for others. Hence, accurate simulations of thermal balance, impurity dynamics and so on, are extremely cumbersome. At the same time the influence of plasma optical opacity on plasma parameters may be very important. Reduced models for impurity description with taking into account opacity effects were developed. Coincidence of some theoretical and experimental results has been achieved taking into account opacity effects with the models mentioned above. The present paper is the review (may be, not complete) of results obtained by some theoretic teams in last few years. Carbon pellets, noble gas jets and disruptions are discussed.

Electricity generating plants powered by nuclear fusion have long been envisioned as possessing inherent advantages for the health and safety of the public, the health and safety of plant workers, and good stewardship of the environment while supporting modern society. Chapter 9 discusses the progress and state-of-the-art of these three principal aspects of fusion safety and environment. The fusion safety philosophy and advantages over traditional thermal power plants are described. Fusion workers should be protected commensurately with workers in other comparable industrial activities. The fusion radwaste management strategy must accommodate the new trend of recycling and clearance, avoiding geological disposal. Here, we discuss the technical elements as well as the US regulatory approach and policy governing the design of safe and environmentally sound fusion devices.

In large tokamak machines such as the TFTR, the JET and the JT-60, a high quality of vacuum integrity is required to achieve impurity-free plasma, so the vacuum leak detection technology is highly important. A combination of a helium (^4He) spraying and a ^4He leak detector is a conventional leak detection method due to its higher detectability, reliability and easier maintainability. However, such tokamak machines are huge, complicated, and inaccessible, so the conventional method is not applicable in many cases. Special leak detection methods were therefore developed; for instance, a sensitivity of the ^4He sniffer method was improved five decades in JT-60, and a lot of experiences on leak events, pinpointing and repairs were accumulated in individual machines. Through these tokamak operations, the nuclear fusion research has considerably advanced, which created the next step for international collaboration — the ITER project. Cadarache in France was eventually selected for the ITER construction site and now, the ITER organization comprising the seven parties is effective for executing the scheduled missions. In Chapter 10, the ITER leak detection system is given an overview, and a reasonable leak detection strategy is proposed, based on the methods and concepts developed so far. To support the strategy, the essential leak detection technologies are exhibited. The high-resolution quadrupole mass spectrometer (HR-QMS) using the zone II condition in the Mathieu diagram has been developed for the mass spectrometer leak detector (MSLD) for torus, where the 10^{-4} peak ratio of $^4\text{He}^+/\text{D}_2^+$ could be obtained with the 3.58 MHz prototype. An improved method with fully circulating the water has been demonstrated, in which krypton (Kr) was dissolved in water. For 10^{-3} Pa·m³/s of two water leaks, the positional accuracy of 12.6 % was obtained, based on the natural convection flow calculation. Another valuable method with fully circulating the water has been developed, namely, a total pressure gauge or the QMS head was mechanically scanned around the suspected area by using a vacuum compatible manipulator. A

unidirectional total pressure gauge could detect the minimum 10^{-8} Pa·m³/s level of air leaks at background pressures of 10^{-5} Pa. A special QMS head equipped with a vacuum-tight rf tuning package operable at more than 150 °C was moved around the argon (Ar) leak nozzle, and could detect the Ar leak of 1.9×10^{-5} Pa·m³/s. A noticeable progress has been made in the leak detection technology as mentioned above, and they will be experimentally tested in the ITER.

Chapter 11 proposes the D-³He spherical tokamak (ST) reactor with the major radius of $R_0=5.6$ m, the minor radius of $a=3.4$ m, the aspect ratio of $A=1.65$, the elongation of $\kappa=3$, and the toroidal field of $B_{t0}=4.4$ T with the beta value of $\langle\beta_t\rangle=40$ %. The large plasma current of ~ 90 MA is ramped up by the external heating (200~300 MW) and fusion product heating power, and the vertical field effect. This large plasma current provides the wider operational regime. The required confinement enhancement factor over IPB98(y,2) scaling is 1.8~2.5 for the density profile of $\alpha_n=0.5$ and parabolic temperature profiles ($\alpha_T=1.0$). The minimum neutron power is as low as 52.5 MW for fuel ratio D : ³He = 0.40 : 0.60, if the particle to energy confinement time ratio of 2 by the powerful pumping and the large wall reflectivity $R_{\text{eff}}=0.99$ are achieved without spin polarization. The bremsstrahlung and synchrotron radiation losses to the first wall (heat flux $\sim 1\text{MW/m}^2$) could be converted to an electric power of 1 GW through the super-critical pressurized water coolant or the organic coolant together with the divertor heat load. New operation scenario of "quasi-continuous cyclic DC operation" is proposed for steady state electric power output in a D-³He ST reactor when the bootstrap current fraction is less than 100 %.

Global warming and energy crises are demanding new directions in sustainable engineering. Academia needs to summarily respond with enhanced curricula that will better prepare students for the changing world. While perspectives are shifting, only a limited number of American programs have nuclear-related components. Due to financial constraints, a new engineering department is often unattainable. Often a new course concentration may not be achievable due to student population or institutional interest. At a minimum, a single introductory course would benefit the modern education of all engineering majors. This technical elective can also provide multi-disciplinary team experiences which may be lacking due to ever-decreasing degree hour requirements.

A nuclear-related course will comply with common university goals by enhancing engineering curricula, providing more qualified non-nuclear engineers for the nuclear industry, and improving faculty teaching competencies. The only required cost is faculty time; enough interest exists in academia that at least one faculty from each technical community will be interested. The resulting nuclear training is an educational benefit for both students and the instructor. The appropriate lecturer needs little experience with nuclear systems in particular but does need a broad-based view of complex engineered systems. In fact, the examination of a new technical field may have the added benefit of research stimulation. The development of nuclear engineering education will also improve student recruitment and industrial collaboration. However, participation from the nuclear industry may be a challenge due to confidentiality and security. If possible, nuclear plant site tours can contribute to the educational experience by allowing students to see full-scale systems in a practical application.

As an example, the innovative initiation of nuclear technical education at the University of Mississippi is detailed. Nuclear power generation will be employed as the model

application of interdisciplinary systems engineering. Promoting technical appreciation rather than apprehension of nuclear technology, the proposed course relates nuclear systems engineering, safe reactor design, infrastructure sustainability, and environmental management. Including a sample outline, course development, assessment, dissemination, and sustainability plans are discussed in the first Short Communication, from the viewpoint of a professor with nuclear industrial experience.

Development of foam capsule fabrication for cryogenically cooled fuel targets is overviewed in the second Short Communication. The fabrication development was initiated as a part of the Fast Ignition Realization Experiment (FIREX) Project at the ILE, Osaka University in the way of bilateral collaboration between Osaka University and National Institute for Fusion Science (NIFS). For the first stage of FIREX (FIREX-1), a foam cryogenic target was designed where low-density foam shells with a conical light guide will be cooled down to the cryogenic temperature and will be fueled through a narrow pipe. The required diameter and thickness of the capsule are 500 μm and 20 μm , respectively. The material should be low-density plastics foam. We have prepared such capsules using 1) new material of (phloroglucinolcarboxylic acid)/formalin resin to control kinematic viscosity of the precursor, 2) phase-transfer-catalyzed gelation process to keep density matching of three phases of the emulsion. 3) non-volatile silicone oil as outer oil of emulsion in order to prevent hazard halogenated hydrocarbon and flammable mineral oil. The obtained foam capsule had fine structure of 180 nm (outer surface) to 220 nm (inner surface) and uniform thickness reaching to resolution limit of optical analysis ($\sim 0.5 \mu\text{m}$). A small hole was made before the solvent exchange and the drying process to prevent distortion due to volume changes. The density of dried foam was 0.29 g/cm^3 . After attaching the petawatt laser guiding cone and fueling glass tube, poly([2,2]paracyclophane) was coated on the foam surface and supplied for a fueling test of cryogenic hydrogen.

RESEARCH AND REVIEW STUDIES

Chapter 1

NEUTRON PHYSICS RESEARCH FOR THE DEVELOPMENT OF ACCELERATOR-DRIVEN SYSTEMS

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Abstract

One of the outstanding new developments in the field of Partitioning and Transmutation (P&T) concerns Accelerator-Driven Systems (ADS), which consist of a combination of a high-power, high-energy accelerator, a spallation target for neutron production, and a sub-critical reactor core.

The development of the commercial critical reactors of today motivated a large effort on nuclear data up to about 20 MeV, and presently several million data points can be found in various data libraries. Accelerator-driven transmutation will make use of neutrons up to GeV energies. Although only a minor fraction of the neutrons will have such high energies, they nevertheless need to be well characterized. At these high energies, data are scarce or even non-existent.

The nuclear data needed for transmutation in an ADS can roughly be divided into two main areas. First, the initial proton beam produces neutrons via spallation reactions. This means that data on proton-induced neutron production is needed. In addition, data on other reactions are needed to assess the residual radioactivity of the target. Second, the produced neutrons can induce a wide range of nuclear reactions, and knowledge of these is useful in the design of an ADS.

In most cases, direct data determination is not the ultimate goal. The global capacity for such measurements is insufficient to obtain complete coverage of important data. It is even impossible in theory to supply all relevant data. This means that the experimental work must be focused on providing benchmark data for theory development, making it possible to use theoretical models for unmeasured parameters in a core environment. In this respect, accelerator-driven systems are not fundamentally different than critical reactors.

An often overlooked aspect is why nuclear data should be measured in the first place. Nuclear data are not needed for a demonstration of the principle of driving a sub-critical assembly with an external neutron source. The need for nuclear data

becomes imminent when a realistic large-scale facility is the goal. With large uncertainties in the nuclear data, large safety margins have to be used, which results in excessive costs. Thus, the role of nuclear data is to reduce the cost for reaching a certain level of safety.

Another important aspect is the trade-off between general and particular information. Below 20 MeV, a single cross section can be of paramount importance to the entire application. Moreover, some cross sections are fundamentally inaccessible to theory, in particular in the resonance region. As a result, at low energies more or less complete data coverage for major elements is required. Above 20 MeV, the situation is fundamentally different. The cross sections are smooth, and the behaviour of the total technical system is always dictated by the sum of a large number of reactions, neither of which strongly dominates the performance. Therefore, getting a grip on the overall picture is more important than precision data on a single reaction.

1. Why Nuclear Data for Accelerator-Driven Waste Incineration?

Before plunging into the subject - neutron physics research for the development of accelerator-driven systems - it might be suitable to ask a most general question: why are we doing this at all?

Sub-critical reactors driven by some type of neutron source were first proposed more than half a century ago. Such a neutron source could be a fusion device or an accelerator-driven spallation target. At that time, the intensities of all available neutron sources were many orders of magnitudes too poor for a technical implementation, and therefore the interest vanished. About twenty years ago, the interest was rejuvenated because of the rapid development of accelerator technology which lead to the advent of spallation-driven neutron sources of intensities not too far from the technical requirements for a functioning technical system. At first, such hybrid reactors with an accelerator-driven neutron source coupled to a sub-critical core, were considered for many purposes; energy production as well as waste incineration. Over time, some consensus has been reached that the imminent potential of ADS is in incineration of spent nuclear fuel, a concept known in media as *transmutation*, a slightly mis-leading use of concept, since transmutation in general means just the conversion from one element to another by nuclear reactions. Anyway, since the word has become synonymous with waste incineration, we will use it in this article.

There is no doubt that waste in the form of spent nuclear fuel from present nuclear power reactors or nuclear weapons material can *in principle* be converted into less long-lived material. This means that already now small quantities can be treated in laboratories to high costs. Thus, the required laws of nature are there, and there is no real need for additional nuclear data to find out whether the fundamental mechanisms will work. They do.

This is, however, not enough when we discuss *realistic* treatment at a scale of interest to society. Going from laboratory-scale incineration at research-scale costs to industry-scale handling at a cost acceptable to society at large, with important boundary conditions like political acceptance, good environmental aspects, both concerning the working conditions for the personnel and losses in the process, etc., nuclear data at high energies play an important role.

The present situation resembles the development of critical power reactors. The first man-made reactor was tested by Fermi and collaborators in Chicago in 1942. At that time, very little was known about relevant cross sections. The ^{235}U fission cross section was reasonably well known, and the existence of beta-delayed neutrons had been established, but this was more or less all information at hand. The first reactors supplying electricity to a grid came into operation around 1955, and their power was about 50 MW electricity. Less than 30 years later, reactors of 1200 MW_e were common. This enormous increase - a factor 25 in 30 years - was largely due to improved nuclear data conditions.

When assessing the safety of a critical reactor system, cross sections for a wide range of reactions have to be known. Fission cross sections are obviously required to know the energy release, scattering cross sections are needed to know the spatial distribution of neutrons in the core, hydrogen and helium production reaction probabilities should be under control for safety and materials issues, capture cross sections must be known to estimate losses, etc.

If a certain cross section is poorly known, or even unknown, it has to be estimated some way. If unknown, it has to be exaggerated to the extent that it cannot be that large, just for safety. Often total cross sections are known although a certain reaction is poorly known, which means that when calculating the safety margins, the total cross section can be used. With such an approach, you are sure you have at least not underestimated the effect.

There is always a balance between safety and cost. If you overestimate the need for safety margins, it will induce excessive costs for extra shielding, additional emergency systems, etc. Thereby, the better known a cross section is, the less it costs to arrive at a given safety level. It is therefore not very meaningful to talk about the safety of a system. What makes more sense is to assess the safety for a given cost, or the cost for a given safety level.

This reasoning was the driving force behind a large worldwide nuclear data campaign during the heydays of nuclear power development, and it resulted in about 3 million experimental nuclear data points, which are accessible in nuclear data libraries.

The present situation on accelerator-driven incineration strongly resembles the development of self-sustained reactors. The main use of nuclear data is not to prove the principle, but to make it possible to go from laboratory to industry, i.e., to build large systems with a reasonable safety to an acceptable cost. Thus; *the main role of nuclear data is to improve the balance of safety versus economy.*

In general, the safe, economical, and reliable operation of a nuclear reactor depends on the use of nuclear data to predict several important characteristics of plant operation. In the case of transmutation in general, the major benefit of accurate nuclear data relates specifically to avoiding unnecessary conservatism in design and operation such as shielding requirements, power coefficients for a core loaded with minor actinides, and the related power requirements of the proton accelerator for ADS systems.

Another important difference between a dedicated transmutation system - critical or sub-critical - and a conventional critical power reactor is that for the latter, deficiencies in detailed nuclear data can partly be overcome through normalizing calculations to existing reactor measurements or experience from the operation of prototypes and test rigs. The desire to pursue new designs (Gen-IV as well as ADS concepts) without performing extensive reactor experiments dictates using nuclear data that will support reactor calculations that give dependable results even without experimental re-normalization.

2. Why Neutrons?

All large-scale applications of nuclear physics involve neutrons, and accelerator-driven incineration is no exception. Why is this the case? There are good reasons for this, governed by the laws of nature.

Atomic nuclei contain enormous energies. A typical nuclear reaction can involve energy changes which are many million times larger than for any chemical reactions. There are, however, some important boundary conditions when we want to utilize this energy.

First, these large energy releases are due to the strong interaction. Thus, we need a strong interaction probe if we want to make nuclear reactions with a reasonable efficiency. Second, atomic nuclei are positively charged and very small, and thus the Coulomb effects are enormous. In addition, nuclei are surrounded by electrons. Although the strong force is some 100–1000 times stronger than electromagnetism, the cross section area of the atom is of the order of 10^{10} times larger than that of the nucleus, so any charged probe will mostly interact with the electrons, and not the nucleus. The only realistic solution to this dilemma is to use an uncharged probe. Third, this probe needs to have a reasonable life time. It should at least be able to travel from one nucleus to another before decaying. The only particle fulfilling these conditions is the neutron.¹

Transmutation using other incident particles than neutrons have been proposed from time to time, seemingly more often in public literature than in scientific. All such proposals have been found to be dead ends, because as soon as any other particle than the neutron is involved, essentially all energy put into the system is lost to unproductive heat and not to induce nuclear reactions. For instance, gamma-ray induced transmutation was suggested some ten years ago. It was soon found that the method had such a low transmutation efficiency that if the electricity used to drive the system were produced by conventional nuclear power, more waste would be produced in the electricity generation than being incinerated in the transmutation process.

3. Which Reactions Are of Interest for Waste Incineration?

The discussion in section 2. concluded that neutron-induced reactions are necessary for a large-scale application. Thus, here we embark to find suitable neutron-induced nuclear reactions for incineration.

The fact that the neutron is uncharged makes it possible for it to enter a nucleus at any energy. Neutrons produced at whatever energy will lose energy in collisions, which finally results in really low-energy (thermal) neutrons. This means that in any reactor, thermal neutrons will almost inevitably comprise a sizeable fraction of the neutrons.

At these low energies, there are essentially three possible reactions; fission (in actinides), capture and elastic scattering. Of these, fission is by far the best reaction, and will always be the choice if possible at all. It releases lots of energy (200 MeV), which can help paying the bill, and it releases 2–4 neutrons, which can induce additional reactions, thereby lowering the demands for costly external neutron production. For really heavy

¹The "second best" would probably be the K_L^0 meson, but its lifetime of 50 ns and the requirement of associated production due to its strangeness properties makes it completely useless for practical purposes.

elements, which often have large fission cross sections, this is the preferred method.

The second best alternative is capture. It also releases energy, but much less (8 MeV) and of the wrong type (photons, which do not induce chain reactions). Thus, capture is the choice only for elements which cannot fission, like the rest products after fission, for instance technetium or iodine.

Finally, scattering plays a major role for low-energy neutrons. This makes no elemental change, but is needed to know anyway when calculating the spatial distribution of neutrons.

At still low - but not extremely low - energies, other reaction channels open, like (n,p) or (n, α). Most of them are useless for our purposes, or even detrimental. There are only a few reactions which are of interest. The $^3\text{He}(n,p)$ reaction was planned to be utilized for tritium production in the APT (Accelerator-based Production of Tritium) project, considered as a backup option in the US. The "classical" tritium production reaction is $^6\text{Li}(n,\alpha)t$ which has been run in special tritium production reactors, but it also plays a vital role in thermonuclear weapons.

Here we cannot resist the temptation to tell the story of when Shrimp went like gangbusters [1]. It is really a good illustration of the need to know nuclear reaction data in general, and what can happen if you do not know them in particular. The first hydrogen bomb, Mike in 1952, was essentially a huge dewar of liquid deuterium, ignited by a conventional fission bomb. This demonstrated that the fusion bomb principle works, but its total weight of 82 tons made it useless as a combat weapon. The next development was the Shrimp device of the Castle Bravo series. It consisted of ^6LiD , where energy is released in a two-step process. Tritons and deuterons fuse into alpha particles and a neutron, and the neutron subsequently produces new tritium by the $^6\text{Li}(n,\alpha)t$ reaction. With this technique, bombs sufficiently light and compact to be airborne became feasible.

What the construction crew had missed altogether was the relatively large $^7\text{Li}(n,2n)$ reaction. The lithium was not pure ^6Li , but enriched to 40 % (it is 7.5 % naturally) and thereby the rest was ^7Li . The $^7\text{Li}(n,2n)^6\text{Li}$ reaction increases the explosion yield in two ways; it increases the number of neutrons and it produces additional ^6Li , which can subsequently produce more tritium. The expected yield was 5 MT, but it went off with 15 MT, which makes it the largest US bomb test ever. A Japanese fishing boat, *Fukuryu Maru* (the *Lucky Dragon*) was hit by radioactive fallout, and the crew members got ill, whereof one subsequently died from secondary infections. For obvious reasons, this event caused political complications in the relations between USA and Japan.

4. Why Accelerator-Driven Systems?

Next question to ask is why accelerators should be used. Why not convert the waste in standard critical reactors?

Let us first give the wrong answer! There are examples of proponents of accelerator-driven systems (ADS) who claim that the key issue of ADS is that they are sub-critical and therefore inherently safe. That argument is not very well founded, because critical water-moderated power reactors have been run over 10 000 production years by now, and up to now there has been only one serious incident.² This was the Three-Mile Island accident in

²The Chernobyl accident concerned a graphite moderated reactor

1979, which destroyed a reactor and made the owners unhappy because of a lost investment, but it did not seriously injure a single person. In fact, to this very day, not a single person has died due to problems in a commercial water-moderated reactor. This makes them maybe the most safe technical systems ever built by man, and superior as safe technical systems for energy production [2].

The real reasons are that some elements are very difficult or impossible to incinerate in a critical reactor, but possible to handle in ADS. This is because of the (non-)existence of delayed neutrons. In a standard critical power reactor, ^{235}U is used as fuel. On average, thermal fission of ^{235}U results in 2.43 neutrons. Of these, exactly one should induce new fission events, and the remaining 1.43 should be removed by capture in, e.g., control rods. Suppose something happens that increases the power in the reactor, so that more neutrons are produced, leaving not 1.00 but 1.01 neutrons for new fission per neutron generation. The average time between neutron release and next fission is of the order of 10^{-4} s. In one second, this should result in 1.01^{10000} neutrons, which is of the order of 10^{43} neutrons. Thus, without some way to slow down things, a 1 % mistake would blow up the reactor in fractions of a second, i.e., much faster than a normal human being can react.

Nature is kind enough to provide a solution: delayed neutrons. All the 2.43 neutrons are not released simultaneously. Almost all are emitted very rapidly, but a small fraction, about 0.7 %, are emitted after beta decay to neutron-unstable states, and are therefore released after anywhere from fractions of a second up to a few minutes. This makes it possible to build a less "nervous" system. The reactor can then be designed in such a way that 0.993 prompt neutrons are available for next fission generation, i.e., the reactor is slowly stopping on the prompt neutrons, but 0.007 additional delayed neutrons appear a few seconds later. Now the neutron economy balances, and the characteristic time constant is orders of magnitudes slower. A mistuned knob will then result in a slow change of the reactor, rather than prompt criticality.

Now back to the original issue. ^{235}U is a very favourable case. Most elements have fewer delayed neutrons, and some elements we want to get rid of have virtually no delayed neutrons at all. This means that, e.g., some americium isotopes are very difficult to burn in critical reactors under controlled conditions. A way out of this dilemma is to build a sub-critical system, in which every fission chain is slowly dying, but new chains are initiated by externally produced neutrons. In popular terms, the role of the delayed neutrons is now replaced by the accelerator-produced ones.

5. Which Data Are of Interest for Accelerator-Driven Systems?

5.1. Direct Measurements or Data for Theory?

Up to now, there has been a very large research volume spent on data on neutron-induced nuclear reactions up to 20 MeV. This was carried out from around 1950 until today, and was motivated by the needs in the development of civil nuclear power, as well as weapons applications and fusion technology. During the last decade, nuclear data at higher energies have been in the limelight due to the discussions about ADS.

The approaches in these two disciplines differ significantly. This is neither a surprise nor a bad choice, because the underlying physics differs significantly, resulting in different

research strategies. Below 20 MeV, a single cross section can be of paramount importance to the entire application. An example is the neutron capture resonance at 6.7 eV in ^{238}U that provides the Doppler effect so important for the stability of critical reactors. Moreover, some cross sections are fundamentally inaccessible to theory, in particular in the resonance region. As a result, at low energies more or less complete data coverage for major elements is required. Above 20 MeV, the situation is fundamentally different. The cross sections are slowly varying in energy, and the behaviour of the system is always dictated by the sum of a large number of reactions, none of which strongly dominates the performance. Therefore, getting a grip on the overall picture has been a more natural ambition in an initial stage, rather than providing precision data on a single reaction.

In section 3., the useful reactions for waste incineration were discussed. This does not mean, however, that these reactions are the only ones to be studied. Many unwanted reactions will also take place, and these need to be known as well. In addition, it is even possible that completely different reactions are even more interesting for our application, although they do not contribute at all (!). How can this be the case?

We have to realize that the task to measure everything that will happen in a future transmutation plant is an insurmountable one. First of all, this is because of practical limitations. At high energies, above 50 MeV or so, *one* cross section for *one* nucleus at *one* energy typically requires about a week of beam time at an international accelerator laboratory. Loosely calculated by industry standards, one week represents a cost of the order of 1 MUSD. Covering all relevant reactions for all relevant elements at moderate energy intervals would not only present costs approaching national budgets in size, it would also literally take centuries to accomplish with the presently available laboratories.

Furthermore, even if the practical obstacles could be overcome, the situation would still not be satisfactory. In a reactor core, large quantities of short-lived elements can affect the operation, but these isotopes can be impossible to study in experiments. A good example is ^{135}Xe , the well-known villain of the Chernobyl accident with a half-life of 9 h, which makes it impossible in practice to make targets of it for in-beam measurements of cross sections.

All these conditions imply that all important data cannot be measured; instead theory has to be used for unmeasured regions. This in turn gives priority to measurements that are decisive for theory development, rather than direct measurements of specific cross sections. Hence, it is possible that reactions not even present in the system, or indirect quantities, should still be measured for theory development reasons.

5.2. Constraints Imposed by Accelerator-Driven Systems

In figure 1, a simplified accelerator-driven system (ADS) is illustrated. It consists of an accelerator producing a proton beam, a spallation target for neutron production, and a surrounding core for waste incineration and/or energy production. This simple picture illustrates the main areas for new nuclear data. First, proton-induced neutron production, i.e., (p,n) reactions, has to be known, and second, whatever these neutrons end up doing has to be investigated.

The (p,n) reaction at small angles was extensively studied during the 1980's on a wide range of elements for completely other reasons [3], so that particular basic information is

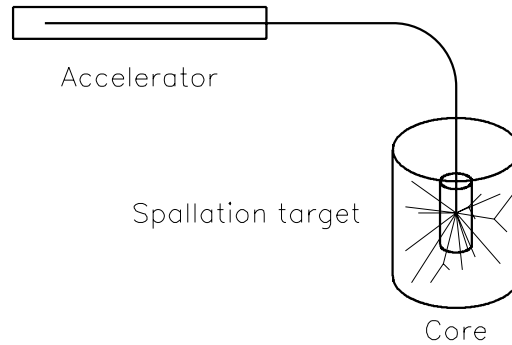


Figure 1. The basic ingredients of an accelerator-driven transmutation system.

at hand. This is unfortunately not sufficient for spallation target modelling. When protons of more than 1 GeV hit a thick target, direct production is not the only neutron production process. Instead, a cascade of nuclear reactions can take place, with processes like (p,d) followed by (d,n), etc. A 1 GeV proton on a thick lead target produces about 20 neutrons via many such reaction chains.

Experimental studies of proton-induced neutron production has the interesting feature that the results depend on the target thickness. The optimal goal would be to disentangle the individual reactions and to calculate the total neutron yield as a sum of all possible reaction cascades. Since so many processes are involved, it is very difficult to measure them all with the technically required precision. Instead, integral measurements of the total number of neutrons are carried out, but then the result depends on the target thickness. Therefore, some caution has to be used when estimating the neutron yield from a spallation target, based on experimental data on proton-induced neutron production.

If it is pretty evident which proton-induced reactions should be studied, i.e., (p,xn) at a few realistic energies for future ADS, the situation on neutron-induced reactions is less obvious. Many reactions play important roles and at a very wide energy range. In most present design studies, the accelerator-produced neutrons comprise typically 5 % of the total number of neutrons. The rest are due to fission chain reactions. Thus, 95 % of all neutrons are at low energies, i.e., released at 0.1–5 MeV. Spallation results in a neutron spectrum with a roughly $1/E_n$ shape, peaking at only a few MeV. Hence, about 20% of the spallation neutrons are below 20 MeV already when coming out from the spallation target, which means that less than 1 % of all neutrons are above 20 MeV in the reactor core, even before moderation.

Thus, the bulk of neutrons are below 20 MeV. The present nuclear data libraries were originally motivated by data needs in power fission reactors or fission weapons (below 8 MeV), or in fusion research or thermonuclear weapons (14 MeV). The 20 MeV range was to large degree dictated by the presence at that time of tandems which could produce neutron beams up to about 20 MeV. Due to this, the data situation at low energies is rather satisfactory. Most elements have been thoroughly studied, but not all. Some isotopes remain to be investigated, and for others there are discrepancies in the data bases. These issues have motivated renewed interest in low-energy measurements.

At the highest energies, the number of neutrons is very small. In addition, theory works reasonably well above, say, 200 MeV. The reason is that at lower energies, the incident neutron has a de Broglie wave length corresponding to the radius of the nucleus. This implies that the entire nucleus, with all its structural properties participate in the reaction, making it exceedingly complex. At higher energies, the de Broglie wave length is about the size of individual nucleons, and the incident neutron interacts just once with a single nucleon. Accordingly, the reaction has a much simpler character, and can be described reasonably well with simple theory. (Well, simple for being nuclear theory...) Another way of illustrating the same effect is to note that a nucleon is typically bound by 8 MeV in a nucleus. This constitutes a major factor when a 10 MeV incident neutron interacts with it, but not when a 1000 MeV neutron shows up.

There is a mid-range (often denoted "intermediate-energy nuclear physics") from 20 to 200 MeV where data for applications are rather scarce, and where theory is not in a very good condition. As a consequence, vast efforts have been devoted to this energy domain in the recent decade. In particular, the European joint project on high-energy nuclear data for accelerator-driven systems, HINDAS, has provided a wealth of new information (see section 7.).

Throughout this paper, high energy means 20 MeV and up, unless something else is stated. The reasons are the ones given above; the availability of large data sets motivated by the classical nuclear applications up to about 20 MeV, and the virtual lack above 20 MeV, which has to be overcome now, when emerging applications demand such data.

5.3. Classification of Data

On a (very) broad level, the nuclear data requirements for transmutation of waste fall into two classes:

1. Resonance and fast neutron reactions for materials that are specific to transmutation: unconventional structural materials, coolants and (in the case of ADS) targets, and minor actinides, whose abundance in the core is much larger than in a conventional reactor.
2. Energy regimes that extend beyond the fast neutron region (up to hundreds of MeV) for the above materials and conventional materials.

The first class applies to any transmutation method, i.e., including critical reactors, whereas the second class exclusively applies to ADS. In this paper, we will discuss both classes, but the emphasis is on high-energy data because they are unique to ADS.

It should be emphasized that nuclear data needs that apply to a critical power system, in general also apply to transmutation systems, critical as well as sub-critical. For example, the important interplay between ^{238}U fission, capture and inelastic scattering, is crucial for a precise determination of criticality. Minimizing the uncertainties in these data is also important for transmutation systems.

5.4. Which Are the Most Important Low-Energy Data?

At low energies, a large bulk of data has been produced in orchestrated campaigns during the last half century. Thus, reasonably accurate data on most important nuclides exist. Future work on nuclear data of transmutation relevance can be classified along the following lines:

1. **Data on elements for which improved precision is required.** In commercial critical reactors, the core is heavily dominated by uranium, with a small content of fission products and transuranium elements. Of the transuranium elements, plutonium typically account for about 90 %. In proposed transmutation devices, it has however been suggested that incineration of americium and/or curium should be a major focus. This imposes demands for more stringent data on americium and curium. In conventional reactors, where these elements constitute a very small fraction of the core, even large data uncertainties for these elements do not result in large uncertainties in the behaviour of the full system. In a transmutation system with large minor actinide inventory, this is no longer true. As a consequence, the experimental efforts on cross section measurements on minor actinides have increased significantly during the last decade.
2. **Data in new energy regions.** In essentially all proposed transmutation systems, the energy spectrum is harder than in conventional light-water reactors. With a larger fraction of neutrons at higher energies, the demands for accuracy could be higher in energy ranges where rather large uncertainties can be tolerated in thermal reactors. A good example is neutron capture in ^{238}U in the keV region. This cross section has recently been identified as one of the major contributors to the overall uncertainties in fast critical reactors, as well as ADS.
3. **Data on elements previously not abundant in reactors.** In an ADS, elements that are not common in conventional reactors could be abundant. One example is lead and bismuth, which are prime candidates as spallation target for neutron production, and also one of the major candidates as coolant. It was pointed out by a recent study that the uncertainties in inelastic neutron scattering from lead could produce large uncertainties in the criticality of the entire system. The reason is that inelastic neutron scattering results in significant energy loss of the neutron, i.e., with a larger inelastic cross section, a larger fraction of the neutrons appear at lower energies. Since fission and capture cross sections depend heavily on neutron energy, also a modest change in the neutron energy spectrum could result in large total fission and capture rates. As a consequence of the findings above, considerable attention has been devoted to neutron scattering on lead in the few MeV range recently.

5.5. Which Are the Most Important High-Energy Data?

As mentioned above, at high energies not even close to all data can be measured. Therefore, we have to make tough selections. The efforts should be concentrated along a few major lines:

1. **Data for theory development.** Such data need not to be strong reactions in a transmutation core, and they do not even need to be direct quantities. The optical model,

which is the prime tool in nuclear models for these kind of systems, relies essentially on three types of experimental information; elastic scattering, and total and reaction cross sections.

The elastic scattering cross section is the most important measurement when determining the optical model, which in turn is the standard representation of the force between a nucleon (proton or neutron) and a nucleus. The optical model is used as input in virtually every cross section calculation where a nucleon is present either in the incident or exit channel, and is therefore of major importance for theory development. This makes, e.g., neutron elastic scattering measurements much more important than they would be if they were only useful for direct use in calculations of the spatial distribution of neutrons in a core.

The total cross section is the sum of all possible interactions, i.e., the probability that anything happens at all. The reaction cross section is the sum of all interactions except elastic scattering. These quantities can both be measured directly even if all the partial cross sections constituting it are unknown. Moreover, they can serve as guidance when different optical models all describe the same elastic scattering data well, but result in different reaction cross sections.

2. **Strong reaction channels.** Elastic scattering is again a good example. At high energies, quantum mechanics requires elastic scattering to constitute at least 50 % of the total cross section, i.e., at least half of all what happens is elastic scattering. For high-energy neutrons, it turns out that elastic scattering in most cases constitutes about 50 %. Other strong reaction channels are (n, xp) , (n, xn) and $(n, x\alpha)$. These four reaction channels (including elastic scattering) typically accounts for about 99 % of all neutron-induced reactions.
3. **Data for special, important purposes.** Fission is a prime example. It is neither top-of-the-list for theory development nor a strong channel (except for some actinides), but its profound consequences makes it important for the overall assessment of the system. For instance, fission products account for a sizeable fraction of the residual radioactivity of the spallation target.

Other examples are (n, xp) and $(n, x\alpha)$ cross sections for hydrogen and helium production. Since hydrogen is explosive, the production has obviously to be kept under control, or at least be known. In addition, hydrogen embrittles steel. Helium makes structural materials like steel swell, and data are therefore required for material aspects.
4. **Overall quantities.** For example, measurements of the total radioactivity after irradiation by residue detection are useful for cooling assessment.

6. Neutron Experiments - The Art of the Impossible?

Rather than presenting a list on all recent and ongoing experiments in the field, we would first like to discuss some general features of experiments involving neutrons, and later give

some non-exhaustive examples on recent achievements. In experimental work on neutron-induced nuclear reactions, there are major experimental challenges. This is true irrespective of energy, but some of the problems get accentuated at high energies. Therefore, our examples are taken primarily from experiments above 20 MeV, although some of the issues discussed here are also important at lower energies.

6.1. How Many Neutrons Are There in the Beam?

To measure a cross section, the intensity of the beam has to be known. A most notorious problem is how to determine the intensity of a neutron beam. This might sound like an innocent problem, but it is close to impossible to overcome it (!).

A charged particle interacts with the electrons of the atom. Thereby it is possible to build systems where *every* particle gives a signal when passing through a detector, and hence it is a relatively simple task to determine the beam intensity by just counting pulses. Another option is to stop particles via their energy loss - which is also an effect of interaction with the atomic electrons - and finally measure the collected charge. This is performed in every Faraday cup at every laboratory.

Neutrons interact by the strong interaction only, and they are uncharged. This means that there is no way you can build a device which produces a signal for each particle that passes, and you cannot stop neutrons in a controlled way. Detection of neutrons *always* has to proceed via a nuclear reaction, releasing charged particles, which can subsequently be detected. The problem is that there is no way to determine a nuclear cross section from theory only with a reasonable precision. This means we end up in circular reasoning.

Let us assume we want to use neutron-proton (*np*) scattering for neutron detection. Counting the protons emanating from a hydrogenous material is a relatively simple task, but we need to know the cross section to derive the number of incoming neutrons. To measure that cross section, however, we need to know the number of incident neutrons. Are there no ways out of this vicious circle?

In fact, there are a few tricks which can be used, but they are all associated with large difficulties. The standard procedure is therefore to determine a single cross section using all these painstaking methods, and subsequently this cross section is used as *reference*, i.e., other cross section measurements are measured relative to it.

The standard reference cross section at our energies is *np* scattering. Suppose we want to measure $^{12}\text{C}(n,p)$. Instead of trying to measure the cross section on an absolute scale (which is virtually impossible), a typical experiment would be designed to measure the *ratio* of $^{12}\text{C}(n,p)$ versus *np* scattering. This facilitates the experimental work immensely.

A major risk with such an approach is that if the reference cross section is uncertain (or even wrong), all other data measured relative to it will be equally off. Such a case might in fact be at hand for neutrons above 50 MeV, where *np* differential cross section data display significant inconsistencies [4]. Not surprisingly, major experimental work has recently been devoted to find agreement about the exact value of this reference cross section [5]-[9].

The techniques used in these ongoing experiments are - not surprisingly - the main tricks used to obtain absolute reference cross sections for neutron-induced reactions. Below, these techniques are described.

6.1.1. Tagged Beams

The methodologically simplest method is probably to use tagged beams, but this does not necessarily mean it is the simplest technique in real life. Neutrons do not exist freely in nature (the life time is about 15 minutes) but have to be produced by nuclear reactions. For a few reactions, detection of the residual nucleus can be used to verify the neutron production. An example is the $D(d,n)^3\text{He}$ reaction. By detecting the kinetic energy and direction of the residual ^3He nucleus, the energy and angle of the neutron is known. In addition, the detection of a ^3He nucleus implies that there *must* be a neutron, i.e., the ^3He nucleus serves as a "tag" on the neutron. With this technique, "beams" of really low intensity, but with known intensity can be produced. This beam can subsequently be used for cross section measurements.

Using this method for every cross section measurement is, however, not an option because of the extreme beam times required. Instead, it is used for precision determination of a reference cross section, which is subsequently used in other, relative, experiments (see above).

6.1.2. Combination of Total and Differential Hydrogen Cross Sections

The total cross section, i.e., the probability that a neutron interacts at all with a target nucleus, is a quantity which can be determined without knowledge of the absolute beam intensity. This integral cross section is related to the attenuation of a neutron beam, which means that a relative measurement of the beam intensity before and after a target is sufficient.

In the case of hydrogen, the total cross section is completely dominated by elastic np scattering, which accounts for more than 99% of the total cross section. The only other reaction channels possible are capture ($np \rightarrow d\gamma$) and bremsstrahlung ($np \rightarrow np\gamma$), which both are orders of magnitudes weaker.

A relative measurement of the np scattering angular distribution can thereby be normalized to agree with the total cross section, and thus an absolute $^1\text{H}(n,p)$ cross section can be obtained.

6.1.3. Combination of Total and Reaction Cross Sections

The differential elastic cross section of a nucleus can be determined absolutely by a combination of total and reaction cross section measurements, together with a relative measurement of the differential elastic cross section. Both the total cross section and the reaction cross section can be determined in relative measurements of beam attenuation. The only important difference is the geometry used. The integrated elastic cross section can then be derived as the difference of the total and reaction cross sections. The elastic differential cross section on almost any nucleus falls dramatically with angle. Thus, by covering a moderately wide range at forward angles, essentially all the elastic differential cross section is covered. Thereby, the differential cross section can be related to the integrated elastic cross section. This technique works best for light nuclei, where there are only a few excited states with large separation in excitation energy. The reason is that the reaction cross section measurements often are plagued with a poor energy resolution, which results in contamination of inelastic scattering into the data.

6.1.4. Theoretically "Known" Reactions

Above 275 MeV, pion production opens up in neutron-proton reactions. The $np \rightarrow d\pi^0$ reaction should have exactly half the cross section of the $pp \rightarrow d\pi^+$ reaction, if isospin were a good symmetry, all particle masses were the same and there were no Coulomb effects. The latter reaction involves charged particles only, and should therefore be possible to determine accurately.

This technique has been frequently used for normalization of np scattering experiments above the pion production threshold, where protons from $np \rightarrow pn$ reactions and deuterons from $np \rightarrow d\pi^0$ reactions are detected simultaneously. Assuming the latter cross section to be known, using the relation between neutron- and proton-induced pion production can then provide an absolute cross section. This method is the least exact of the ones presented here, however, because it involves rather large, and partly unknown, corrections.

6.2. How Large Is a Neutron Detector?

In any experiment aiming at measuring a cross section, knowledge of the solid angle subtended by the detector is required. In charged-particle measurements, it is common to define the solid angle by either a passive collimator in front of the detector, which physically stops the particles, or by an active collimator, i.e., a detector with a hole so that particles hitting the collimator produce signals, which in turn are used for event rejection.

At really low neutron energies, passive collimators can sometimes be used, because there are a few elements (e.g., boron and cadmium) which possess huge capture cross sections, and thus very efficiently remove neutrons. These large capture cross sections are present only up to a few eV energies though. At slightly higher energies, collimation can still be obtained by having a moderator (like paraffin) doped with, e.g., boron. Thus, neutrons are moderated and then captured.

Above 20 MeV, such techniques are completely useless. There are no strong capture cross sections, and the scattering cross sections needed for moderation are so small that any reasonably well-working collimator has a thickness of the order of metres (!). In addition, at these energies many particle-production channels are available. Thereby, a collimator can sometimes produce almost as many particles as it removes. The bottom line of this discussion is that in most cases detectors without front collimation is the preferred choice. This is often referred to as "naked" detectors, which is a truth with qualification. Often the detectors have very heavy shielding at the sides and back to protect them from ambient room background.

Since collimators cannot be successfully used, it is sometimes very difficult to know the effective size of a detector. A good example is provided by a recent state-of-the-art determination of the efficiency of a liquid scintillator detector, which has been used in proton-induced neutron production experiments for ADS [10]. The efficiency of this detector was measured in a tagged neutron experiment (see above), where the position and direction of the incident neutron was well known. Thereby, the detection efficiency for different physical parts of the detector could be determined. It was found that the efficiency was constant over most of the area, and that it dropped close to the edges, which was expected. It was also found, however, that some signals in the detector corresponded to neutrons not even hitting the detector. The reason for this weird effect was that there is a plastic front layer

in the detector, which extends outside the active area of the detector. Thus, neutrons which would have barely missed the detector had been scattered into the detector, raising its apparent efficiency significantly. This illustrates that the effective area of a neutron detector might not be straight forward to establish, and the question in the headline of this section might not be as stupid as it sounds at first.

6.3. How Are High-Energy Neutron Experiments Carried Out?

Rather than presenting a long list of different kinds of experiments, we are going to go through a case study: elastic neutron scattering. The choice of reaction is partly dictated by convenience (the author has some experience of it) but it is also a good illustration of all the problems associated with high-energy neutron work, because it involves neutrons both in the incident and exit channel. Thus, it gives you all the trouble...

6.3.1. Neutron Production

The first obstacle is neutron production. At low energies (below 20 MeV or so) monoenergetic neutron beams can be produced. There are a few light-ion reactions, like $D(d,n)^3\text{He}$ and $T(d,n)^4\text{He}$, which have positive Q-values and sizeable cross sections. The most widely used reaction is $T(d,n)^4\text{He}$, where deuterons of only a few hundred keV can produce 14 MeV neutrons.

Such a beam is strictly monoenergetic up to about 2 MeV incident deuteron energy. Above this energy, there is a possibility that the deuteron breaks up (it has a 2.2 MeV binding energy) into a proton and a neutron. Hence, strictly monoenergetic neutron beams are only possible up to about 16 MeV (i.e., 2 MeV beam plus 14 MeV Q-value). In reality, this is not a major obstacle until you get up to about 30 MeV or so, because the $T(d,n)^4\text{He}$ cross section is so large that the breakup neutrons form only a small low-energy tail. At even higher energies though, the $T(d,n)^4\text{He}$ cross section is smaller, making the total yield too low for most measurements.

The breakup effect is there in all nuclei, and in almost all nuclei neutrons are bound by about 8 MeV. The largest neutron separation energy is about 20 MeV. The consequence of this is that truly monoenergetic beams are impossible to produce above that energy. What is available at higher energies are quasi-monoenergetic beams, i.e., beams where a single energy dominates, but it is always accompanied by a low-energy tail.

If we go to energies at 50 MeV and up, three production reactions give reasonably monoenergetic beams. These are $D(p,n)$, $^6\text{Li}(p,n)$ and $^7\text{Li}(p,n)$. The first has a large cross section, but the drawback that the energy resolution of the full-energy neutrons cannot be better than 3 MeV due to the Fermi motion of the neutron inside the deuteron. If a sharper energy definition is required, one of the two reactions using lithium is selected. They are about equally good, but there is a major practical difference: ^6Li is used in hydrogen bombs and is therefore not easily obtained, while ^7Li is provided more or less for free. As one could expect, $^7\text{Li}(p,n)$ is the most common production reaction for monoenergetic neutron beams. At 100 MeV, about 50 % of the neutrons fall within 1 MeV at maximum energy, while the remaining half are distributed about equally from maximum energy down to zero. This is the closest to monoenergetic conditions nature provides.

There is also a completely different approach to the whole production. Instead of trying to get the neutrons as well gathered in energy as possible, all energies are produced simultaneously. A high-energy proton beam hits a thick (mostly stopping) target and lots of neutrons of all energies are produced, with typically a $1/E_n$ spectrum. If the incident proton beam is bunched and the experiment target is placed at a rather large distance from the neutron production target, time-of-flight (TOF) methods can be used to determine the energy of the incident neutron on an event-by-event basis.

The advantage of such so called white beams is the total intensity, which is larger than for monoenergetic beams, but instead the intensity per energy interval is much lower at high energies. This can partly be compensated by summing data over limited energy intervals, but still the intensity per such interval is lower. The advantage of being able to measure at many energies simultaneously is not worth much if you get insufficient statistics everywhere. As a consequence, white beams are restricted to experiments at low energies, where the intensities are large, or to high-energy reactions where the cross sections are rather large. Another feature is that white sources require event-by-event measurements. Experiments of effects with an energy dependence where the individual events cannot be distinguished cannot be performed at white beams. For experiments fulfilling the requirements above, white sources can, however, provide large quantities of very valuable information. This is especially true when excitation functions, i.e., the energy dependence of a cross section, is of particular interest.

The total cross section data from Los Alamos could serve as an example [11], where many thousand data on a wide range of nuclei in the energy range from a few MeV up to over 500 MeV have been acquired. Producing data of similar quality at monoenergetic beams would be extremely time-consuming.

6.3.2. Neutron Detection

Just detecting a neutron at all can be hard enough, but isolating a certain final nuclear state is even more difficult, because it requires energy measurement of the neutron. Since the neutron must be converted to a charged particle via a nuclear reaction to be detected at all, we easily run into trouble. Most nuclear reactions can produce charged particles at many different energies, and hence there is not direct correspondence between the detected charged-particle energy and the incident neutron energy. This restricts the possible techniques for neutron spectrometry immensely.

At low energies, where the $T(d,n)$ reaction is used for neutron production, the standard technique for elastic scattering measurements is time-of-flight. A pulsed deuteron beam hits a tritium target, thereby producing pulsed neutrons. These neutrons are scattered from a target and hit a fast detector, almost always a liquid scintillator, with an intrinsic efficiency of the order of 10 %. This detector only needs to register that a neutron hit it, and at which moment. Thereby, the energies of the released charged particles in the detector do not need to be well known. The flight time is provided by the time difference between the deuteron pulse hitting the tritium target and the stop pulse from the neutron detector.

At these energies, the deuteron beam stops in a thin metal foil. The beam stop is then made of an element with a high threshold for neutron emission, so that all neutrons emanate from the tritium target. The scattering target can be located very close to the neutron

production, and the total flight path needed to resolve nuclear final states is a few metres. If a large hall is available, the detection system with its shielding can be placed on a rotating system. If not, it is customary to use a fixed flight path and instead rotate the incident beam at the neutron production with a beam swinger, consisting of a moveable magnet system.

When going above 50 MeV, the conditions change significantly. Time-of-flight techniques become gradually more difficult to use with increasing energy in a more than linear fashion. With a total time resolution of 1 ns, one will get an energy resolution of 0.5 MeV with a flight path of 5 m at 20 MeV, while for the same energy resolution at 100 MeV, 60 m is needed. This would require a very large array of neutron detectors to preserve a reasonable solid angle and count rate. Even if accepting a 2 MeV resolution, the flight path is 15 m, which is too long for a rotating detector system. This leaves a beam swinger as the only alternative.

This will not solve all problems, however. At these high energies, the beam stop for the incident charged beam needs to be many centimetres thick, and we are above the neutron production thresholds for any material. This means that a vast majority of all neutrons produced come from the beam stop and not the production target. A solution to this dilemma is to move the beam stop away from the neutron production by a second beam swinger system. This does not only cost lots of money; it also costs count rate, because it increases the production-to-target distance, and the neutron intensity at target falls with the square of this distance.

One way out of this dilemma is to use a completely different approach. If we skip the time-of-flight technique altogether, and instead detect neutrons by conversion to charged particles, where the energy of the latter is measured, we can shorten the distance from the scattering target to detection to about 1 m. A typical converter is a material containing hydrogen, with conversion via np scattering. Detection of the direction and energy of secondary protons can be used to determine the energy and direction of neutrons hitting the converter. Now it is relatively easy to get a large detection solid angle, but this is also a pre-requisite, because the conversion efficiency is inherently low, far smaller than 1 %. With such a technique, we can allow a longer (and fixed) distance from neutron production to scattering target which makes a nice dumping of the initial beam feasible.

It should be pointed out that for all other reactions than elastic scattering, we get an additional problem. The beam is not truly monoenergetic, and therefore TOF measurements are needed to reject the low-energy neutrons in the beam. This means that, e.g., inelastic scattering cannot be measured at all with TOF techniques at these energies, because TOF information is needed twice (production to scattering target and scattering target to detector) but only one TOF measurement is available (production to detector).

7. Survey of Recent High-Energy Experiments

As has been described already, the data needs for development of accelerator-driven systems can never be satisfied by measurements only. Theory has to be used for unmeasured (or unmeasurable) regions. Even with an optimum coordination between experiment and theory, the data requirements would require large efforts at many laboratories over a long time. Thus, some kind of structure and organization is called for. Such a framework exists, and below it is briefly outlined. In section 7., the ongoing work is described in more detail.

HINDAS (High- and Intermediate-energy Nuclear Data for Accelerator-driven Systems) was a joint European effort, which gathered essentially all European competence on nuclear data for transmutation in the 20-2000 MeV range during 2000–2003. Similar activities were undertaken in the USA and Japan, to a large extent complementing experiments on the same nuclear reactions. Since the completion of the HINDAS project, the newly produced high-energy nuclear data for ADS have to a large degree been follow-up experiments with the same setups.

The program was designed to obtain a maximal improvement in high-energy nuclear data knowledge for transmutation. The approach to reach this goal was to employ a well-balanced combination of basic cross section measurements, nuclear model simulations and data evaluations. The three elements iron, lead and uranium were selected to give a representative coverage of typical materials for construction, target and core, respectively, especially relevant to ADS, as well as to supply a wide coverage of the periodic table of elements.

The only important high-energy reaction not covered was fission. This was instead the target of a focused series of experiments by primarily Russian research groups active under the realm of ISTC, the International Science and Technology Centre.

7.1. Elastic Neutron Scattering

Every physics student has to take a course on basic optics. In all such courses, the diffraction of light by slits and grids is treated. In a two-slit diffraction setup, the intensity versus angle shows a characteristic pattern, with intensity maxima and minima overlayed on an overall intensity decrease. This effect is due to constructive and destructive interference of waves passing through the two slits.

When neutrons are scattered elastically from a nucleus, a similar effect occurs due to interference of neutron waves on each side of the nucleus. The nucleus itself is strongly absorbing, being almost "black" for incident neutrons. There is hence a direct analogy between optics and neutron scattering.

A real potential can only cause scattering. This is because a real potential conserves the flux, i.e., the number of incident and emitted particles has to be the same (and they have to be of the same type). Nuclear reactions, on the other hand, violates this conservation. This means we need an imaginary potential to account for particle changes or losses.

This is another analogy with optics. A real refraction index can only describe scattering of light, but not intensity losses. For that, an imaginary refraction index has to be used.

When we want to describe the interaction between a neutron and a nucleus, the simplest potential we can use has two components, one imaginary and one real, and this is called *optical potentials* because the approach was inspired by optics. Such optical potentials are true work horses of nuclear physics. Whenever a neutron impinges on, or is being emitted from, a nucleus, an optical potential is used to describe the motion.

The major features of these optical potentials are derived from elastic scattering data. Such data are therefore not only needed for calculations of neutron transport and moderation, but they are also a necessary component in the description of many other reaction channels.

In addition, the elastic cross section is also the largest of the individual partial cross

sections contributing to the total cross section. In fact, a consequence of the optical model is that the elastic cross section must constitute at least half the total cross section.

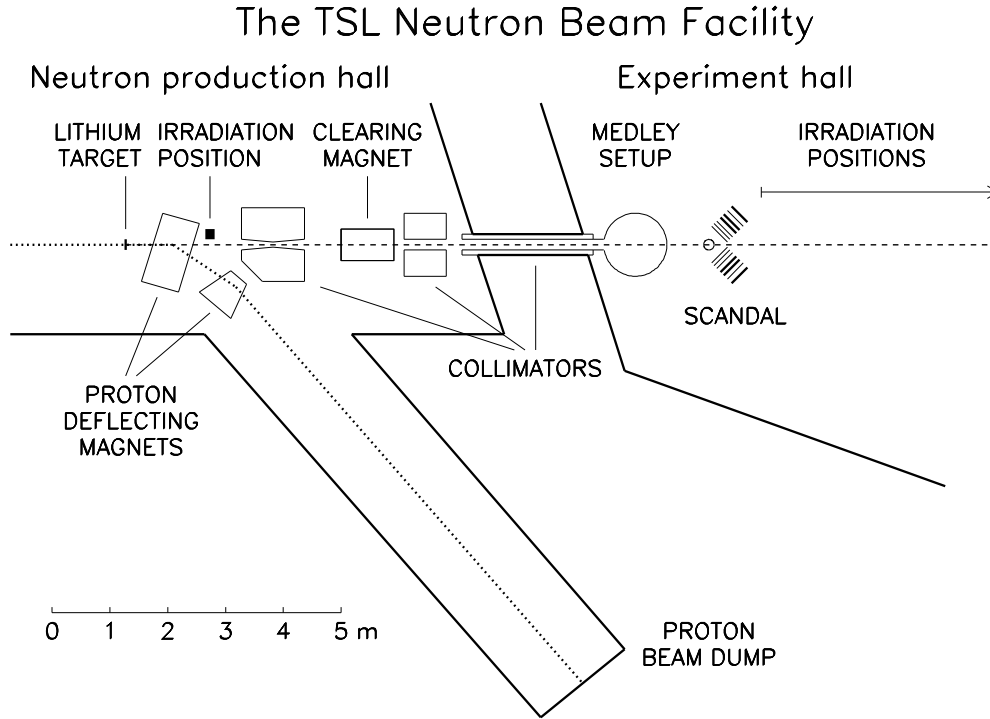


Figure 2. The TSL neutron beam facility.

Given the time and cost to carry out elastic scattering experiments, the main focus must be on developing theoretical models rather than systematically measuring all nuclei. The obvious nuclei to study are then the magic or semi-magic nuclei, i. e. ^{12}C , ^{16}O , ^{40}Ca , ^{90}Zr and ^{208}Pb . Here it is fortunate that lead (cooling) and zirconium (cladding) are also important materials in future transmutation facilities, so the gain is twofold.

A facility for detection of scattered neutrons, SCANDAL (SCattered Nucleon Detection AssemBLy), has recently been installed at the The Svedberg Laboratory (TSL) in Uppsala, Sweden. It is primarily intended for studies of elastic neutron scattering, but can be used for the (n,p) and (n,d) reactions as well. The energy interval for detected neutrons is 50–130 MeV, which makes the facility suitable for studies of transmutation-related cross sections.

At the neutron facility at TSL [12, 13] (see fig. 2), neutrons are produced by the $^7\text{Li}(p,n)$ reaction. After the target, the proton beam is bent by two dipole magnets into an 8 m concrete tunnel, where it is focused and stopped in a well-shielded carbon beam-dump. A narrow neutron beam is formed in the forward direction by a system of collimators.

The setup is primarily intended for studies of elastic neutron scattering, i. e., (n,n) reactions. The neutron detection is accomplished via conversion to protons by neutron-proton scattering. In addition, (n,p) reactions in nuclei can be studied by direct detection of protons

(see section 7.2.). This is also used for calibration of the setup.

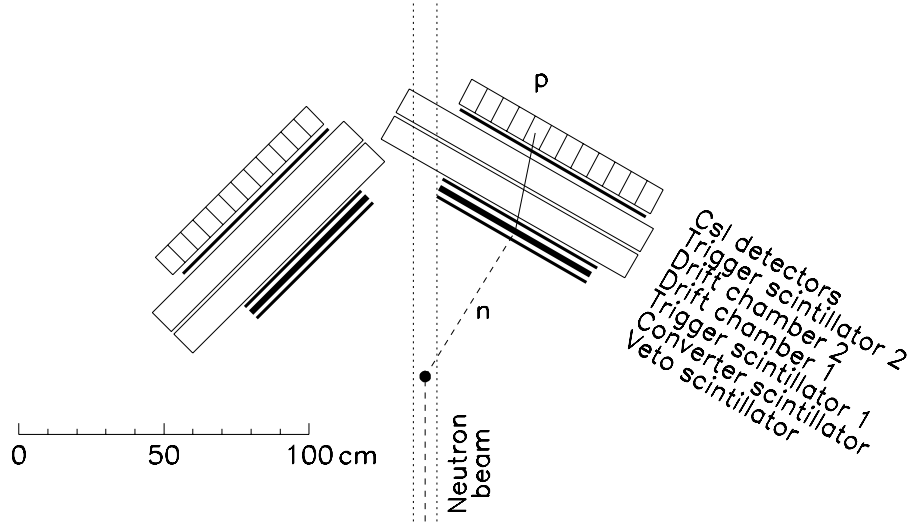


Figure 3. Schematic figure of the SCANDAL setup.

The device is illustrated in fig. 3. It consists of two identical systems, typically located on each side of the neutron beam. The design allows the neutron beam to pass through the drift chambers of the right-side setup, making low-background measurements close to zero degrees feasible.

In neutron detection mode, each arm consists of a 2 mm thick veto scintillator for fast charged-particle rejection, a neutron-to-proton converter which is a 10 mm thick plastic scintillator, a 2 mm thick plastic scintillator for triggering, two drift chambers for proton tracking, a 2 mm thick ΔE plastic scintillator which is also part of the trigger, and an array of CsI detectors for energy determination. The trigger is provided by a coincidence of the two trigger scintillators, vetoed by the front scintillator. If used for (n,p) studies, the veto and converter scintillators can be removed, and additional drift chambers can be mounted if desired.

In a typical experiment, the two arms will be located such as to cover 10–50°, and 30–70°, respectively. For a one-week run on ^{208}Pb , the total number of counts for a one-degree angular bin is expected to be about 5 000 at 10°, and 1 at 70°, illustrating that the cross section falls off rapidly with angle.

At present, data have been taken on ^{12}C and ^{208}Pb . In addition, ^1H has been studied for normalization purposes. The full resolution is in the 3–4 MeV range.

7.2. Neutron-Induced Charged-Particle Production Reactions

Production of protons and α particles have to be known in an ADS, because protons finally end up as hydrogen, which is explosive and causes embrittlement, and α particles can cause swelling of structural materials, like steel.

Data on (n,xp) reactions are also useful for model development. This is particularly

true for precompound (or preequilibrium, that is essentially the same) reactions. When a low-energy neutron (or whatever particle) hits a nucleus, it is absorbed and moves around inside the compound nucleus in a chaotic fashion. In this process, the incident energy is dissipated on many nucleons in the nucleus, and the "memory" of the incoming particle is lost. Eventually, a particle is emitted, but this can now be described by purely statistical parameters. At much higher energies, the incident neutron interacts with just one single nucleon in a direct reaction, where all the other nucleons act more or less like spectators. This makes reaction theory relatively simpler above about 200 MeV.

There is an intermediate region from, say, 20 to 200 MeV where both direct and compound processes have to be taken into account. Moreover, there are also processes which are some kind of compromises between the two extremes; the incident neutron interacts a few times with a few nucleons. This is called precompound reactions. Direct reactions typically result in emitted particles of high energies, while compound processes produce low-energy ejectiles. Preequilibrium reactions are found somewhere half the way, at intermediate ejectile energies.

For precompound model development, measurements of (n,xp) reactions at these intermediate ejectile energies are therefore of high priority. In real experiments, however, it is even better to cover as complete emission energy ranges as possible. If all secondary particle energies are measured, the interplay between compound, precompound and direct processes can be studied. An example is given in figure 4, where Fe/Pb/U(n,xp) data at 96 MeV neutron energy are displayed [14].

Typically, such measurements are carried out using particle telescopes. Energy determination is not sufficient by itself to know the particle type, but a combined energy loss and full energy measurement is needed. The standard technique is to use a thin ΔE and a thick (stopping) E detector. When you go to higher incident energies, the range of ejectile energies also gets larger, but even more so the range of stopping powers. This makes it very difficult to construct two-detector telescopes which makes a good job all the way. This is because the ΔE detector must be thin enough to allow low-energy particles to punch through, but then the signal from high-energy protons becomes so small that it drowns in the electronic noise. Three-detector telescopes are then a way out of this dilemma. An example of such a facility is MEDLEY [15], which uses eight telescopes, each being a Si-Si-CsI stack. The silicon detectors are about 50 and 500 μm thick, respectively, and the CsI detector is 3 cm thick, sufficient for stopping 100 MeV protons. With this setup, a very wide dynamic range, from 3 MeV α particles up to 100 MeV protons, can be covered.

Such a telescope setup has the advantage that it can provide data of a wide energy and angular range, but with the drawback that the solid angle of each telescope is rather limited, with statistics limitations as a consequence. Recently, a series of (n,xp) experiments have been carried out using both the MEDLEY and SCANDAL setups. SCANDAL has a very large solid angle, and it can be used as proton detector. Its dynamic range is, however, smaller. The combined setup thus produces both wide-range data with moderate statistics, and moderate-range data with superior statistics.

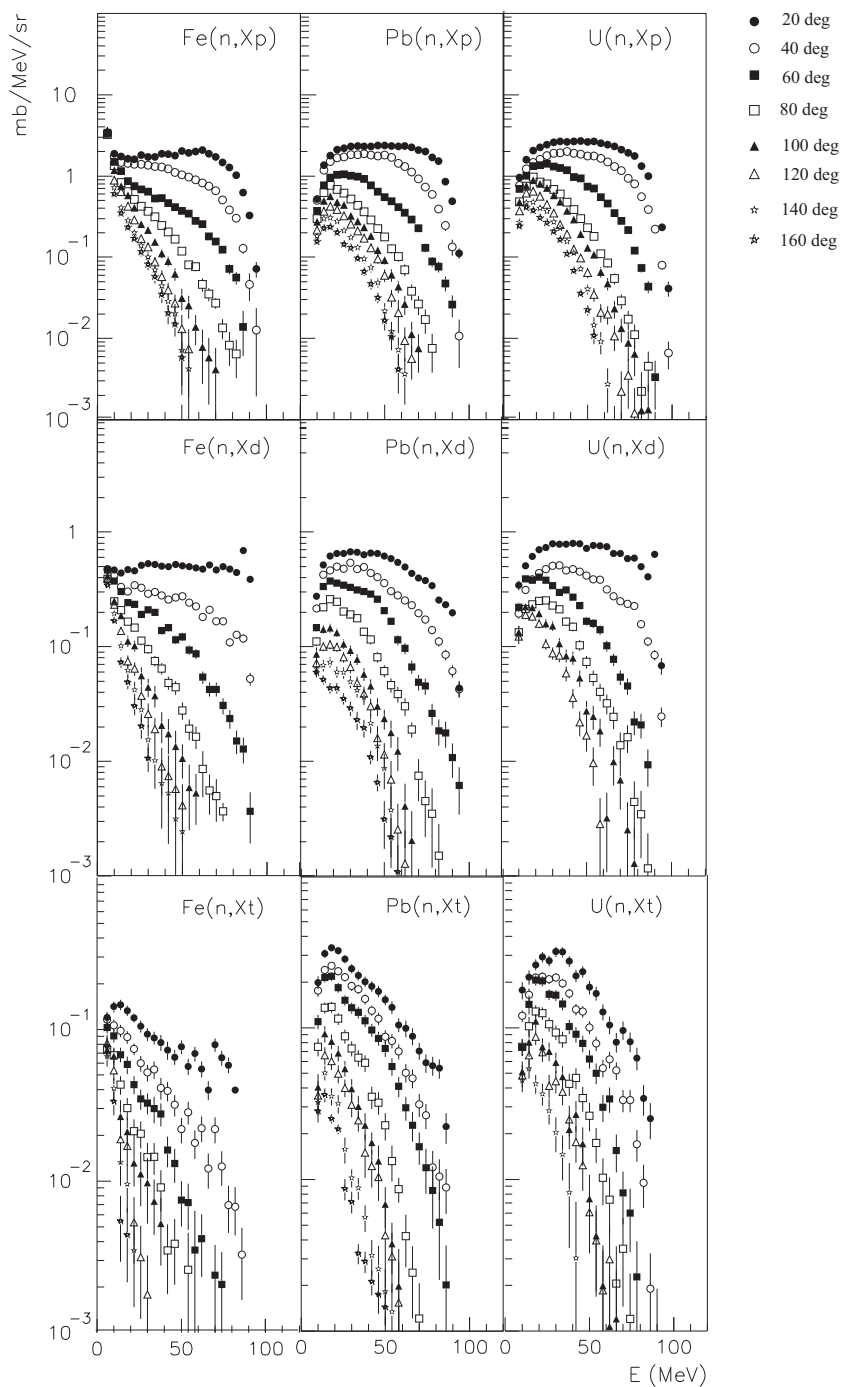


Figure 4. Double differential cross-sections for p, d, t particles (respectively, top, middle and bottom lines) emitted in 96 MeV neutron induced reactions on Fe, Pb and U targets (respectively, left, middle, right rows) [14].

7.3. Proton-Induced Charged-Particle Production Reactions

The motivations for studies of proton-induced charged-particle production reactions are essentially the same as for neutron-induced ditto, however with the extra pre-requisite that measurements in coincidence with low-energy neutrons are of special interest.

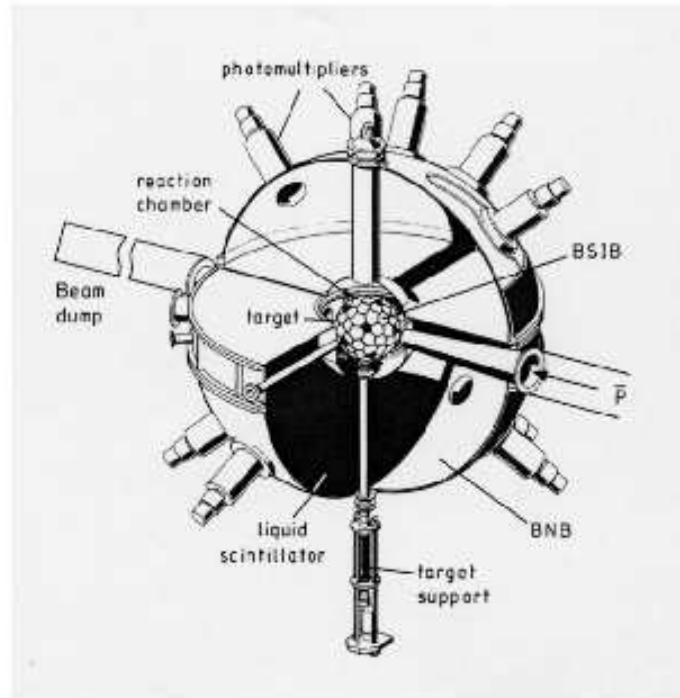


Figure 5. The Berlin ball detector system [16].

This is because one of the weak spots in modelling of ADS is the neutron production cascade in the spallation target. The final result of this cascade has been studied experimentally, by neutron multiplicity measurements. These studies, albeit valuable by their own merits, still do not disentangle the very complex chain of events resulting in neutrons. If the ambition is to get such a full understanding, cross sections for each step in the chain have to be known. If such measurements are carried out in coincidence with neutron multiplicity studies, the possibilities to link the bulk effects and individual cross sections are improved.

A 4π silicon ball for charged-particle detection for incident proton energies up to 2.5 GeV is shown in figure 5. Up to now, it has been used at 0.8, 1.2, 1.8 and 2.5 GeV on several targets [16]. Development of a magnetic spectrometer setup for similar studies is underway.

7.4. Production of Residues

Measurements of elementary reactions, like (p,n) or elastic scattering, can provide very essential information to guide theory development. The ultimate goal would be to have such a successful nuclear theory that all the complex phenomena taking place in an ADS could be modelled based on elementary theory only. This is, however, not the case (yet). Such models are relatively good at describing, e.g., neutron production in the spallation target, but they are notoriously unreliable when it comes to describe the production of residual nuclei, i.e., what is left over after neutron production. Such information is of great technological significance, because the radioactivity of the core after shutdown largely depends on this.

There are two main techniques for measurements of residual nuclide production; measurements of induced activity and inverse kinematics studies. Induced activity studies are typically out-of-beam experiments [17]. They are performed by irradiation of a target, where the radioactive residual nuclei are detected afterwards by, e.g., γ spectrometry. These experiments often require long irradiation time, but since very little technology is needed during the actual irradiation, they can often be run as parasites on other experiments. This is particularly true for neutron beam experiments, where most often only a very small fraction of the beam is consumed by the primary experiment, which allows plenty of parasitic experiments further downstream in the beam. The long irradiation time needed can also be compensated by having many targets exposed simultaneously.

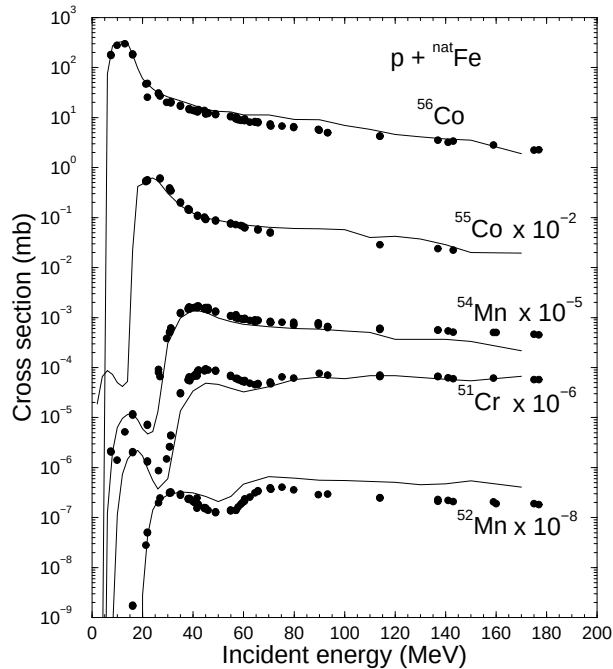


Figure 6. Cross sections for production of various residues for protons on natural iron [18]. The solid lines are predictions by an early version of the TALYS model [19].

After irradiation, short-lived (minutes to years) radioactive nuclides are detected, most often with γ spectrometry using germanium detectors. This technique allows a very large

number of integrated production cross sections to be determined, because all γ -emitting products can be detected. An example is shown in figure 6, where cross sections for production of various residues for protons on natural iron are presented [18].

With this technique, the stable products cannot be detected. This is, however, not such a terrible loss, because most nuclides are radioactive, and these studies are motivated by determining the induced activity. Another restriction is that radioactive nuclei with γ -free decay are missed. An example is ^{90}Sr , which contributes with a significant activity to spent fuel from standard power reactors of today, but it β decays by an electron (and neutrino) only, without γ emission. These restrictions of the method can be overcome by accelerator mass spectrometry (AMS), in which all residual nuclei, radioactive or not, are identified.

In particular for proton irradiation experiments above 200 MeV, a significantly different technique has been developed during the last few years [20]. Instead of sending a 1 GeV proton beam on a lead target, a 1 GeV per nucleon lead beam is impinging on a liquid hydrogen target. With this technique, the spallation products, which often have energies close to zero in standard experiments and therefore stop already inside the target, now move with about 1 GeV per nucleon. Thereby, they can be directly detected with standard techniques used in heavy-ion physics. Combinations of magnetic spectrometers, time-of-flight and energy loss measurements can provide unique identification of the emerging products.

An advantage with this method is that what you detect are the direct products, while in standard irradiation measurements, only the remaining elements after a waiting period can be determined. This makes the inverse kinematics method very useful for theory benchmarking when insight into the reaction mechanism is desired. In addition, it is possible to determine the kinetic energy of the products with this technique, which is of great value for assessment of structural damage in an ADS.

A disadvantage is that only one element can be studied at the time, which is not the case for induced-activity experiments. Moreover, interaction with neutrons cannot be studied directly. Instead, it has to be inferred from studies comparing liquid hydrogen and deuterium targets. Another limitation is that this technique is difficult to use at low energies.

The two techniques for measurements of residual nuclide production are not competitors. Rather, they complement each other and to some extent they can be used to verify results from the other.

7.5. High-Energy Neutron Production

A central issue in ADS is the neutron production at the spallation target. This has been studied using two complementary techniques for the neutron detection. Time-of-flight has been used for low neutron energies, and conversion to protons in combination with a magnetic spectrometer at high energies. Thereby, the choice of techniques resembles the situation for the example of elastic neutron scattering discussed in section 6.3..

With this combination of methods, high-resolution neutron emission spectra from 2 MeV up to the incident energy, has been measured on several targets at 800, 1200 and 1600 MeV. Data on proton-induced neutron production on ^{208}Pb [21] are displayed in figure 7.

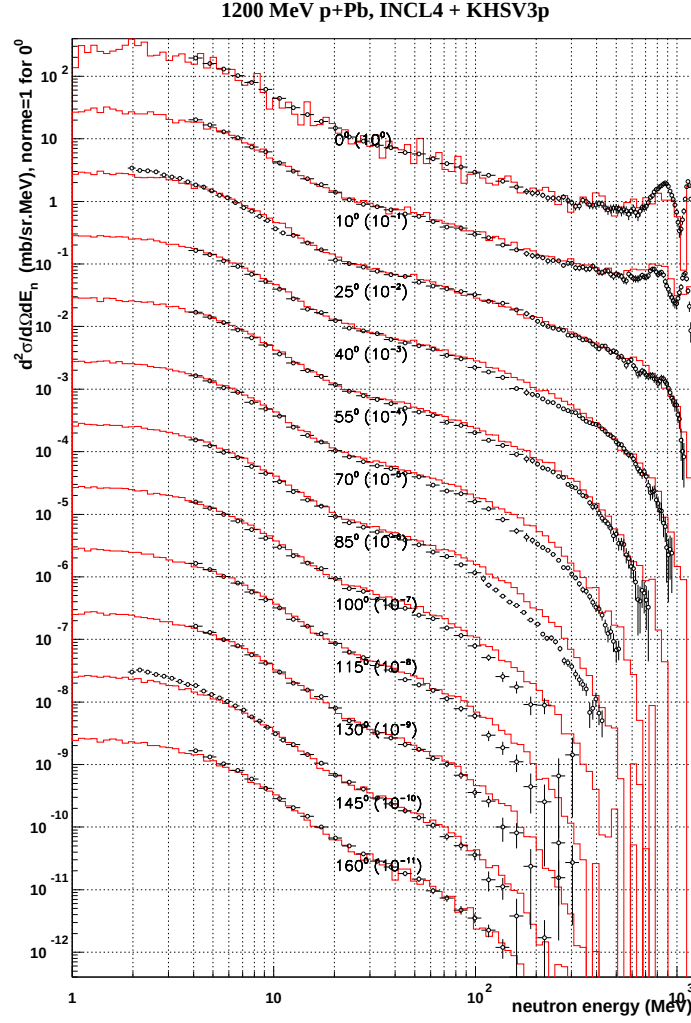


Figure 7. Differential cross sections for the $^{208}\text{Pb}(p,xn)$ reaction [21]. The histograms are calculations with the Liège intra-nuclear cascade (INC) model plus the KHSV3p evaporation-fission model [22, 23].

7.6. Fission

Although being a rather small cross section for all but the heaviest nuclei, fission plays a major role in ADS, even in places where you at first might not think about it. At low energies, fission is restricted to a few fissile nuclei, like ^{235}U , while at higher energies also much lighter nuclei - like lead - have sizeable fission branches. As an example, in a realistic ADS equipped with a lead spallation target irradiated with 1.6 GeV protons, 10–15% of the overall residual activity will be due to fission products after one year of cooling [24].

Among the most common degrees of freedom studied are the fission cross section and the relative yields of different fission products. At low neutron energies, plenty of data exist, while at energies above 50 MeV, only a few data on fission cross sections have been

published, and fission yield data are virtually absent. There are good experimental reasons for this. At thermal energies, the fission process is easy to recognize, because essentially no other reactions producing large nuclear fragments are prevalent. At higher energies, however, effects of, e.g., multiple ion production can produce signals in a detector similar to those of fission fragments. Finding detectors sensitive to fission fragments but insensitive to lighter ions is therefore of high priority in these experiments. Recently, experimental programs on fission cross section measurements using thin-film breakdown counters and ionization chambers have been carried out [25, 26, 27] (see Fig. 8) and experiments with parallel-plate avalanche counters are under development.

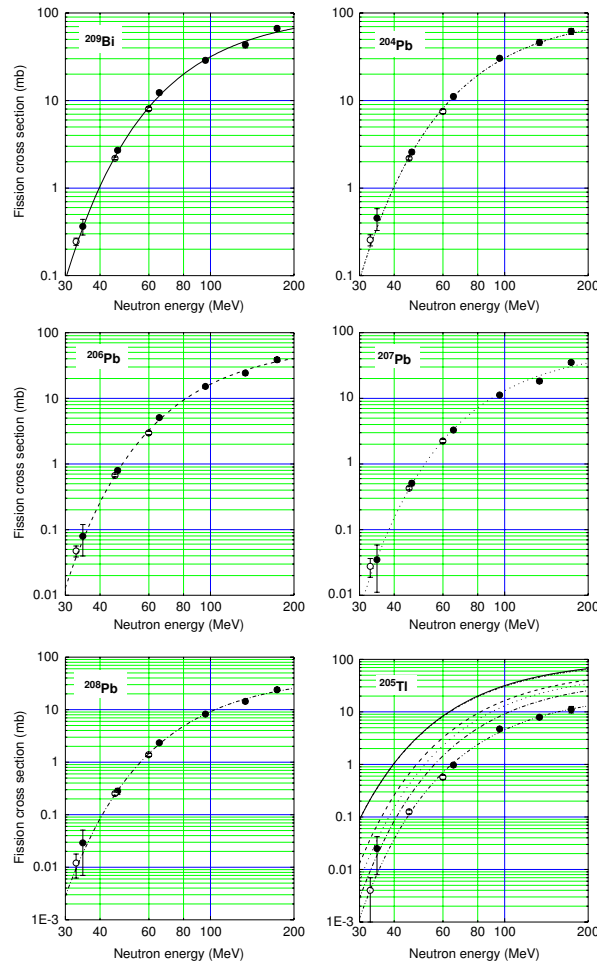


Figure 8. Neutron-induced fission cross sections of ^{205}Tl , $^{204,206-208}\text{Pb}$ and ^{209}Bi measured at the Louvain-la-Neuve (open circles) and TSL (solid circles) neutron beam facilities [27]. The plotted curves are the data fits using a phenomenological function. For an easy comparison, the fits of the Pb and Bi data are reproduced in the ^{205}Tl panel.

8. The Present Experimental Nuclear Data Situation

The experimental efforts described above have resulted in a wealth of new information. In this section, we summarize our view of the present situation. It should already at the onset be stressed that in ADS, the data needs develop over time. Initially, some basic data were needed for the assessment whether ADS was worth continued attention. At present, the nuclear data research is on a level where data needed for the design of a demonstration facility should be the target. If plans for a full-scale plant or a close-to-full-scale prototype mature, the nuclear data needs evolve even further, and if industry deployment becomes a reality, a massive nuclear data campaign will be motivated. For each step, the target accuracies increase. At present, we have experimental data with uncertainties in the 5 % range on about five nuclei for each of the most important nuclear reactions. For successful design of a prototype, half that uncertainty and good data on at least 20 nuclei should be a minimum goal. For industrial-scale employment, uncertainties as low as 1 % for some selected key materials should be reached, and 2-5 % uncertainties would be desirable for a wider range of types of reactions than before. The number of nuclei to be studied would however still stay around 20 or so, because the number of elements where the high-energy neutron flux is sizeable in such a device is fairly limited. Increased attention to cross sections of relevance to shielding and radiation protection could be anticipated though.

It is a fairly limited class of reactions that are of interest for the further development of ADS in the near future. These are elastic scattering, inelastic neutron emission, light ion production, heavy ion production and fission.

Table 1. Summary of the nuclear data situation for the most important neutron-induced nuclear reactions above 20 MeV. See the text for details.

Reaction	Status	Uncertainty
(n,n)	Done up to 100 MeV	5 %
	Can be done up to 200 MeV	
(n,xn')	Underway at 100 MeV	10 %
	Can be done up to 200 MeV	
(n,light ion)	Done up to 100 MeV	5 %
	Up to 200 MeV underway	
(n,f)	Cross sections up to 200 MeV	15 %
	Possible up to 5 GeV	
	Absolute scale problem	
	$d\sigma/d\Omega$, yields, etc. remaining	
	Overall limitation: normalization	5 %

Elastic scattering has been studied on a range of nuclei up to 96 MeV. At present, results on seven nuclei have been published [28]-[30], and an overall uncertainty of about 5 % has been achieved. A novel normalization method has been established that allows

elastic scattering data to be normalized absolutely to about 3 % uncertainty. This method, however, works only for elastic scattering. Feasibility studies have shown that the technique as such works up to about 200 MeV, and preparations for extended studies up to this energy is underway.

An experimental programme on inelastic neutron emission, i.e., (n, xn') reactions, is in progress [31]. Data have been taken on lead and iron, and the method as such seems to work. It is too early to quote a final uncertainty in the results, but 10 % seems feasible.

Data on light ion production has been acquired on about ten nuclei at 96 MeV [14, 32, 33]. Normalization has been obtained by simultaneous detection of np scattering at an angle where the cross section uncertainty can be estimated to about 5 %, which is the dominating uncertainty in the final light ion production cross sections. These studies are presently being extended to 180 MeV [34].

Fission cross sections have been studied at many facilities up to about 200 MeV energy. The energy dependencies of the cross sections agree fairly well in shape, but the absolute scale differs by up to 15 %. It is at present not clear what causes this. One possibility is the normalizations used. Another possible cause is that the sensitivity to low-energy neutrons is not under control for some of the experiments. Dedicated experiments to remedy this situation are underway.

In principle, fission cross sections can be measured up to several GeV using white beams with a very high initial proton energy, like at the CERN-nTOF facility [35]. The neutron beam intensity is very low, but the cross sections are large and it is possible to detect a major fraction of the fission fragments, resulting in reasonable statistical precision. A major problem, however, is normalization, since the beam intensity is very difficult to monitor at these very high energies.

There are only a few examples of other fission data than cross sections. This means that important fission parameters, like angular distributions, yields, etc., essentially remain to be investigated at high neutron energies.

9. Towards Application: From Cross Sections to Nuclear Data

The quality and reliability of the computational simulation of a macroscopic nuclear device is directly related to the quality of the underlying basic nuclear data. The argument that microscopic nuclear experiments and theory development are imperative to enhance the status of applied nuclear research is generally accepted and in fact used by several institutes to construct their scientific program. Maybe less transparent is *how* this microscopic information can actually be applied. This brings us directly to the important task of a nuclear data evaluator, namely the provision of the *quantitative* link between two huge fields of research: fundamental nuclear physics and nuclear applications. This is done by means of a so-called evaluated data file. Often, a data file consists of a mixture of experimental data and calculated results, computed from nuclear reaction models or even simple systematics. As long as the contents of the data file are in correspondence with reality (which we assume is represented by the available experimental data) any combination of these inputs is allowed. This is to serve the common goal of nuclear data evaluation: to store all possible reaction channels on a data file, with maximum quality of the cross sections and other quantities like resonance and fission parameters.

An advantage of this way of working is the modularity from the point of view of human expertise. An evaluator or team of evaluators combines all experimental data and results of relevant nuclear models into one data file per isotope which, after it has been processed by a code like NJOY, is the evaluators' end product. Then, the applied physicist, e.g. somebody involved in reactor analysis, can take this data file as the starting point, and in principle does not need to worry about the trajectory that resulted in this data file. Of course, undesired results from applied calculations frequently leads to feedback to the evaluator, leading to an iterative process that eventually should lead to a nuclear data file that embodies the best compromise between microscopic data and requirements from integral benchmarks. The only, very improbable, alternative approach to the data evaluation method would be one nuclear model supercode that (a) produces better results than all data files combined for all energies and isotopes, and (b) is so fast that it can be hardwired directly in a transport code. Although we all hope that somebody can produce such a miracle, it is very likely that data libraries will remain the key ingredients of applied nuclear simulations in the future. We should mention that much depends on the required accuracy of the nuclear data, i.e. how precise do the basic data need to be for a reasonably accurate determination of the macroscopic quantity (such as k_{eff} or the number of neutrons produced per incident proton in an accelerator-driven system). Sensitivity studies should guide the data evaluator at this point. In sum, since one is in the position to use the best available code or experimental data for each partial nuclear reaction channel, as long as certain sum rules are obeyed, the method of storing nuclear data in evaluated files arguably allows the closest possible connection between nuclear reaction physics and practical applications. After processing, the data libraries can serve as input for deterministic or Monte-Carlo nuclear transport codes and activation codes. For a few decades now, evaluated data files are serving as crucial starting points of various applications.

The starting point of serious computational modeling of a realistic accelerator-driven system is basic nuclear reaction information. Feasibility calculations of such systems, which include items such as neutron and energy balance, the radiotoxicity of spallation products, damage and activation, rely critically on well-tested nuclear data. In this contribution, we aim at classifying the basic nuclear research that is required to fulfill these needs and to demonstrate how such basic nuclear data is connected with an accelerator-driven application. The emphasis will be on nuclear model calculations for thin targets that will have direct influence on more macroscopic components.

Required nuclear data for transmutation (i.e. more general than ADS alone) consists, first and foremost, of low-energy nuclear reaction information: neutron-induced reactions for major and minor actinides in both the thermal and fast energy range, with extra emphasis on minor actinides as compared to conventional reactors. Similarly, nuclear data for fission products require additional effort. The emergence of ADS as a possible solution to the waste problem comes with a whole new class of nuclear data requirements. This consists certainly not only of nuclear data beyond 20 MeV that will be discussed hereafter. Traditionally, most effort for actinides has been put on energies up to several MeV, i.e. including the thermal and fast energy range. Arguably, several (especially minor) actinides of the current data libraries may not be in good shape for energies between 5 and 20 MeV, simply because that energy range has never been application-relevant. For shielding materials, this situation is generally better because of the fusion-related effort of nuclear data up to 14 MeV. ADS also

requires nuclear data for materials that have been somewhat neglected, notably Pb and Bi, because they have, so far, never fitted into any serious applied research program.

For ADS systems, there is an obvious need for nuclear data beyond 20 MeV. The actual provision of these intermediate energy nuclear data to the users is now also developing rapidly. There are basically two ways to provide a link between thin target nuclear reaction physics and applied analyses. The first method, which is traditionally linked with high energies, is to perform the calculation of both microscopic nuclear reactions and macroscopic transport processes by the same computer code. LAHET [36], preferably combined with modern intranuclear cascade codes such as BRIC [37] and INCL4 [38] is a well-known examples of intranuclear cascade codes that work according to this principle. Quality statements about such codes can be obtained by comparison of the results with available thin-target experimental data. Then, after choosing geometry specifications for the accelerator, target and/or the reactor, the code could be set in “production mode” to predict processes that take place in a realistic device. Additional validation is possible using integral experiments, in which the neutron flux and other macroscopic quantities of interest can be measured for a thick target.

The alternative method is by means of evaluated nuclear data files, as outlined in the beginning of the introduction. At present, the intranuclear cascade and data evaluation methods are regarded as complementary valuable approaches for analyses of accelerator-driven systems. The aforementioned effort in fission and fusion reactor studies has resulted in data files that cover nuclear reactions up to 20 MeV. However, the matching energy between the two methods should be around 200 MeV. There are several reasons for this particular energy. First of all, below 200 MeV the predictive power of several pre-equilibrium/statistical model codes (such as the TALYS code described in this contribution) seems to be superior to intranuclear cascade codes for continuum reactions. Also, individual reaction mechanisms (giant resonances, direct collective reactions, etc.) constitute a relatively larger fraction of the reaction spectrum and require an individual, more sophisticated treatment. When results from such detailed reaction mechanisms are collected and included in a data file (and eventually fed to a transport code), they form a data source that in quality can never be matched by one single computer code.

An obvious follow-up question after this enumeration of advantages is: Why should nuclear data files stop at 200 MeV and not go all the way up to the incident energy of the proton beam of an accelerator-driven system. At this point, there are several reasons in favor of intranuclear cascade codes at energies above 200 MeV. Above 150-200 MeV, pion production becomes important and the present low-energy codes (and the ENDF6-format) do not yet cover such reactions. Also, above about 200 MeV we need reliable optical model parameterizations that have predictive power in regions where there is no experimental data. For the moment, intranuclear cascade codes that handle both the cross section generation and the transport part for various types of particles are preferable above 200 MeV.

Summarizing, we argue that a global calculation scheme for accelerator-driven systems should consist of a combination of intranuclear cascade codes above 200 MeV and evaluated data libraries for energies up to 200 MeV. An important development is the integration of the two nuclear data methods (intranuclear cascade + data files/transport) into one code system (MCNPX) [39]. This allows maximal flexibility concerning the choice of intranuclear cascade models and data files.

It is evident that the total nuclear data task is quite sizeable: one has to keep track of nuclear processes starting from 1-1.5 GeV incident protons down to the final reaction stages with thermalizing neutrons and gamma-ray cascades for a large number of residual nuclides. Along this trajectory, a whole panoply of different appropriate nuclear models applies, each with their own validity range. In addition, the required physical methods and modeling may differ from nuclide to nuclide. With regard to the tractability of the work, it is important to observe that certain parts of the accelerator-driven system may depend more critically on the quality of nuclear data, and therefore deserve more attention, than other parts [40]. In addition, the data requirements may differ per material. It can easily be imagined that for a target material like lead, the number of produced neutrons per incident proton is a key quantity, whereas for a shielding material like iron, the secondary particle energy-angle distribution is more relevant (for economical aspects of the shielding) as well as activation cross sections. These simple considerations show that sensitivity studies for the different parts of the whole device are indispensable as a parallel area of research in the field of nuclear data, both at low and intermediate energy. They provide a valuable guideline of isotopes and reactions to be measured and evaluated.

In this contribution, we focus on nuclear data up to 200 MeV. The main emphasis is on the TALYS code, which was developed to simulate nuclear reactions and to generate nuclear data for evaluated libraries.

10. Theory and Nuclear Model Software

Nuclear theory and the associated nuclear model software has become indispensable in modern nuclear data evaluation. To perform adequate inter- and extrapolation on the energy and angular grids per reaction channel, transport and reactor codes rely on a complete description of a nuclear reaction in a data file, and not only on the data that happen to be available through measurements. The preferred method is to let a nuclear model code generate an evaluation that is entirely complete in its description of reaction channels: with incident and outgoing particle energies and angles on a sufficiently dense grid, and all possible reaction yields. The adjustable parameters of the nuclear model code should be fitted to reproduce the experimental data available for the nucleus under study. The starting point is then a complete data file of reasonable, and often good, quality. Next, this evaluation needs to be updated with the crucial experimental data points with a precision that can not be accomplished by the model code. This concerns always the resolved resonance range, and sometimes the unresolved resonance and fast neutron range (especially for light and fissile nuclides). Thus, the model code ensures the completeness of the data file, and the model code + experimental data the highest possible quality, i.e. realistic values, of the data file at a given moment in time. The process needs to be repeated whenever better versions of the model code and more precise experimental data become available. If the methodology is well-automated and properly quality assured, such revisions will take much less time than the first evaluation.

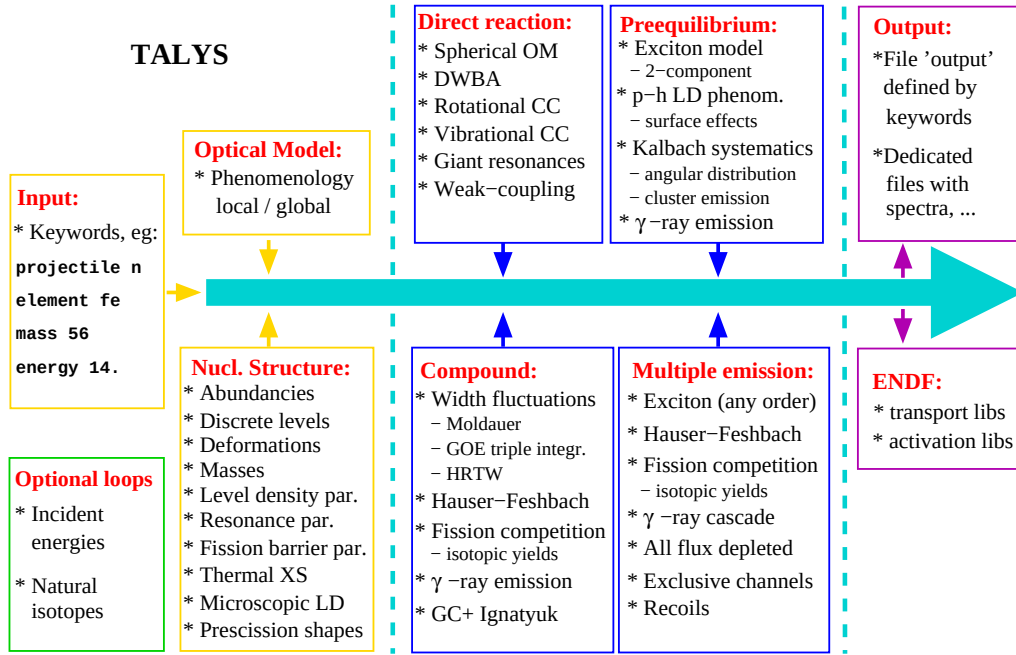


Figure 9. Nuclear models in TALYS.

10.1. TALYS Code

TALYS [41] is a computer code system for the prediction and analysis of nuclear reactions. TALYS simulates reactions that involve neutrons, gamma-rays, protons, deuterons, tritons, helions and alpha-particles, in the 1 keV - 200 MeV energy range and for target nuclides of mass 12 and heavier. This is achieved by implementing a suite of nuclear reaction models into a single code system. It enables to evaluate nuclear reactions from the unresolved resonance region up to intermediate energies. The evaluations described in this paper are based on a theoretical analysis that utilizes the optical model, compound nucleus statistical theory, direct reactions and pre-equilibrium processes, in combination with databases and models for nuclear structure. Fig. 9 summarizes the nuclear models implemented in TALYS. The following output that is produced by TALYS is stored in the nuclear data files:

- Total, elastic and non-elastic cross sections,
- Elastic scattering angular distributions,
- Inelastic scattering cross sections and angular distributions to discrete states,
- Exclusive channel cross sections, e.g. (n,γ) , $(n,2n)$, (n,np) ,..., energy and double-differential spectra,
- Gamma-ray production for discrete states and continuum,
- Isomeric and ground state cross sections,
- Residual production cross sections,

- Total particle cross sections, e.g. (n,xn) , (n,xp) ,..., energy and double-differential spectra.

Besides the ENDF-formatted data file, all data is also produced in easily readable files, per reaction channel, enabling easy graphical and numerical comparison with measurements. In Fig. 9, the label “Input” may give the obviously false impression that the maximal predictive quality can be obtained by simply specifying the nuclear reaction under consideration. Instead, usually various nuclear model parameters, fed to the code by means of keywords, need to be adjusted to produce the optimal results, although the four-line input file generally produces quite reasonable “blind” results.

We now first give a description of the calculation order of the implemented reaction mechanisms and the various nuclear models that are used.

10.2. General Reaction and Calculation Flow

An outline of the general theory and modeling of nuclear reactions can be given in many ways. A common classification is in terms of time scales: short reaction times are associated with direct reactions and long reaction times with compound nucleus processes. At intermediate time scales, pre-equilibrium processes occur. An alternative, more or less equivalent, classification can be given with the number of intranuclear collisions, which is one or two for direct reactions, a few for pre-equilibrium reactions and many for compound reactions, respectively. As a consequence, the coupling between the incident and outgoing channels decreases with the number of collisions and the statistical nature of the nuclear reaction theories increases with the number of collisions. Fig. 10 explains the role of the different reaction mechanisms during an arbitrary nucleon-induced reaction in a schematic manner. The underlying nuclear models will be discussed later.

When discussing nuclear reactions in the context of a computer code, rather than a mathematically formal approach, we think Fig. 11 is an appropriate starting point. A particle incident on a target nucleus will induce several *binary* reactions which are described by the various competing reaction mechanisms that were mentioned above. The end products of the binary reaction are the emitted particle and the corresponding recoiling residual nucleus. In general this is, however, not the end of the process. A total nuclear reaction may involve a whole sequence of residual nuclei, especially at higher energies, resulting from multiple particle emission. All these residual nuclides have their own separation energies, optical model parameters, level densities, fission barriers, gamma strength functions, etc., that must properly be taken into account along the reaction chain. The implementation of this entire reaction chain forms the backbone of TALYS. The program has been written in a way that enables a clear and easy inclusion of all possible nuclear model ingredients for any number of nuclides in the reaction chain. Of course, in this whole chain the target and primary compound nucleus have a special status, since they are subject to *all* reaction mechanisms, i.e. direct, pre-equilibrium, compound and fission and, at low incident energies, width fluctuation corrections in compound nucleus decay. Also, at incident energies below a few MeV, only binary reactions take place and the target and compound nucleus are often the only two nuclei involved in the whole reaction. Historically, it is for the binary reactions that most of the theoretical methods have been developed and refined, mainly because their validity, and their relation with nuclear structure, could best be tested with

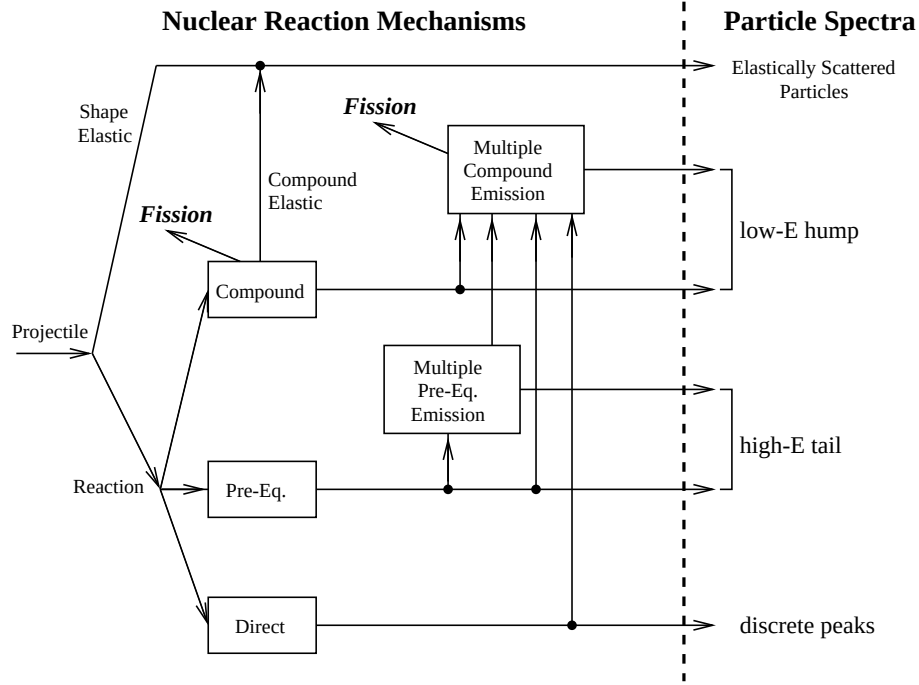


Figure 10. The role of direct, pre-equilibrium and compound processes in the description of a nuclear reaction and the outgoing particle spectra.

exclusive measurements. In general, however, Fig. 11 should serve as the illustration of a total nuclear reaction at any incident energy. The projectile, in this case a neutron, and the target ($Z_C, N_C - 1$) form a compound nucleus (Z_C, N_C) with a total energy

$$E^{tot} = E_{CM} + S_n(Z_{CN}, N_{CN}) + E_x^0, \quad (1)$$

where E_{CM} is the incident energy in the CM frame, S_n is the neutron separation energy of the compound nucleus, and E_x^0 the excitation energy of the target (which is usually zero, i.e. representing the ground state). The compound nucleus is described by a range of possible spin (J) and parity (Π) combinations, which for simplicity are left out of Fig. 11. From this state, transitions to all open channels may occur by means of direct, pre-equilibrium and compound processes. The residual nuclei formed by these binary reactions may be populated in the discrete level part and in the continuum part of the available excitation energy range. In Fig. 11, we have only drawn three binary channels, namely the ($Z_C, N_C - 1$), ($Z_C - 1, N_C$) and ($Z_C - 1, N_C - 1$) nuclei that result from binary neutron, proton and deuteron emission, respectively. Each nucleus is characterized by a separation energy per possible ejectile. If the populated residual nucleus has a maximal excitation energy $E_x^{max}(Z, N)$ that is still above the separation energies for one or more different particles for that nucleus, further emission of these particles may occur and nuclei with lower Z and N will be populated. At the end of the nuclear reaction (left bottom part of Fig. 11), all the reaction population is below the lowest particle separation energy, and the residual nucleus ($Z_C - x, N_C - y$) can only decay to its ground or isomeric states by means of gamma decay. In a computer program, the continuum must be discretized in excitation energy (E_x) bins.

We have taken these bins equidistant, although we already want to stress the important fact here that the *emission* energy grid for the outgoing particles is non-equidistant in TALYS. After the aforementioned binary reaction, every continuum excitation energy bin will be further depleted by means of particle emission, gamma decay or fission. Computationally, this process starts at the initial compound nucleus and its highest energy bin, i.e. the bin just below $E_x^{max}(Z_C, N_C) = E^{tot}$, and subsequently in order of decreasing energy bin/level, decreasing N and decreasing Z . Inside each continuum bin, there is an additional loop over all possible J and Π , whereas for each discrete level, J and Π have unique values. Hence, a bin/level is characterized by the set $\{Z, N, E_x, J, \Pi\}$ and by means of gamma or particle emission, it can decay into all accessible $\{Z', N', E_{x'}, J', \Pi'\}$ bins/levels. In this way, the whole reaction chain is followed until all bins and levels are depleted and thus all channels are closed. In the process, all particle production cross sections and residual production cross sections are accumulated to their final values.

We will now zoom in on the various parts of Fig. 11 to describe the various stages of the reaction, depending on the incident energy, and we will mention the nuclear reaction mechanisms that apply.

10.3. Reaction Mechanisms

In the projectile energy range between 1 keV and several hundreds of MeV, the importance of a particular nuclear reaction mechanism appears and disappears upon varying the incident energy. We will give a short description of the particle decay scheme that typically applies in the various energy regions. Because of the Coulomb barrier for charged particles, it is clear that the discussion for low energy reactions usually concerns incident neutrons. In general, however, what follows can be generalized to incident charged particles. The energy ranges are just meant as helpful indications, which apply for a typical medium mass nucleus.

10.3.1. Low Energies

Elastic scattering and capture ($E < 0.2$ MeV) If the energy of an incident neutron is below the excitation energy of the first inelastic level, and if there are no (n, p) , etc. reactions that are energetically possible, then the only reaction possibilities are elastic scattering, neutron capture and, for fissile nuclides, fission. At these low energies, only the $(Z_C, N_C - 1)$ and (Z_C, N_C) nuclides of Fig. 11 are involved, see Fig. 12. First, the shape (or direct) elastic scattering cross section can directly be determined from the optical model, which will be discussed in Section 10.4.. The compound nucleus, which is populated by a reaction population equal to the reaction cross section, is formed at one single energy $E^{tot} = E_x^{max}(Z_C, N_C)$ and a range of J, Π -values. This compound nucleus either decays by means of compound elastic scattering back to the initial state of the target nucleus, or by means of neutron capture, after which gamma decay follows to the continuum and to discrete states of the compound nucleus. The competition between the compound elastic and capture channels is described by the compound nucleus theory, including width fluctuation corrections, which we will discuss in Section 10.5.. To be precise, the elastic and capture processes comprise the first *binary* reaction. To complete the description of the total reaction, the excited (Z_C, N_C) nucleus, which is populated over its whole excitation energy

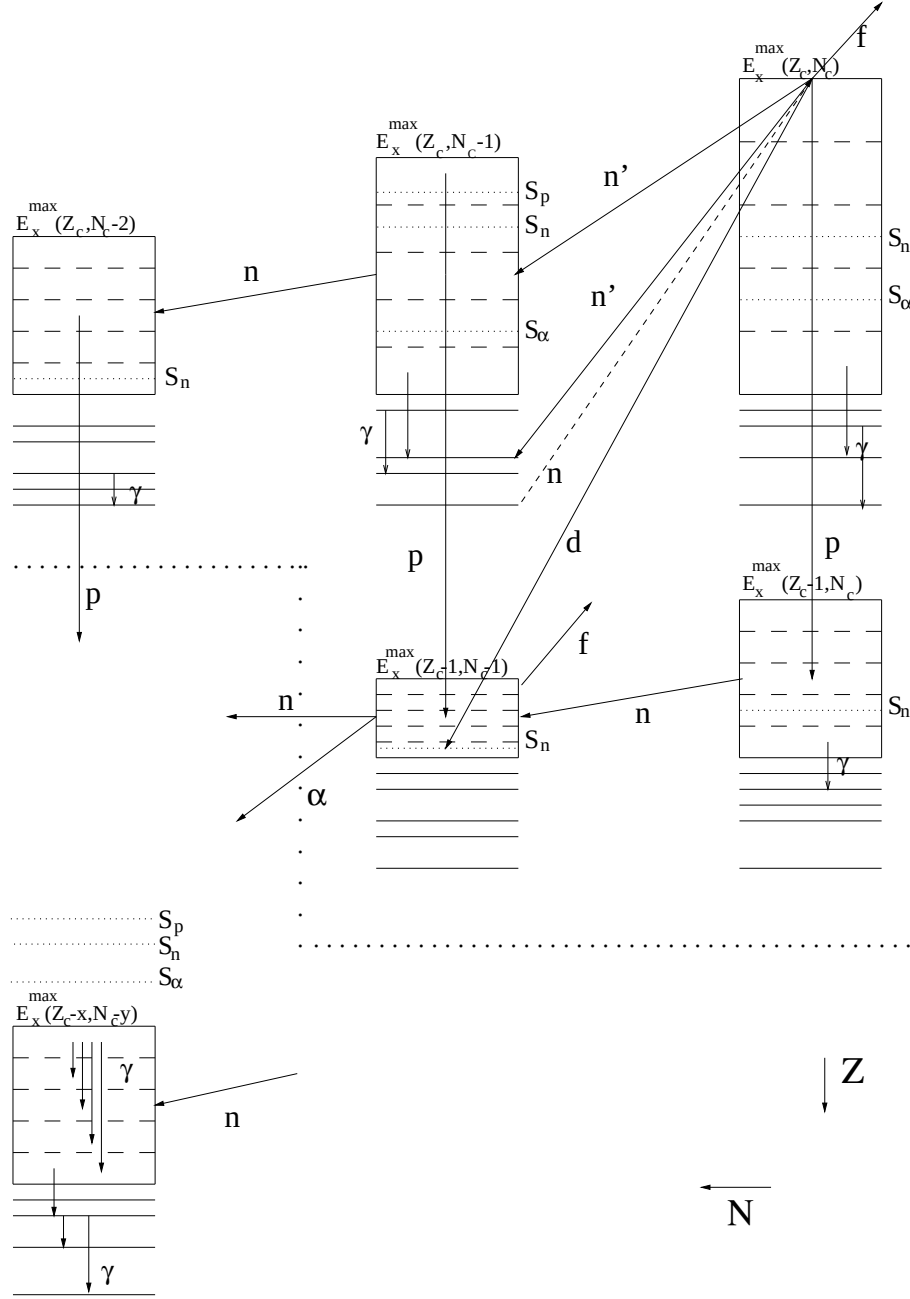


Figure 11. Neutron-induced reaction. The dashed arrow represents the incident channel, while the continuous arrows represent the decay possibilities. $E_x^{\max}$ denotes the maximal possible excitation energy of each nucleus and S_k is the particle separation energy for particle k . For each nucleus a few discrete levels are drawn, together with a few continuum energy bins. Spin and parity degrees of freedom are left out of this figure for simplicity. Fission is indicated by an f .

range by the primary gamma emission, must complete its decay. The highest continuum bin is depleted first, for all J and Π . The subsequent gamma decay increases the population of the lower bins, before the latter are depleted themselves. Also, continuum bins that are above the neutron separation energy S_n of the compound nucleus contribute to the feeding of the $(n, \gamma n)$ channel. This results in a weak continuous neutron spectrum, even though the elastic channel is the only true binary neutron channel that is open. The continuum bins and the discrete levels of the compound nucleus are depleted one by one, in decreasing order, until the ground or an isomeric state of the compound nucleus is reached by subsequent gamma decay.

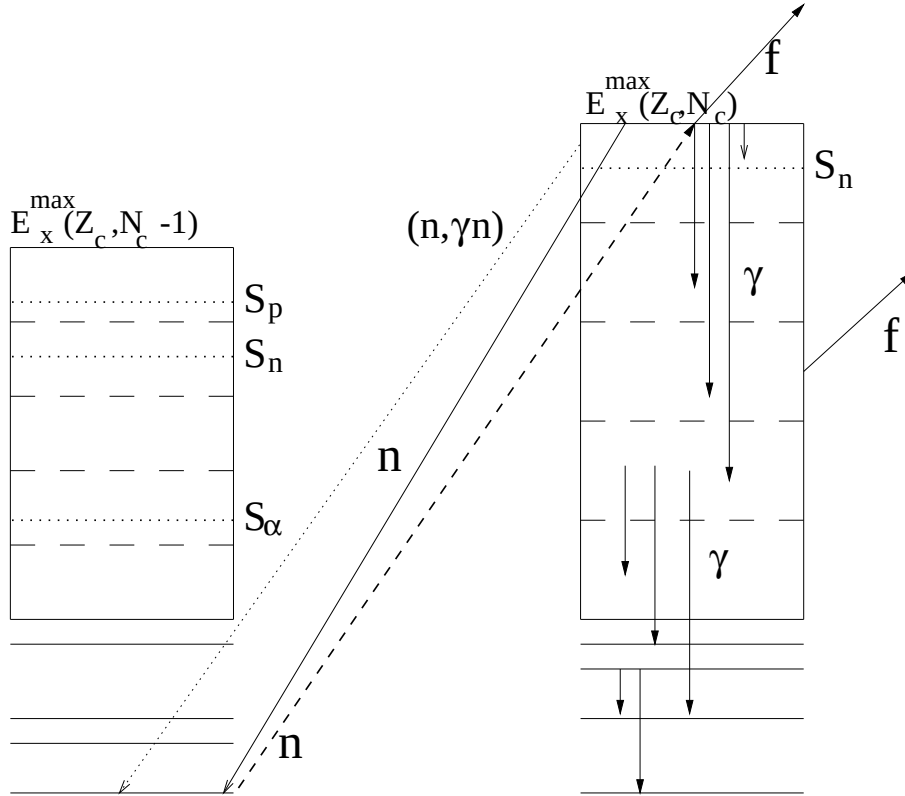


Figure 12. Neutron-induced reaction at low energy. The dashed arrow represents the incident channel, while the continuous arrows represent the elastic channel. The only possibilities are elastic scattering and capture of the neutron in the compound nucleus, with subsequent decay to the ground state or an isomeric state of the compound nucleus. A small part of the population may decay to the target nucleus by means of the $(n, \gamma n)$ channel (dotted arrow). For fissile nuclei, fission may be another open channel.

Inelastic scattering to discrete states ($0.2 < E < 4$ MeV) At somewhat higher incident energies, the first inelastic channels open up, see Fig. 13. Reactions to these discrete levels have a compound and a direct component. The former is again described by the compound nucleus theory, while the latter is described by the Distorted Wave Born Approximation

(DWBA) for spherical nuclei and by coupled-channels equations for deformed nuclei, see Section 10.6.. When the incident energy crosses an inelastic threshold, the compound inelastic contribution rises rapidly and predominates, whereas the direct component increases more gradually. Obviously, the elastic scattering, capture and fission processes described in the previous subsection also apply here. In addition, there is now gamma decay to an isomeric state or the ground state in the target nucleus after inelastic scattering. When there are several inelastic levels open to decay, the compound contribution to each individual level is still significant. However, the effect of the width fluctuation correction on the compound cross section is already small in this case.

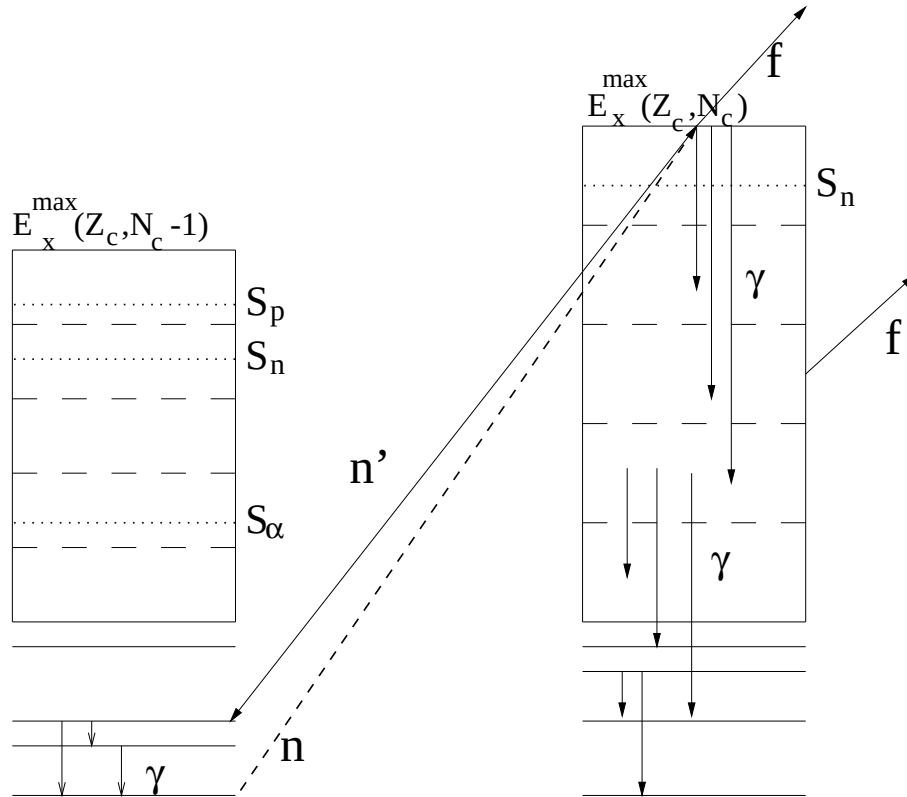


Figure 13. Neutron-induced reaction at somewhat higher energy. The dashed arrow represents the incident channel, while the continuous arrows represent the decay possibilities. In addition to the possibilities sketched in the previous figure, there is now inelastic scattering followed by gamma decay in the target nucleus.

10.3.2. High Energies

Pre-equilibrium reactions ($E > 4$ MeV) At higher incident energies, inelastic cross sections to both the discrete states and the continuum are possible, see Fig. 11. Like reactions to discrete states, reactions to the continuum also have a compound and a direct-like component. The latter are usually described by pre-equilibrium reactions which, by definition, include direct reactions to the continuum. They will be discussed in Section 10.7.. Also

non-elastic channels to other nuclides, through charge-exchange, e.g. (n, p) , and transfer reactions, e.g. (n, α) , generally open up at these energies, and decay to these nuclides can take place by the same direct, pre-equilibrium and compound mechanisms. Again, the channels described in the previous subsections also apply here. In addition, gamma decay to ground and isomeric states of all residual nuclides occurs. When many channels open up, particle decay to individual states (e.g. compound elastic scattering) rapidly becomes negligible. For the excitation of a discrete state, the direct component now becomes predominant, since that involves no statistical competition with the other channels. At about 15 MeV, the *total* compound cross section, i.e. summed over all final discrete states and the excited continuum, is however still larger than the summed direct and pre-equilibrium contributions.

Multiple compound emission ($E > 8$ MeV) At incident energies above about the neutron separation energy, the residual nuclides formed after the first binary reaction contain enough excitation energy to enable further decay by compound nucleus particle emission or fission. This gives rise to multiple reaction channels such as $(n, 2n)$, (n, np) , etc. For higher energies, this picture can be generalized to many residual nuclei, and thus more complex reaction channels, as explained in the introduction of this Section, see also Fig. 11. If fission is possible, this may occur for all residual nuclides, which is known as multiple chance fission. All excited nuclides will eventually decay to their isomeric and ground states.

Multiple pre-equilibrium emission ($E > 40$ MeV) At still higher incident energies, above several tens of MeV, the residual nuclides formed after binary emission may contain so much excitation energy that the presence of further *fast* particles inside the nucleus becomes possible. These can be imagined as strongly excited particle-hole pairs resulting from the first binary interaction with the projectile. The residual system is then clearly non-equilibrated and the excited particle that is high in the continuum may, in addition to the first emitted particle, also be emitted on a short time scale. This so-called multiple pre-equilibrium emission forms an alternative theoretical picture of the intra-nuclear cascade process, whereby now not the exact location and momentum of the particles is followed, but instead the total energy of the system and the number of particle-hole excitations (exciton number). In TALYS, this process can be generalized to any number of multiple pre-equilibrium stages in the reaction by keeping track of all successive particle-hole excitations. For these incident energies, the binary compound cross section becomes small: the non-elastic cross section is almost completely exhausted by primary pre-equilibrium emission. Again, Fig. 11 applies.

10.4. Optical Model

This section shows a few highlights of the extensive optical model study of [42].

The central assumption underlying the optical model is that the complicated interaction between an incident particle and a nucleus can be represented by a complex mean-field potential, which divides the reaction flux into a part covering shape elastic scattering and a part describing all competing non-elastic channels. Solving the Schrödinger equation with

this complex potential yields a wealth of valuable information. First, it returns a prediction for the basic observables, namely the elastic angular distribution and polarisation, the reaction and total cross section and, for low energies, the s , p -wave strength functions and the potential scattering radius R' . The essential value of a good optical model is that it can reliably predict these quantities for energies and nuclides for which no measurements exist. Second, the quality of the not directly observable quantities that are provided by the optical model have an equally important impact on the evaluation of the various reaction channels. Well-known examples are transmission coefficients, for compound nucleus and multi-step compound decay (a pre-equilibrium model), and the distorted wave functions that are used for direct inelastic reactions and for transitions to the continuum that describe statistical multi-step direct reactions. Also, the reaction cross sections that are calculated with the optical model are crucial for the semi-classical pre-equilibrium models.

The phenomenological, local optical model potential $\mathcal{U}$ for incident neutrons and protons is usually defined as:

$$\mathcal{U}(r, E) = -\mathcal{V}_V(r, E) - i\mathcal{W}_V(r, E) - i\mathcal{W}_D(r, E) + \mathcal{V}_{so}(r, E) + i\mathcal{W}_{so}(r, E) + \mathcal{V}_C(r), \quad (2)$$

where $\mathcal{V}_{V,so}$ and $\mathcal{W}_{V,D,so}$ are the real and imaginary components of the volume-central (V), surface-central (D) and spin-orbit (SO) potentials, respectively, and E is the laboratory energy of the incident particle in MeV. All components are separated in E -dependent well depths, V_V , W_V , W_D , V_{so} , and W_{so} , and energy-independent radial parts f , namely

$$\begin{aligned} \mathcal{V}_V(r, E) &= V_V(E)f(r, R_V, a_V), \\ \mathcal{W}_V(r, E) &= W_V(E)f(r, R_V, a_V), \\ \mathcal{W}_D(r, E) &= -4a_D W_D(E) \frac{d}{dr} f(r, R_D, a_D), \\ \mathcal{V}_{so}(r, E) &= V_{so}(E) \left(\frac{\hbar}{m_\pi c} \right)^2 \mathbf{l} \cdot \boldsymbol{\sigma} \frac{1}{r} \frac{d}{dr} f(r, R_{so}, a_{so}), \\ \mathcal{W}_{so}(r, E) &= W_{so}(E) \left(\frac{\hbar}{m_\pi c} \right)^2 \mathbf{l} \cdot \boldsymbol{\sigma} \frac{1}{r} \frac{d}{dr} f(r, R_{so}, a_{so}). \end{aligned} \quad (3)$$

As usual, the form factor $f(r, R_i, a_i)$ is a Woods-Saxon shape

$$f(r, R_i, a_i) = (1 + \exp[(r - R_i)/a_i])^{-1}, \quad (4)$$

where the geometry parameters are the radius $R_i = r_i A^{1/3}$, with A the atomic mass number, and the diffuseness a_i for each component of the potential. For charged projectiles the Coulomb term $\mathcal{V}_C$ is given by

$$\begin{aligned} \mathcal{V}_C(r) &= \frac{Zze^2}{2R_C} \left(3 - \frac{r^2}{R_C^2} \right), \quad \text{for } r \leq R_C \\ &= \frac{Zze^2}{r}, \quad \text{for } r \geq R_C, \end{aligned} \quad (5)$$

with $Z(z)$ the charge of the target (projectile), and $R_C = r_C A^{1/3}$ the Coulomb radius of a uniformly charged sphere.

It is important to note that in Eq. (3) the real and imaginary potentials of each component V and SO share the same form factors, i.e. we assume the same geometry parameters for the pair $(\mathcal{V}_V, \mathcal{W}_V)$ and for the pair $(\mathcal{V}_{so}, \mathcal{W}_{so})$, while $\mathcal{W}_D$ has its own geometry parameters. Moreover, we take each r_i and a_i independent of energy.

For both the local optical models (i.e. per nucleus) and the global optical model (i.e. as a function of mass) we have determined [42] the values for the three pairs of radius and diffuseness and the most appropriate E -dependent parameterization for the various potential depths. In many previous optical model parameterizations, this energy dependence has been modeled by one or more straight line segments. The first derivatives of such straight line parameterizations are discontinuous at the connecting points. Associated anomalies can be observed for transmission coefficients and angular distributions around the connection points, which display an unphysical discontinuous behavior as a function of energy. It is more appropriate to replace these straight line segments by smooth functions that contain the same, and preferably a smaller, number of parameters which at the same time allow more flexibility.

Inspired by previous dispersive optical model analyses, all functional forms for the potential depths depend on $(E - E_f)$, where E_f is the Fermi energy in MeV. The latter is defined as the energy halfway between the last occupied and the first unoccupied shell of the nucleus. For incident neutrons, it is given by

$$E_f^n = -\frac{1}{2}[S_n(Z, N) + S_n(Z, N + 1)], \quad (6)$$

with S_n the neutron separation energy for a nucleus with proton number Z and neutron number N , and for incident protons by

$$E_f^p = -\frac{1}{2}[S_p(Z, N) + S_p(Z + 1, N)], \quad (7)$$

with S_p the proton separation energy. We have used the Audi-Wapstra mass table [43] to obtain these values.

For each (near-)spherical nucleus for which appropriate experimental data exists, we have constructed local neutron and proton optical models for the entire 1 keV - 200 MeV energy region without any discontinuities. In addition, we have constructed a global neutron and proton optical model for the same energy region and for nearly the whole periodic table of elements (more precisely, for $24 \leq A \leq 209$). For the results per nucleus, we refer to the tables of [42]. Here we restrict ourselves to the global optical model potential for both neutrons and protons.

The global neutron OMP for $0.001 \leq E \leq 200$ MeV and $24 \leq A \leq 209$ is given by

$$\begin{aligned} V_V(E) &= v_1^n[1 - v_2^n(E - E_f^n) + v_3^n(E - E_f^n)^2 - v_4^n(E - E_f^n)^3] \text{ (MeV)} \\ W_V(E) &= w_1^n \frac{(E - E_f^n)^2}{(E - E_f^n)^2 + (w_2^n)^2} \text{ (MeV)} \\ r_V &= 1.3039 - 0.4054A^{-1/3} \text{ (fm)} \\ a_V &= 0.6778 - 1.487 \cdot 10^{-4}A \text{ (fm)} \\ W_D(E) &= d_1^n \frac{(E - E_f^n)^2}{(E - E_f^n)^2 + (d_3^n)^2} \exp[-d_2^n(E - E_f^n)] \text{ (MeV)} \end{aligned}$$

$$\begin{aligned}
r_D &= 1.3424 - 0.01585A^{1/3} \text{ (fm)} \\
a_D &= 0.5446 - 1.656 \cdot 10^{-4}A \text{ (fm)} \\
V_{so}(E) &= v_{so1}^n \exp[-v_{so2}^n(E - E_f^n)] \text{ (MeV)} \\
W_{so}(E) &= w_{so1}^n \frac{(E - E_f^n)^2}{(E - E_f^n)^2 + (w_{so2}^n)^2} \text{ (MeV)} \\
r_{so} &= 1.1854 - 0.647A^{-1/3} \text{ (fm)} \\
a_{so} &= 0.59 \text{ (fm)},
\end{aligned} \tag{8}$$

where the parameters for the potential depths and E_f^n are given in Table 2.

The global proton OMP is given by

$$\begin{aligned}
V_V(E) &= v_1^p[1 - v_2^p(E - E_f^p) + v_3^p(E - E_f^p)^2 - v_4^p(E - E_f^p)^3] \\
&\quad + \bar{V}_C \cdot v_1^p \left[v_2^p - 2v_3^p(E - E_f^p) + 3v_4^p(E - E_f^p)^2 \right] \text{ (MeV)} \\
W_V(E) &= w_1^p \frac{(E - E_f^p)^2}{(E - E_f^p)^2 + (w_2^p)^2} \text{ (MeV)} \\
r_V &= 1.3039 - 0.4054A^{-1/3} \text{ (fm)} \\
a_V &= 0.6778 - 1.487 \cdot 10^{-4}A \text{ (fm)} \\
W_D(E) &= d_1^p \frac{(E - E_f^p)^2}{(E - E_f^p)^2 + (d_3^p)^2} \exp[-d_2^p(E - E_f^p)] \text{ (MeV)} \\
r_D &= 1.3424 - 0.01585A^{1/3} \text{ (fm)} \\
a_D &= 0.5187 + 5.205 \cdot 10^{-4}A \text{ (fm)} \\
V_{so}(E) &= v_{so1}^p \exp[-v_{so2}^p(E - E_f^p)] \text{ (MeV)} \\
W_{so}(E) &= w_{so1}^p \frac{(E - E_f^p)^2}{(E - E_f^p)^2 + (w_{so2}^p)^2} \text{ (MeV)} \\
r_{so} &= 1.1854 - 0.647A^{-1/3} \text{ (fm)} \\
a_{so} &= 0.59 \text{ (fm)} \\
r_C &= 1.198 + 0.697A^{-2/3} + 12.994A^{-5/3} \text{ (fm)},
\end{aligned} \tag{9}$$

where the parameters for the potential depths, $\bar{V}_C$ and E_f^p are given in Table 3. Note that the functional form of the proton global OMP differs from the neutron global OMP only by the Coulomb correction term in $V_V(E)$.

As an example, we present the predicted results of the neutron optical models for the energy range 0-200 MeV for the total cross section of a few nuclides in Fig. 14.

All optical model calculations in TALYS are performed with the versatile coupled-channels code ECIS97 [44], which we have included as a subroutine. Apart from the basic cross sections and angular distributions, the most important quantities provided by the optical model are the transmission coefficients $T_{\alpha l j}^J(E_a)$ that we need for compound nucleus calculations.

Table 2. Potential depths and Fermi energy for the neutron global OMP of Eq. (8).

v_1^n	$= 59.30 - 21.0(N - Z)/A - 0.024A$	MeV
v_2^n	$= 0.007228 - 1.48 \cdot 10^{-6}A$	MeV^{-1}
v_3^n	$= 1.994 \cdot 10^{-5} - 2.0 \cdot 10^{-8}A$	MeV^{-2}
v_4^n	$= 7 \cdot 10^{-9}$	MeV^{-3}
w_1^n	$= 12.195 + 0.0167A$	MeV
w_2^n	$= 73.55 + 0.0795A$	MeV
d_1^n	$= 16.0 - 16.0(N - Z)/A$	MeV
d_2^n	$= 0.0180 + 0.003802/(1 + \exp[(A - 156.)/8.])$	MeV^{-1}
d_3^n	$= 11.5$	MeV
v_{so1}^n	$= 5.922 + 0.0030A$	MeV
v_{so2}^n	$= 0.0040$	MeV^{-1}
w_{so1}^n	$= -3.1$	MeV
w_{so2}^n	$= 160.$	MeV
E_f^n	$= -11.2814 + 0.02646A$	MeV

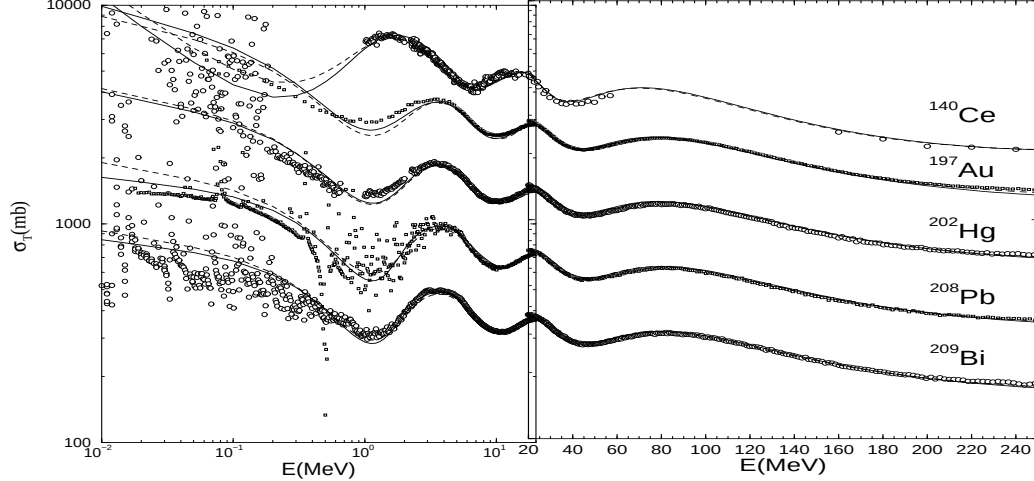


Figure 14. Comparison of predicted neutron total cross sections and experimental data (symbols), for nuclides in the Ce-Bi mass region, for the energy range 10 keV-250 MeV. The solid line represents the local OMPs of [42] while the dashed line represents the global OMP of Eq. (8). The curves and data points for ^{140}Ce represent true values, the others are offset by a factor of 2. The experimental cross sections below 4 MeV have been averaged over logarithmically equidistant energy bins in order to reduce the number of points.

Table 3. Potential depths and Fermi energy for the proton global OMP of Eq. (9). The parameter values for neutrons are given in Table 2. $\bar{V}_C$ appears in the Coulomb correction term $\Delta V_C(E)$, of the real central potential.

v_1^p	$= 59.30 + 21.0(N - Z)/A - 0.024A$	MeV
v_2^p	$= 0.007067 + 4.23 \cdot 10^{-6} A$	MeV ⁻¹
v_3^p	$= 1.729 \cdot 10^{-5} + 1.136 \cdot 10^{-8} A$	MeV ⁻²
v_4^p	$= v_4^n$	MeV ⁻³
w_1^p	$= 14.667 + 0.009629A$	MeV
w_2^p	$= w_2^n$	MeV
d_1^p	$= 16.0 + 16.0(N - Z)/A$	MeV
d_2^p	$= d_2^n$	MeV ⁻¹
d_3^p	$= d_3^n$	MeV
v_{so1}^p	$= v_{so1}^n$	MeV
v_{so2}^p	$= v_{so2}^n$	MeV ⁻¹
w_{so1}^p	$= w_{so1}^n$	MeV
w_{so2}^p	$= w_{so2}^n$	MeV
E_f^p	$= -8.4075 + 0.01378A$	MeV
$\bar{V}_C$	$= 1.73/r_C \cdot Z \cdot A^{-1/3}$	MeV

10.5. Compound Nucleus Reactions

The term compound nucleus reaction is commonly used for two different processes: (i) capture of the projectile in the target nucleus to form a compound nucleus, which subsequently emits a particle or gamma, (ii) multiple emission from the chain of excited, equilibrated residual nuclides following the binary reaction. Both are needed for a complete nuclear reaction description and are thus implemented in TALYS. At low incident energy (i) plays an important role. It differs from (ii) at two important points: (a) the presence of width fluctuation corrections in the compound nucleus expression and (b) non-isotropic, though still symmetric, angular distributions. We use Moldauer's width fluctuation model [45] for these low-energy reactions since that model was shown to be the best [46, 47] for practical calculations. In our implementation, the model yields the compound cross section for all discrete levels and the continuum as well as the elastic and inelastic angular distribution for all levels. The full mathematical framework is given in Refs. [48, 49, 50].

The important ingredients for the compound nucleus model are transmission coefficients and level densities.

The particle transmission coefficients emerge from the optical model. Similarly, gamma-ray transmission coefficients enter the compound nucleus model for the calcula-

tion of the capture cross section.

The gamma-ray transmission coefficients $T_{X\ell}$ for multipolarity ℓ of type X (where $X = M$ or E) are calculated through the energy-dependent gamma-ray strength function according to Kopecky and Uhl [51], for $E1$ radiation, or Brink-Axel [52], for all transition types other than $E1$. Again, the full mathematical details of the associated expressions are given in Ref. [48, 49, 50]. Here, we only mention that we renormalize the gamma-ray transmission coefficients [53] through the (often available) average radiative capture width Γ_γ , by integrating the gamma-ray transmission coefficients over the density of final states that may be reached in the first step of the gamma-ray cascade,

$$\frac{2\pi\Gamma_\gamma}{D_0} = G_{norm} \sum_J \sum_\Pi \sum_{X\ell} \sum_{I'=\lceil J-\ell \rceil}^{J+\ell} \sum_{\Pi'} \int_0^{S_n} dE_\gamma T_{X\ell}(E_\gamma) \rho(S_n - E_\gamma, I', \Pi') \times f(X, \Pi', \ell). \quad (10)$$

where S_n is the neutron separation energy and D_0 is the mean resonance spacing. Here, the J and Π sums are over the compound nucleus states with spin J and parity Π that can be formed with s -wave incident particles, and I' and Π' denote the spin and parity, respectively, of the final states. The multipole selection rules are $f(E, \Pi', \ell) = 1$ if $\Pi = \Pi'(-1)^\ell$, $f(M, \Pi', \ell) = 1$ if $\Pi = \Pi'(-1)^{\ell+1}$, and 0 otherwise. It is understood that the integral over dE_γ includes a summation over discrete states. G_{norm} is the normalisation factor that ensures the equality (10). In practice, the transmission coefficients are thus multiplied by G_{norm} before they enter the nuclear reaction calculation. To obtain the best possible fit for the measured capture cross sections, i.e. if Eq. (10) does not suffice, we use G_{norm} as the adjustable parameter.

Finally, we require photo-absorption cross sections, for pre-equilibrium gamma-ray emission among others, at higher energies than provided by the above formalism. For this we use the expression of Chadwick et al. [54], in which the photo-absorption cross section can be written as a sum over a giant dipole part and a quasi-deuteron part.

For the level density ρ that is required in the compound nucleus formula we take a combination [55] of the constant temperature model at low energies and the Fermi gas model at high energies.

In the Fermi gas expression, we use the level density parameter a which is energy-dependent and takes into account the damping of shell effects at high excitation energy [56],

$$a(E_x) = \tilde{a} \left[1 + \delta W \frac{1 - \exp(-\gamma U)}{U} \right]. \quad (11)$$

Here, δW is the shell correction energy in MeV which we take as the difference between the real mass of the nucleus and the mass according to the spherical liquid drop model as given by Myers and Swiatecki [57]. The asymptotic level density value $\tilde{a}$ is given by

$$\tilde{a} = \alpha A + \beta A^{2/3}, \quad (12)$$

in which we generally use the parameters

$$\alpha = 0.06926, \beta = 0.2828, \quad (13)$$

which we have obtained from a simultaneous fit of the associated level density to all average resonance spacing parameters D_0 of the Reference Input Parameter Library [58].

From the same fit, the following systematical formula for the shell damping parameter γ is obtained,

$$\gamma = \frac{0.433}{A^{\frac{1}{3}}}. \quad (14)$$

At low excitation energies, i.e. below some matching energy E_x^{match} , the constant temperature total level density ρ_T^{tot} reads

$$\rho_T^{\text{tot}}(E_x) = \frac{1}{T} \exp\left(\frac{E_x - E_0}{T}\right). \quad (15)$$

By matching this expression with the Fermi gas expression, and similarly for the corresponding derivatives, E_x^{match} , the temperature T and energy shift E_0 are solved, under the extra condition that Eq. (15) yields the best possible fit of the cumulative discrete levels of the nucleus. Then, a smooth level density over the entire energy range is obtained.

10.6. Direct Reactions

For the fast interaction of the projectile and the nucleus various models exist, which depend on the shape of the nucleus. If the nucleus is deformed, coupled-channels calculations need to be performed. For spherical nuclides, the so called Distorted Wave Born Approximation (DWBA) can be used. In Section 11., we take the Pb isotopes as an example, which are of a (near-)spherical nature. The even-even nuclides considered in this paper, ^{204}Pb , ^{206}Pb , and ^{208}Pb are of a vibrational nature. This means they possess a spherically symmetric ground state and their excited states undergo shape oscillations about the spherical equilibrium model. For such nuclides, one can include only one-phonon states in the harmonic vibrational model and moreover use DWBA. The interaction between the projectile and the target nucleus is then modeled by the derivative of the optical model potential for elastic scattering times the square of the deformation parameter, β_λ^2 .

For direct inelastic scattering off odd nuclei, e.g. ^{207}Pb or ^{209}Bi , one can use the weak-coupling model [59], which assumes that a valence particle or hole interacts only weakly with a collective core excitation.

The DWBA calculations also provide the direct inelastic scattering angular distributions, or equivalently the Legendre coefficients, which are added to their compound counterparts to give the total angular distribution per discrete state.

A third included model for direct reactions is for collective excitations of the nucleus that are known as giant resonances [60, 61].

10.7. Pre-equilibrium Reactions

At incident energies above about 10 MeV, a significant part of the reaction flux is emitted in the pre-equilibrium stage, i.e. it takes place after the first stage of the reaction but long before statistical equilibrium of the compound nucleus is attained. It is imagined that the incident particle step-by-step creates more complex states in the compound system and gradually loses its memory of the initial energy and direction. The default pre-equilibrium model of TALYS is the two-component exciton model which has been tested against basically all available experimental nucleon spectra for $A > 24$ [62].

The partial level density parameters used in the model are $g_\pi = Z/15$ and $g_\nu = N/15$.

The models also contains the adjustable transition matrix element M^2 for each possible transition between neutron-proton exciton configurations. A proton-neutron ratio of 1.6 for the squared internal transition matrix elements was adopted to give the best overall agreement with experiment, i.e., $M_{\pi\nu}^2 = M_{\nu\pi}^2 = 1.6M_{\pi\pi}^2 = 1.6M_{\nu\nu}^2 (= 1.6M^2)$, while we use the following expression for the matrix element,

$$M^2 = \frac{1}{A^3} \left[6.8 + \frac{4.2 \times 10^5}{(\frac{E^{\text{tot}}}{n} + 10.7)^3} \right]. \quad (16)$$

For pre-equilibrium reactions involving deuterons, tritons, ^3He and alpha particles, a contribution from the exciton model is automatically included. It is, however, well known that for nuclear reactions involving projectiles and ejectiles with different particle numbers, mechanisms like stripping, pick-up and knock-out play an important role and these direct-like reactions are not covered by the exciton model. Therefore, Kalbach [63] developed a phenomenological contribution for these mechanisms, which is included in TALYS. It has recently been shown (see Ref. [63], but also e.g. Table I of Ref. [64]) that this method gives a considerable improvement over the older methods. The latter seemed to consistently underpredict neutron-induced reaction cross sections.

Semi-classical models, such as the exciton model, have always had some problems to describe angular distributions (essentially because it is based on a compound-like concept instead of a direct one). Therefore, as mentioned previously, the double-differential cross sections are obtained from the calculated energy spectra using the Kalbach systematics [65].

10.8. Multiple Emission

At incident energies above approximately 7-10 MeV (the neutron separation energy), the residual nuclides formed after the first binary reaction are left with enough excitation energy to enable further decay by particle emission. This is called multiple emission. We distinguish between two mechanisms: multiple Hauser-Feshbach (compound) decay and multiple pre-equilibrium decay.

For incident energies up to several tens of MeV, Hauser-Feshbach decay is sufficient to treat multiple emission. For these energies, it can be assumed that pre-equilibrium processes only take place in the binary reaction and that secondary and further particles are emitted by compound emission. After the binary reaction, the residual nucleus may be left in an excited discrete state or an excited state within a bin which is characterized by excitation energy, spin and parity. Hence, the only differences between binary and multiple compound emission are that width fluctuations and angular distributions do not enter the model and that the initial compound nucleus energy is replaced by an excitation energy bin of the mother nucleus. With this scheme, TALYS follows all reaction chains until all emission channels are closed, while the spectra of all emitted particles that are produced along the chain are recorded in the appropriate arrays.

If the residual nucleus has a high excitation energy, resulting from a binary reaction with a high incident energy, this nucleus is far from equilibrated and it should be described by more degrees of freedom than just excitation energy, spin and parity. In general, we will

keep track of the particle-hole configurations that are excited throughout the reaction chain and thereby calculate multiple pre-equilibrium emission up to any order within the exciton model. The multiple pre-equilibrium emission is described by Ref. [62].

11. An Example: Pb

All nuclear models and quantities described in the previous section can be put into practice, with a single TALYS calculation, to produce all results that can be compared with measurements. Here, we take ^{208}Pb and ^{209}Bi as example, plus a few results for the other Pb isotopes. On the basis of the whole collection of available experimental data, the nuclear model parameters used in TALYS can be adjusted, as specified before, until the optimal description of experimental data was obtained. It is then advised to perform all isotopic evaluations simultaneously, both for incident neutrons and protons. This enables, when compared with a single-isotope effort, to put stronger constraints on the produced calculated data, i.e. a globally good comparison between TALYS and experimental data is requested for all isotopes at the same time, while nucleus-specific input (default or adjusted) parameters are consistently used for all isotopes. Also, experimental data that is not available for the isotope under study may be present, and tested, for adjacent nuclides or for other projectiles. If these can be successfully described by the models, a similar performance can often be expected for the projectile/isotope under consideration.

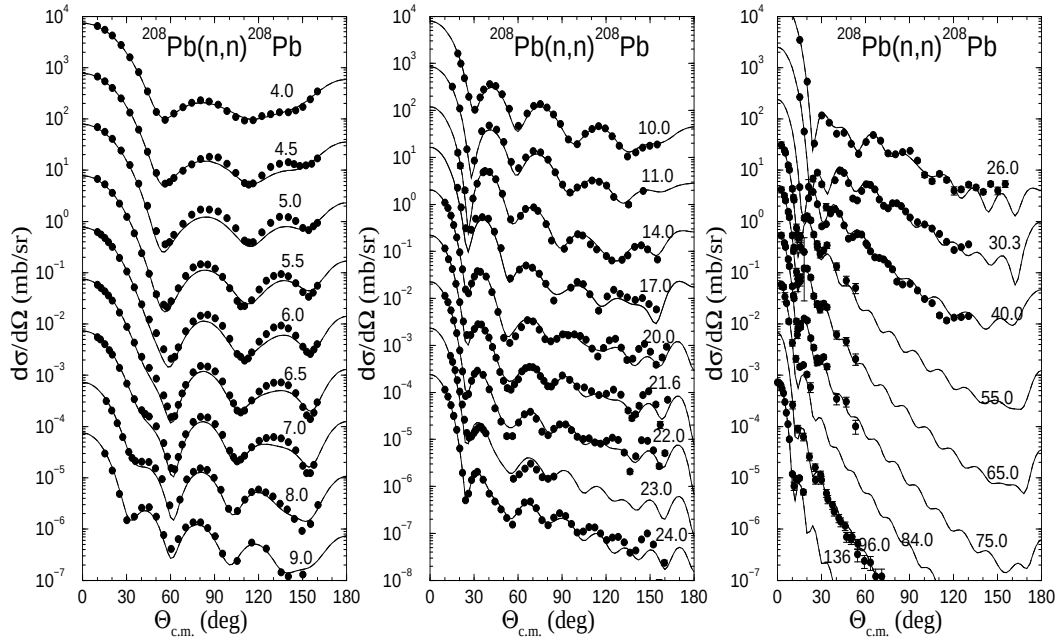


Figure 15. Comparison of predicted differential cross sections and experimental data, for neutrons scattered from ^{208}Pb . The incident laboratory energies are indicated in MeV. The curves and data points at the top represent true values, while the others are offset by factors of 10, 100, etc.

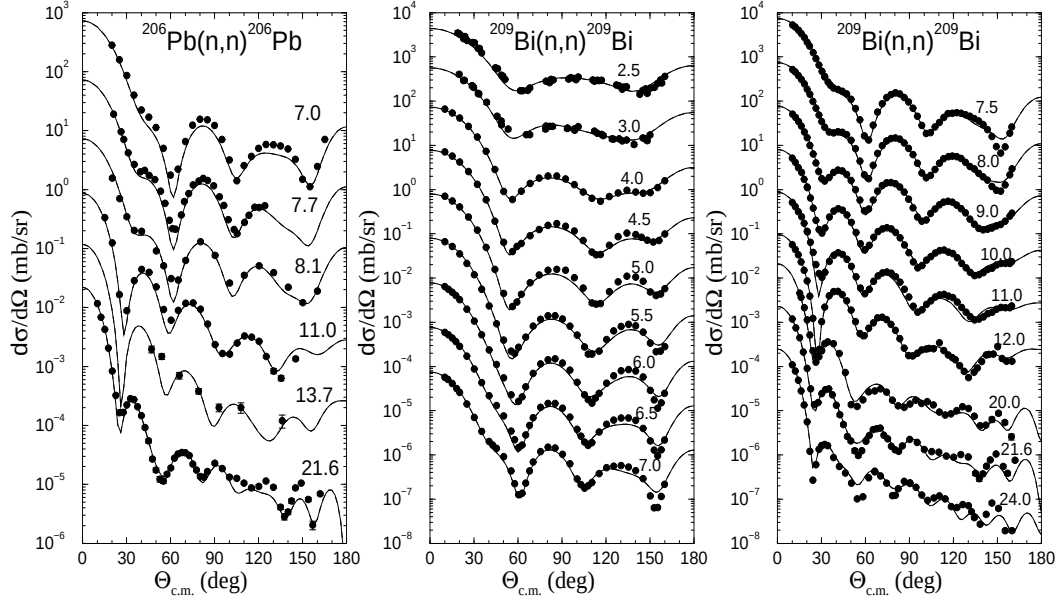


Figure 16. Comparison of predicted differential cross sections and experimental data, for neutrons scattered from ^{206}Pb and ^{209}Bi . The incident laboratory energies are indicated in MeV. The curves and data points at the top represent true values, while the others are offset by factors of 10, 100, etc.

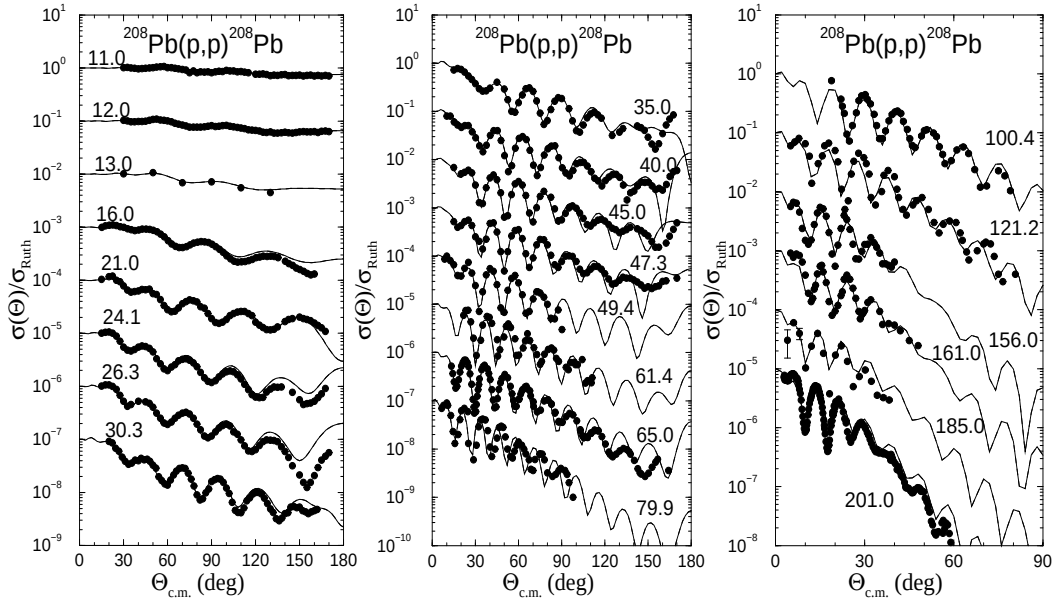


Figure 17. Comparison of predicted differential cross sections and experimental data, for protons scattered from ^{208}Pb . The incident laboratory energies are indicated in MeV. The curves and data points at the top represent true values, while the others are offset by factors of 10, 100, etc. The differential cross sections are presented as ratios to the Rutherford cross sections.

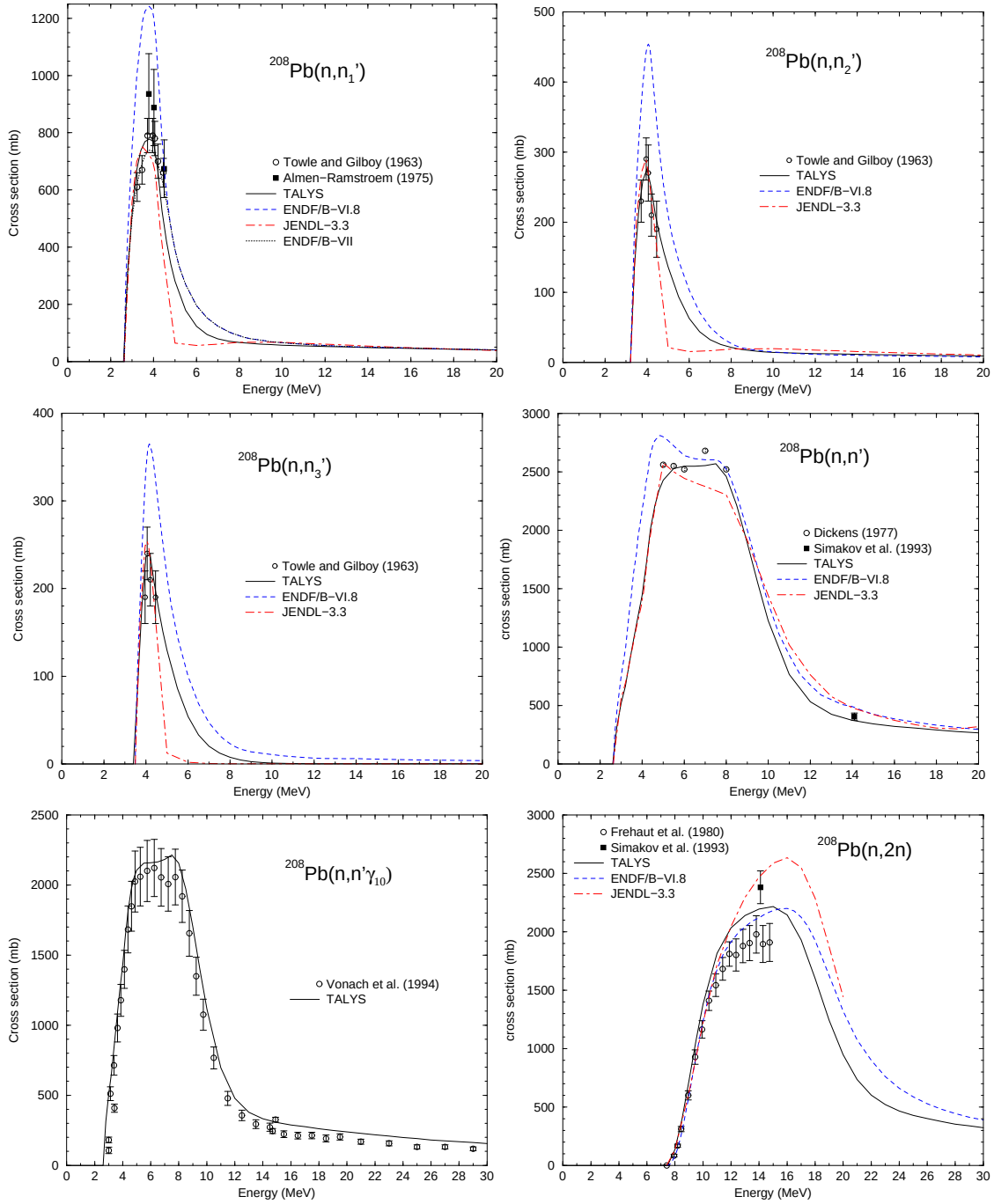


Figure 18. Cross sections for ^{208}Pb : Inelastic scattering to the first level, second level, third level level, total inelastic scattering, gamma-ray production for the first level to the ground state, and (n,2n).

11.1. Total Cross Sections and Elastic Scattering Angular Distributions

In Section 10.4., we already outlined the optical model potential we are using and showed the results for several isotopes in Fig. 14. Here, we also give a comparison of all available neutron elastic scattering distributions, see Figs. 15 and 16. An extra asset, compared to Ref. [42], is that the new 96 MeV neutron elastic scattering results from Uppsala [28] are now available. The comparison as given in the right panel of Fig. 15 confirms the findings of Ref. [42] that an adequate description of the angular distribution can be obtained with an optical model such as that given in this paper. Besides this recent measurement, all references for the experimental data can be found in the tables of Ref. [42]. As already observed in that paper, for ^{208}Pb our results underestimate the last oscillation in angular distributions between 4 and 8 MeV. At the other energies, good agreement is obtained over the entire angular range.

For incident protons, the results for the angular distributions $\sigma(\theta)/\sigma_{Ruth}$ are plotted in Fig. 17. Our OMP underestimates the total reaction cross section below about 25 MeV [42], while our description of $\sigma(\theta)/\sigma_{Ruth}$ is good in general. There are isolated phasing problems at 79.9 MeV, an overestimation at backward angles between 24 and 35 MeV and too much structure at backward angles between 40 and 50 MeV.

11.2. Inelastic Cross Sections, Photons and Angular Distributions

The description of inelastic scattering is driven by the optical model, the compound nucleus model and, at incident energies above a few MeV, level densities and the pre-equilibrium model. In Fig. 18, a comparison between our new results, experimental data and the ENDF/B-VI.8 and JENDL-3.3 data libraries is given for the lowest three discrete levels of ^{208}Pb . There are significant differences between these data libraries and our results. A consistent trend is visible: The ENDF/B-VI.8 excitation function is rather high and overestimates the experimental data in the peak of the excitation curve (this has been corrected [66], for the first discrete level only, in a preliminary version of the new ENDF/B-VII file for ^{208}Pb). In the peak, the JENDL-3.3 results are close to our results and also to the available experimental data. However, the JENDL-3.3 curve does not exhibit a smooth fall-off with increasing energy, while theory predicts that for ^{208}Pb around 5-6 MeV the direct component of inelastic scattering should, after a gradual increase, predominate the compound component. The sum of the two should result in a smooth curve, unlike the discontinuous behaviour of the JENDL curve. An independent test of the quality of inelastic scattering cross sections comes from gamma-ray production cross sections from specific levels. Fig. 18 shows the $^{208}\text{Pb}(n,n'\gamma)$ cross section for decay of the first discrete level to the ground state, as measured by Vonach et al [67]. For incident energies below about 3.5 MeV, these results should coincide with that of an inelastic neutron scattering measurement. At higher energies, inelastic scattering to higher levels and the subsequent gamma-ray cascade contributes to this $(n,n'\gamma)$ cross section. The good prediction of this $^{208}\text{Pb}(n,n'\gamma)$ cross section gives us confidence that we provide a good estimate for the inelastic neutron scattering channels. An extensive collection of inelastic gamma ray data for Pb and Bi isotopes is currently under analysis at IRMM Geel [68]. This will provide a further test of our model. Another independent comparison can be made with the total inelastic cross section, see Fig. 18. Note that the same trends as for the individual levels are visible: The ENDF/B-

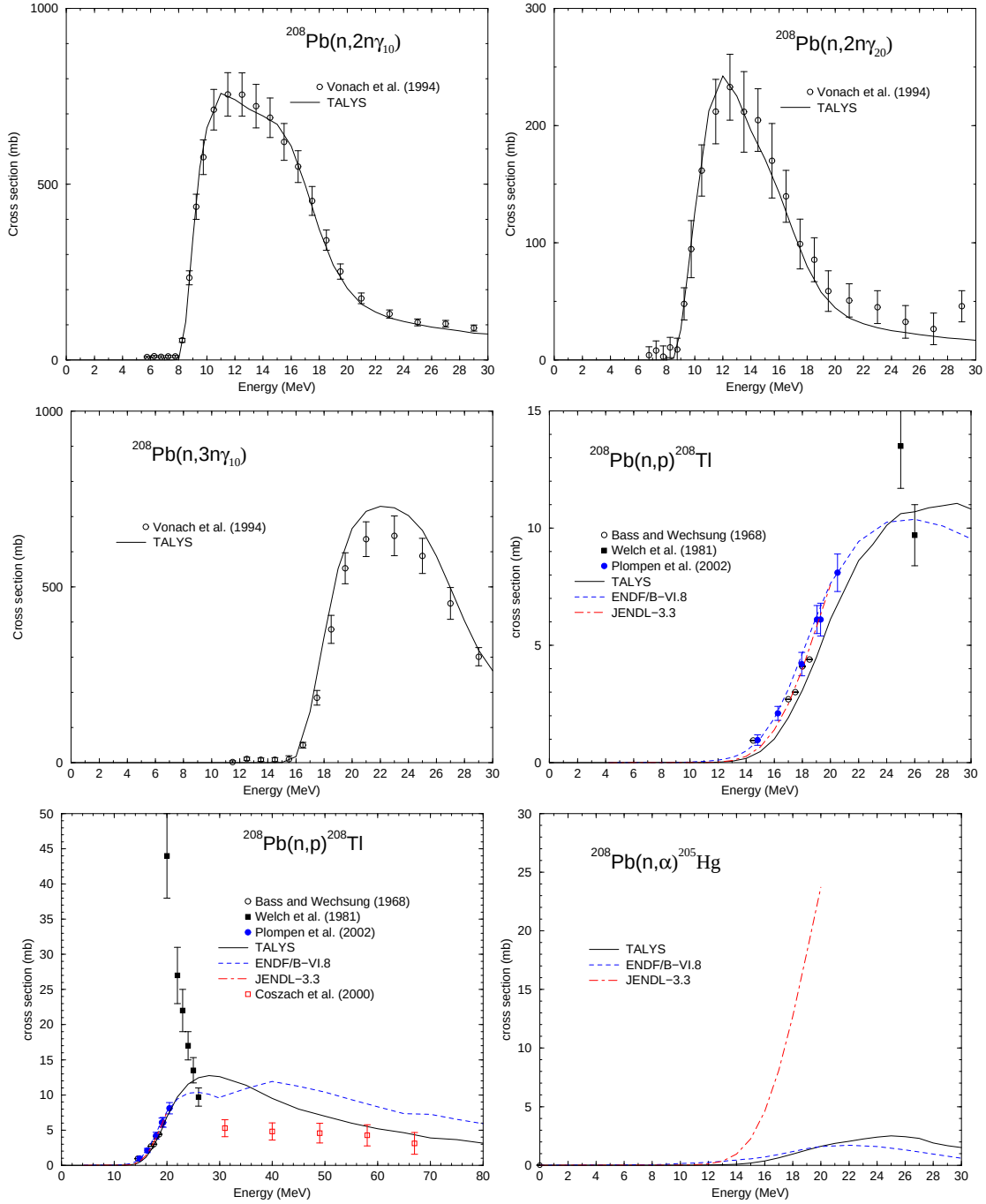


Figure 19. Cross sections for ^{208}Pb : (n,2n) gamma-ray production for the first level to the ground state, (n,2n) gamma-ray production for the second level to the ground state, (n,3n) gamma-ray production for the first level to the ground state, (n,p), and (n,p) at higher energies, and (n,α).

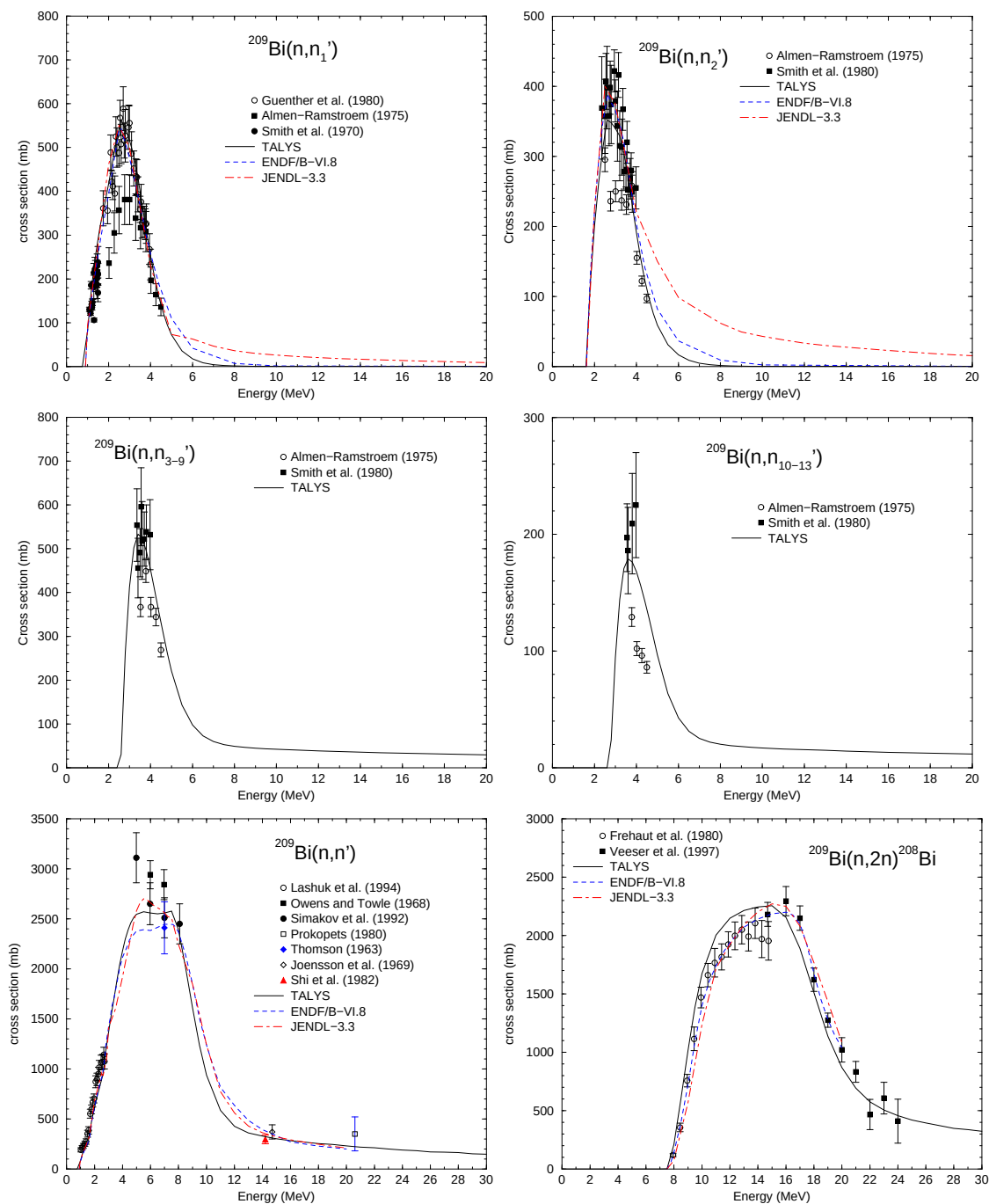
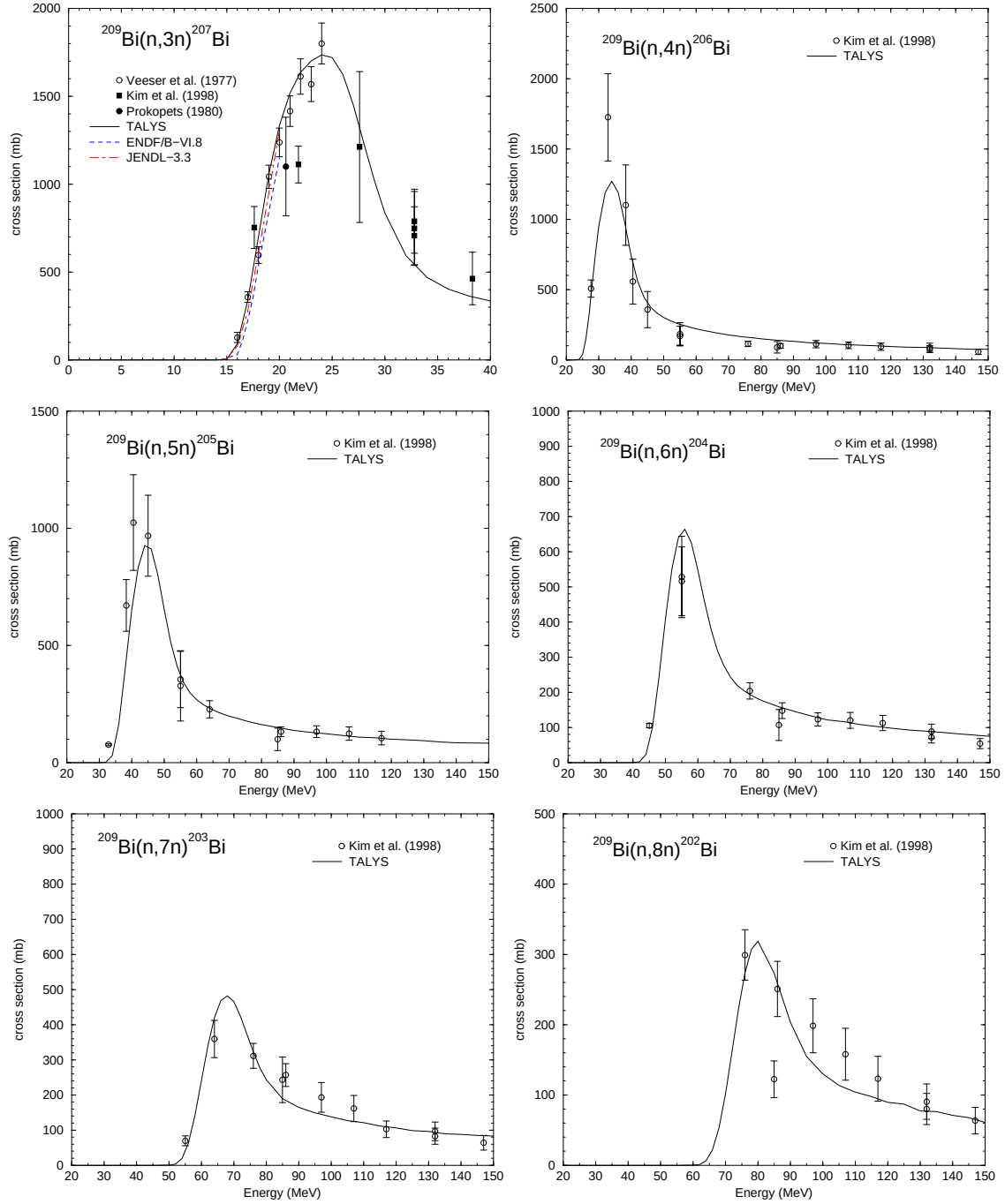


Figure 20. Cross sections for ^{209}Bi : Inelastic scattering to the first level, second level, 3th-9th level, 10th-13th level level, total inelastic scattering, and (n,2n).

Figure 21. Cross sections for ^{209}Bi : (n,3n) - (n,8n).

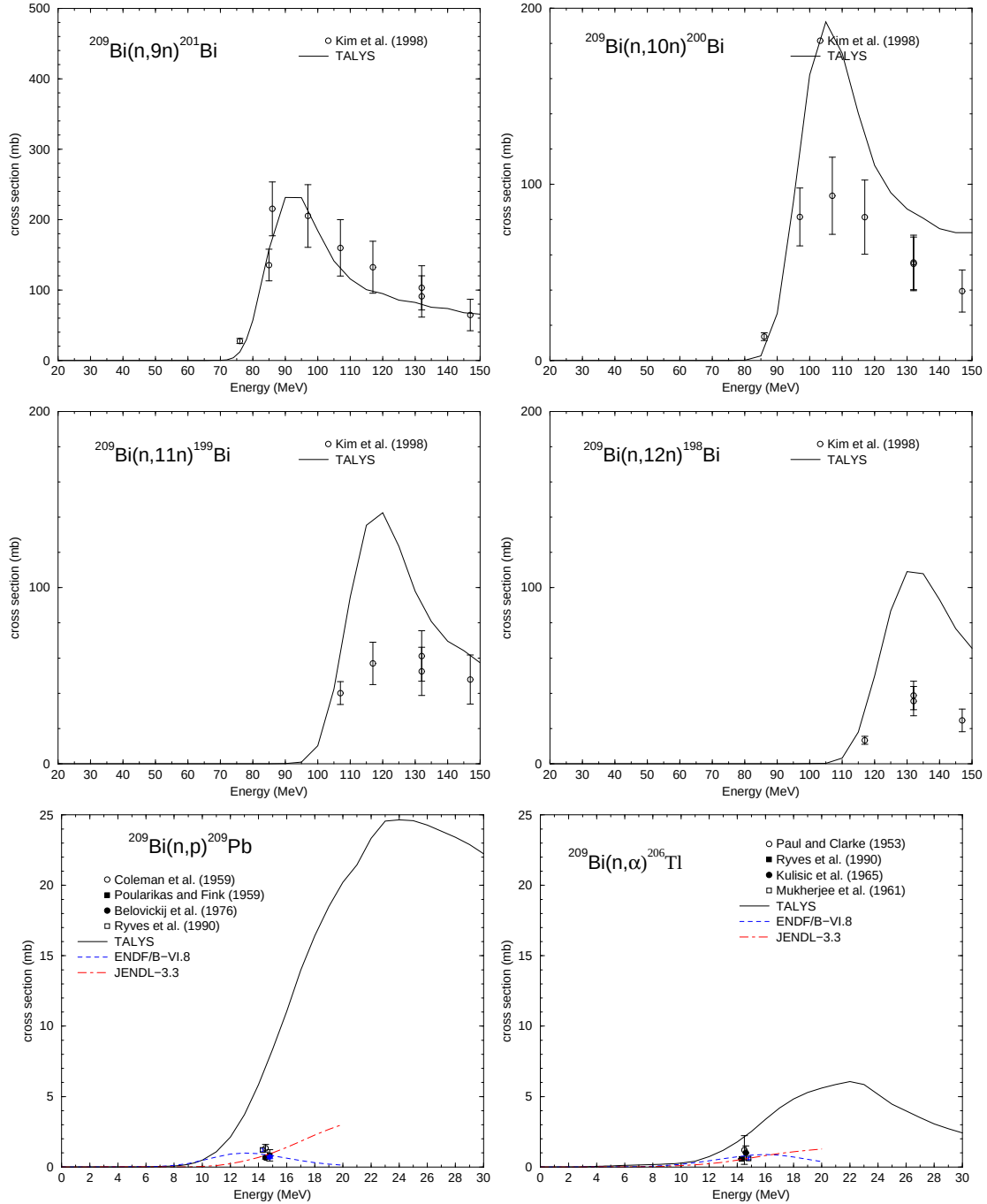


Figure 22. Cross sections for ^{209}Bi : (n,9n) - (n,12n), (n,p), and (n,α).

VI.8 results are higher than other evaluations and the experimental data between 3 and 6 MeV, while the rapid fall-off of the JENDL-3.3 cross sections for individual levels gives rise to a similar trend for the total (n,n') cross section between 5 and 8 MeV. Finally, since for Pb only the neutron and gamma channels are significant for energies below 20 MeV, the

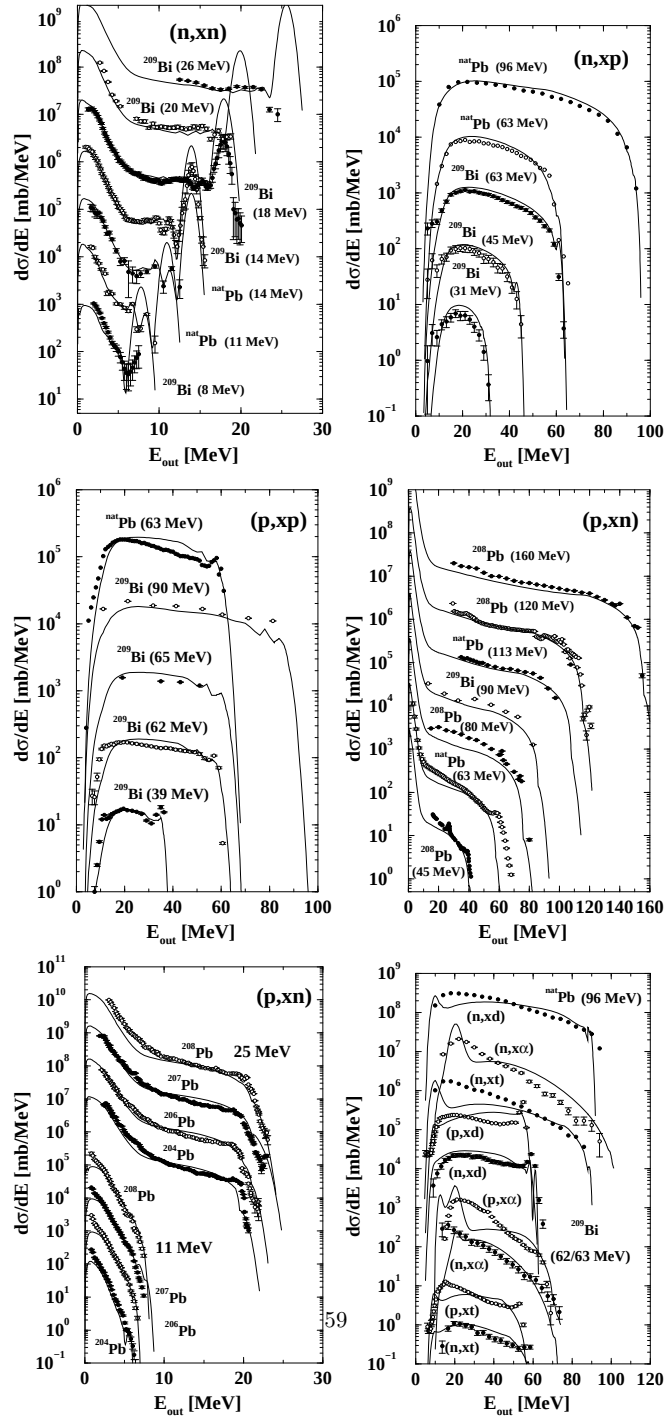


Figure 23. Angle-integrated spectra for several Pb and Bi targets: (n,xn) between 8 and 26 MeV, (n,xp) between 31 and 96 MeV, (p,xp) between 39 and 90 MeV, (p,xn) between 11 and 160 MeV, and some examples of neutron-induced and proton-induced cluster emission spectra.

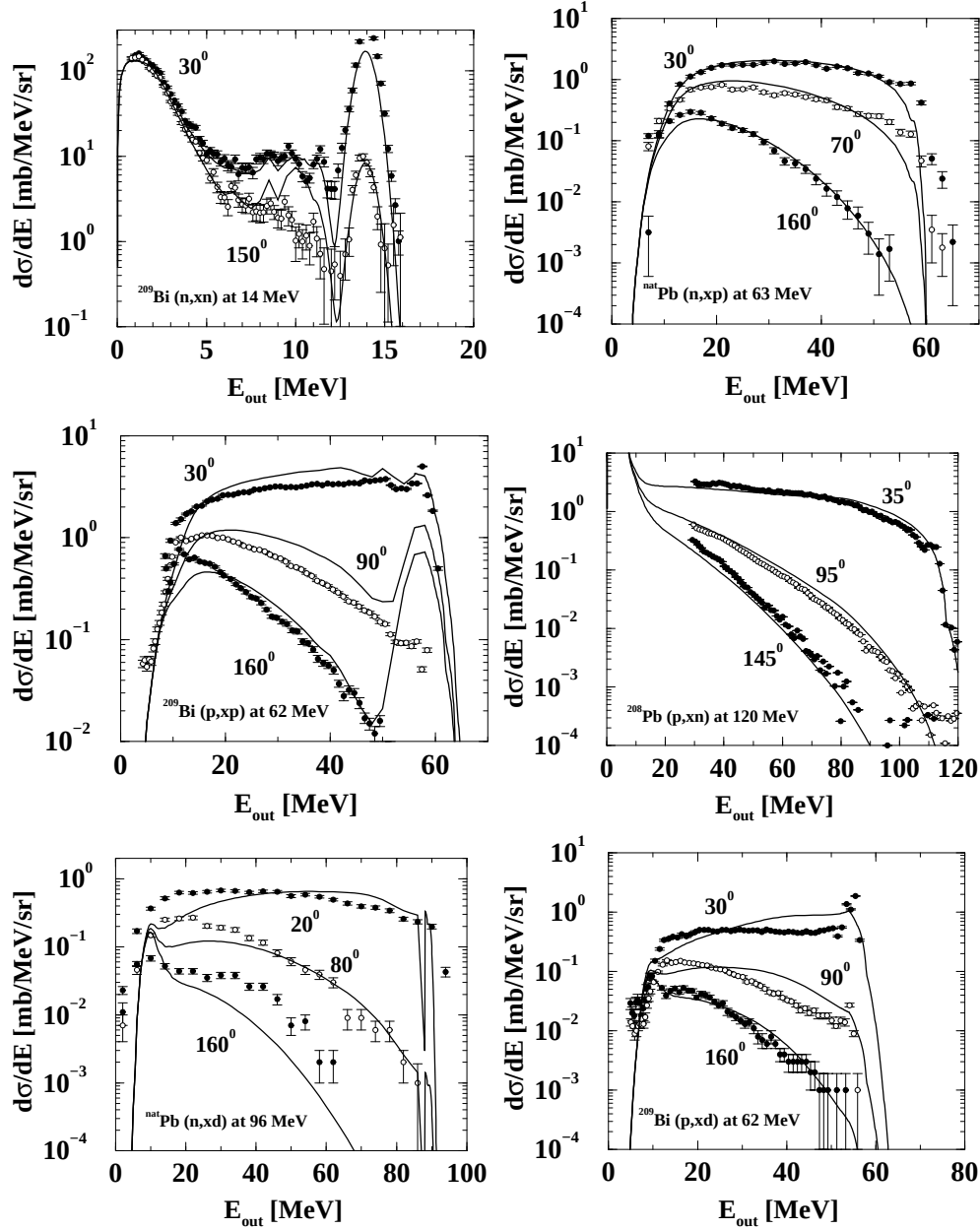


Figure 24. Double-differential spectra for Pb and Bi: (n,xn) at 14 MeV, (n,xp) at 63 MeV, (p,xp) at 62 MeV, (p,xn) at 120 MeV, (n,xd) at 96 MeV, and (p,xd) at 62 MeV. Each data set is multiplied by an additional factor of 10 with respect to the previous one.

neutron non-elastic cross section provides a further constraint for the division among the neutron inelastic (and (n,2n) and (n,3n)) channels.

For ^{209}Bi , the evaluated vs. experimental [69]–[76] inelastic cross sections are displayed in Fig. 20. Here, the differences for the first few levels are much smaller. The same then obviously holds for the total neutron inelastic scattering, see Fig. 20.

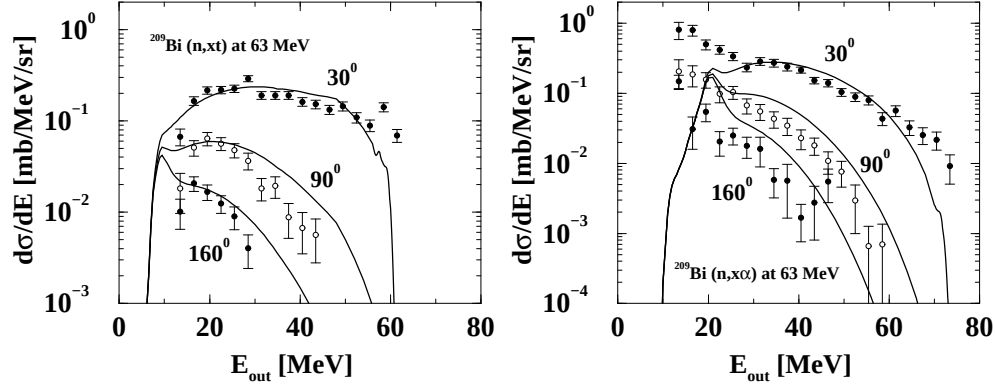


Figure 25. Double-differential spectra for ^{209}Bi . Left panel: (n,xt) at 63 MeV, and right panel: (n,x α) at 63 MeV.

11.3. Other Partial Cross Sections

Around 8-10 MeV, when the total inelastic cross section starts to decrease, the other reaction channels open up. The Coulomb barrier still prevents protons and other charged particles to be emitted at these energies, which implies that most of the reaction flux goes into the (n,2n) channel. Fig. 18 displays the calculated (n,2n) cross section, together with the experimental data [77, 78] and the results from the JENDL-3.3 and ENDF/B-VI.8 data libraries. The TALYS calculations and data libraries give an (n,2n) cross section higher than the one measured by Frehaut et al. [78]. The JENDL-3.3 evaluation for (n,2n) is considerably higher than all the other data between 12 and 20 MeV. This is also reflected by the shape of the total non-elastic cross section, which deviates from the other evaluations. According to the ^{208}Pb file description of JENDL-3.3, the (n,2n) was normalized to the energy integrated emission spectrum at 14 MeV, as measured by Takahashi et al. [79]. Fig. 29 shows this emission spectrum, together with an alternative measurement by Schmidt et al. [80] and our TALYS calculation, which follows the latter data set. Of course, the TALYS curve stems from the same calculation that produced the (n,2n) result. Also, the prediction of exclusive (n,2n) gamma-ray data for the first level to the ground state and the second level to the ground state, see Fig. 19, serves as a heavy constraint, as it did for (n,n'). Again, the quality of the TALYS fits suggest to regard the (n,2n) cross section as dependable. Moreover, our (n,2n) value at 14.1 MeV is 2179 mb, which is very close to the value of 2193 mb that Vonach [81] evaluated for the European Fusion File EFF-2.4. For ^{209}Bi (Fig. 20) they are much closer to each other and the experimental data [82]. For ^{208}Pb , an (n,3n γ) data set is available [67], see Fig. 19. Again, the TALYS prediction is satisfactory.

For ^{209}Bi , TALYS produces an overestimation of the few existing (n,p) data points [83]-[85] at 14 MeV, while also the (n, α) cross section is somewhat higher than the other data sets [86]-[88], see Fig. 22. The situation is again different for $^{208}\text{Pb}(n,\alpha)$, see Fig. 19, where now the TALYS and ENDF/B-VI.8 evaluations are close to each other whereas the JENDL-3.3 curve is higher by several factors. The $^{208}\text{Pb}(n,p)$ cross section is well described by all evaluations up to 30 MeV, see Fig. 19. An interesting measurement at higher energies was

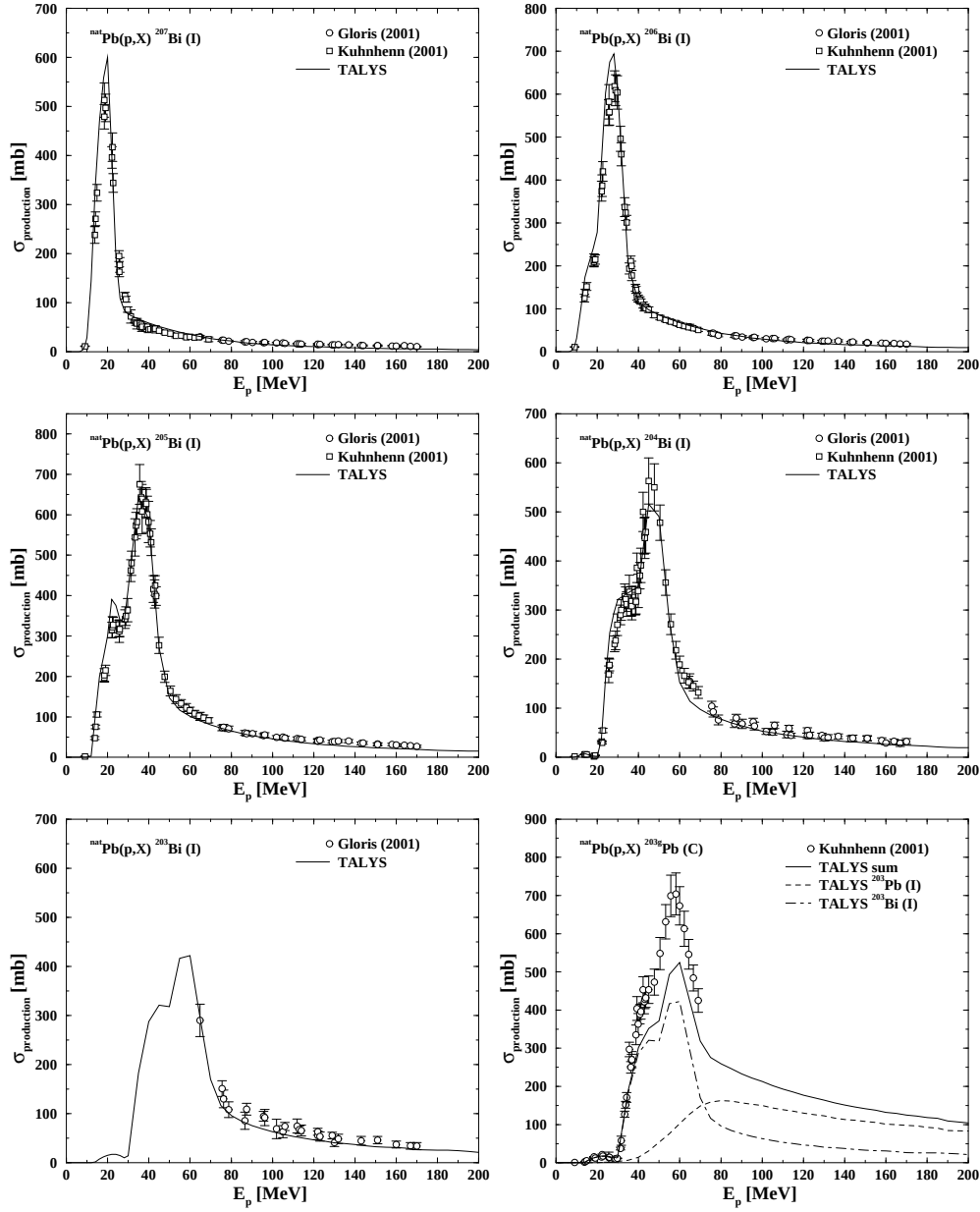


Figure 26. Residual production cross sections for protons on $^{\text{nat}}\text{Pb}$. I denotes an independent yield and C denotes a cumulative yield.

reported in Ref. [89]. It is compared in Fig. 19 with the available high-energy evaluations, which both are higher than the experimental data [90]-[91].

11.4. Differential and Double-Differential Spectra

The cross sections discussed in the previous section are of prime importance for a correct description of neutronics and activation issues. In addition, secondary energy and energy-

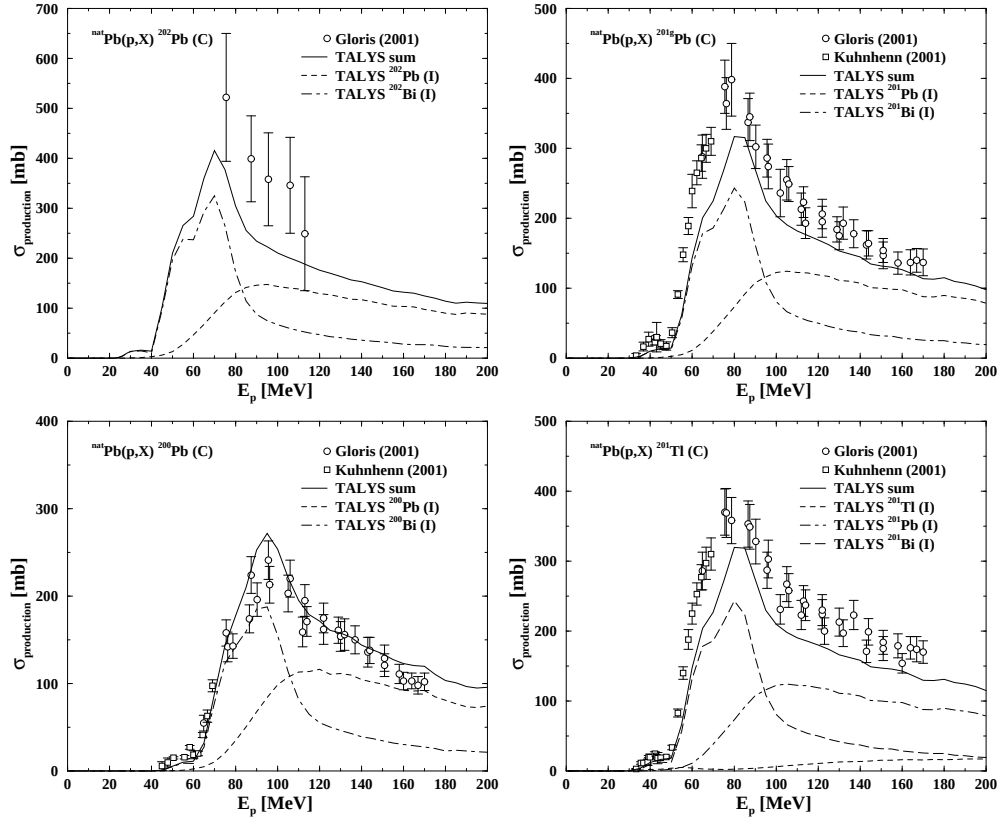


Figure 27. Residual production cross sections for protons on $^{\text{nat}}\text{Pb}$. I denotes an independent yield and C denotes a cumulative yield.

angle spectra play an important role. For a complete description of a nuclear reaction these need to be adequately described as well. Fig. 23 gives a good impression of the general quality of the calculated outgoing energy spectra. The preequilibrium contribution in the 20 and 26 MeV (n,xn)-spectra on ^{209}Bi seems to be slightly underestimated (some of the data sets may be mutually inconsistent, as explained in Ref. [62]). At lower incident energies, the calculated and measured spectra match very well. Some deviations are visible between the calculated and measured excitation of collective states in the ^{209}Bi (n,xn)-spectra, whereas the 11 and 14 MeV (n,xn)-spectra on $^{\text{nat}}\text{Pb}$ show a perfect agreement.

In general, the exciton model parameterisation used in this work yields overpredictions of the outgoing proton spectra in the Pb and Bi mass region, especially in the primary preequilibrium contribution [62]. This is encountered both in neutron-induced and in proton-induced reactions. The recent experimental (n,xp)-data are the only available data sets for Pb and Bi targets. In general, the shape of the spectra is very well reproduced while TALYS tends to overestimate the (n,xp)-spectra. The maximum deviation amounts to 30%. The (p,xp)-spectra included in Fig. 23 reveal the same systematic deviation. The good theoretical description of the 39 and 90 MeV data sets on ^{209}Bi seems to form an exception to the rule.

The description of the low-energy (p,xn) spectra of Pb isotopes, in Fig. 23, is reasonable.

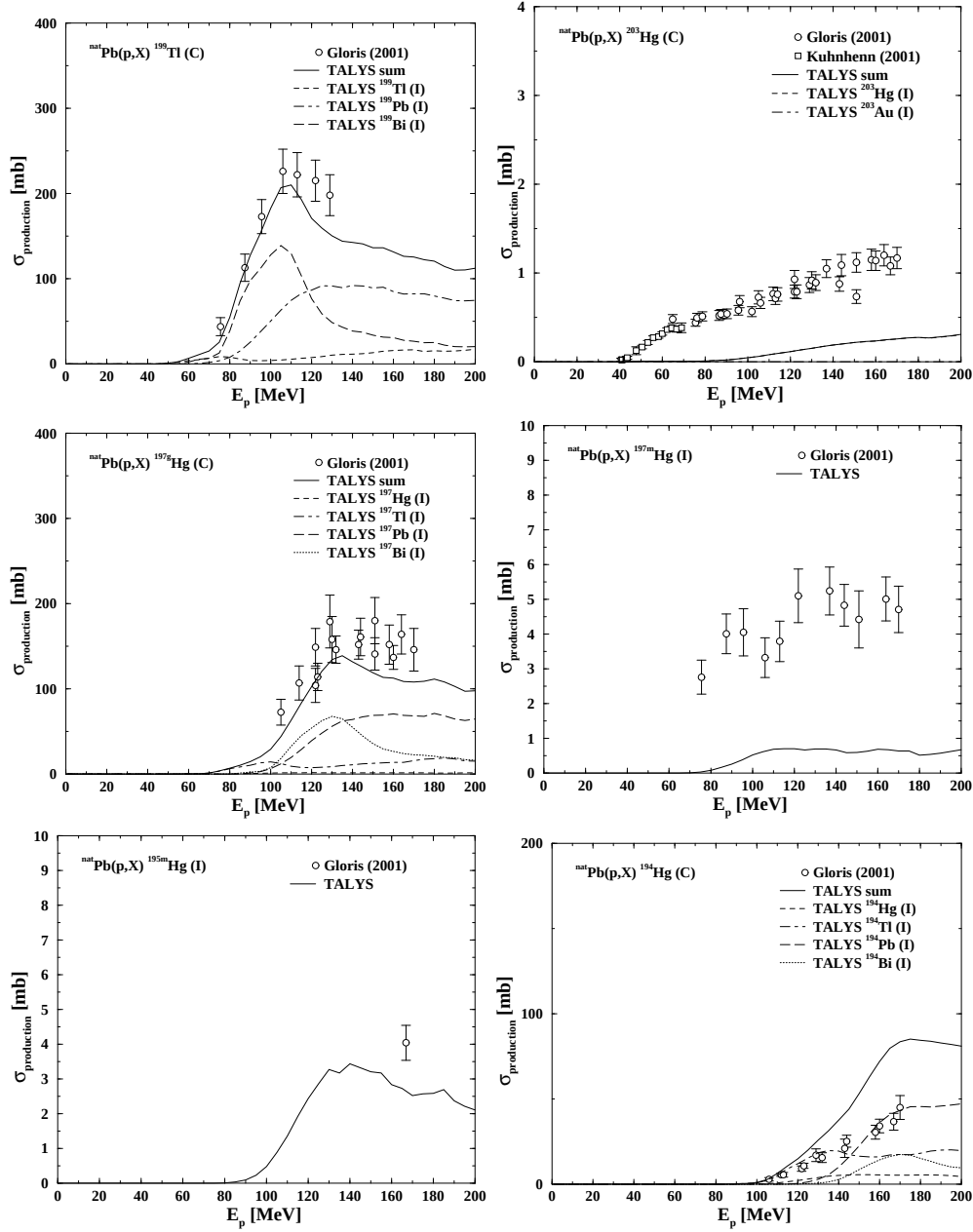


Figure 28. Residual production cross sections for protons on ^{nat}Pb . I denotes an independent yield and C denotes a cumulative yield.

In the middle of the pre-equilibrium region, say at neutron emission energies that are half of the incident proton energy, both the shape and magnitude are generally well reproduced. At the highest emission energies, the calculated spectrum sometimes matches the data very well. An inconsistent set of experimental spectra seems to exist for (p,xn) reactions for Pb and Bi around 90 MeV, see Fig. 23. A significant underestimation is found for ^{208}Pb (p,xn)

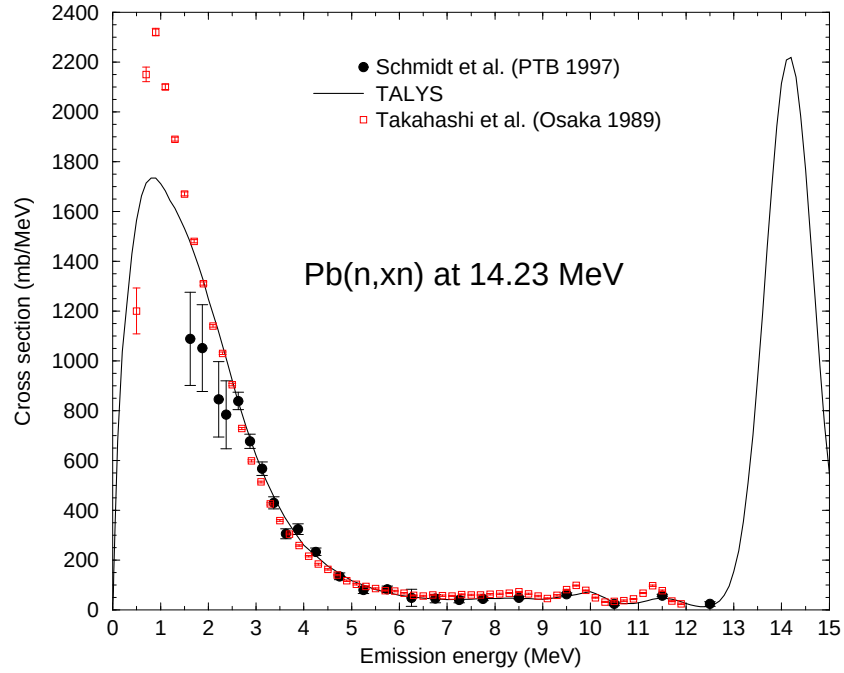


Figure 29. Angle-integrated emission spectrum for 14.2 MeV neutrons on ^{nat}Pb . Difference between experimental data sets and TALYS.

at 80 MeV. Although this is consistent with the underestimation found at 63 MeV (where in addition a deviating shape is observed), it clashes with the perfect agreement obtained for $^{209}\text{Bi}(p,xn)$ at 90 MeV and $^{208}\text{Pb}(p,xn)$ at 120 MeV.

The last panel in Fig. 23 contains examples of the cluster emission spectra. The shape of the spectra resulting from the combination of Kalbach's empirical model [63] and the compound contribution is certainly not perfect, but the order of magnitude is correct in most cases.

As explained in Sec. 10.7., we fold the well-known Kalbach systematics for angular distributions with the computed energy spectra to obtain the double-differential spectra. No additional fitting has been performed and Figs. 24 and 25 are included to illustrate the quality of the resulting predictions at various angles. Obviously, a large deviation found in the total energy spectrum translates in a similar deviation in the dominating forward angles of the double-differential results.

11.5. Residual Production Cross Sections

Finally, the prediction of the residual products deserves special attention. These activation cross sections determine the production of undesired products in the entire device and also may have consequences for the shielding requirements.

Information that is very valuable for the validation of the total neutron production in the core and coolant is provided by the series of $^{209}\text{Bi}(n,xn)$ measurements by Kim et al. [92] for neutrons up to 150 MeV. Figs. 21-22 show a comparison between the experimental data

and the TALYS calculations. The agreement is very satisfactory up to the (n,9n) channel. For (n,10n)-(n,12n), we overestimate the data. A similar prediction has been obtained by Chadwick et al. [93].

Proton-induced activation of ^{nat}Pb has been measured up to 200 MeV. Figs. 26-28 depict for many different residual products the experimental data and the TALYS curve. Some production cross sections correspond to the independent yield of that particular nucleus, which is indicated by (I) in the figures. Other production cross sections reflect the cumulative yield, denoted by (C), corresponding to the summed yield of that nucleus and its beta-decay precursors. The excitation functions do not all possess a similar shape, like the (n,2n) up to (n,12n) cross sections on ^{209}Bi mentioned above. Some reveal interesting structures, which indicate the presence of a second peak. In the case of the independent yields, this may be caused either by contributions of two dominating different exclusive channels or by contributions from two different Pb target isotopes. In the case of cumulative yields, obviously, these structures may arise from adding the contributions of beta-decay parents. All such processes are accounted for by these TALYS calculations. Consequently, TALYS reproduces these structures quite nicely. In general, the predictive power of the absolute value of the residual production cross sections deteriorates for residual nuclides further away from the initial target nucleus.

12. Future Possibilities

12.1. Microscopic Cross Section Measurements

As was discussed in section 8. the prospects for development in the near future, i.e., within ten years, can be summarized to extension to about 200 MeV of ongoing work on elastic scattering, inelastic neutron emission and light ion production, and fission studies of other parameters than the cross section.

If looking a bit further into the future, we can allow ourselves to be more visionary. To our opinion, the single most important problem to solve if we want a significant development of the field is normalization. At present, we inevitably end up with an uncertainty of about 5 %, because we have to normalize to something, typically np scattering, which is known to - at best - 5%, and it is difficult to see how this can be radically improved upon in a short term with present techniques.

We consider energy resolution to be the second largest problem, with intensity on third place. These two are, however, to a large degree coupled. If you aim for good neutron-beam energy resolution, you have to pay by poor intensity and vice versa. It is presently close to inconceivable to produce neutrons at high energies with a resolution better than 1 MeV with a reasonable intensity. The limited intensity puts severe constraints on the detection, in such a way that the detection often has to be performed with techniques that sacrifice resolution for efficiency, resulting in a final resolution of a few MeV. This means that only in a few rare cases, resolved final states can be studied.

Recently, a way out of this dilemma has been proposed. At CERN, planning is ongoing for a beta-beam facility [94]. The background is that neutrino physics has progressed rapidly the last few years, with the discovery of neutrino oscillations as the most visible example. Up to now, essentially all accelerator-produced neutrinos have been muon neu-

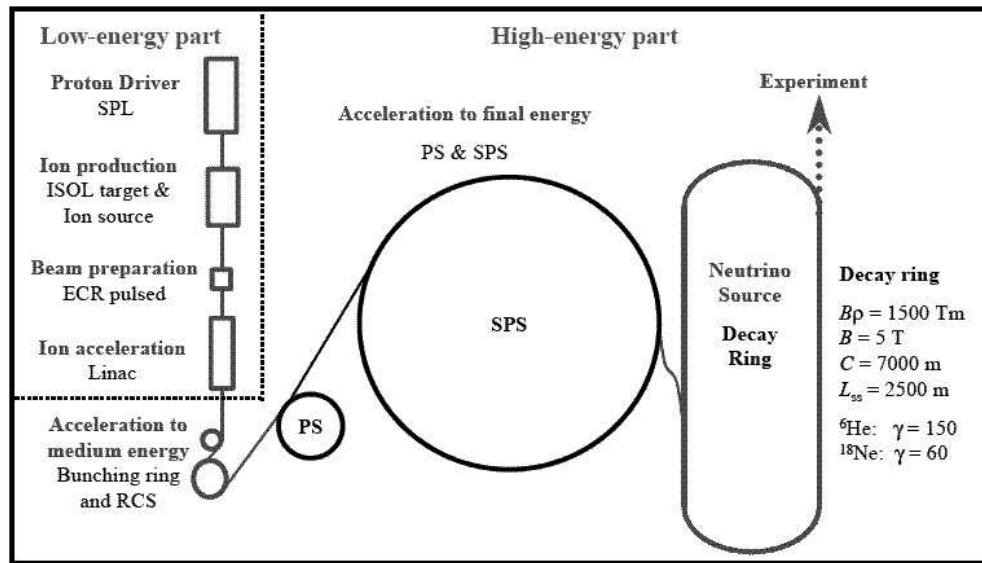


Figure 30. Overview of the proposed CERN beta-beam facility.

trinos, being the final product of pion decay. Electron neutrinos are much more difficult to produce in large amounts, because they require a nuclear beta decay for their creation.

At the proposed CERN beta-beam facility, production of suitable beta-emitting nuclei should be undertaken in an ISOLDE-like facility, and the produced nuclei should be post-accelerated to very high energy by a series of accelerators, the final one being SPS. After acceleration, the ions are directed to a decay ring of race-track shape. At these very high energies, hundreds of GeV per nucleon, there is a very strong Lorentz boost, which means that the neutrino is emitted very close to the beam direction in the laboratory system, in spite of that the emission is isotropic in its moving reference frame. Thereby, intense neutrino beams can be produced. The idea is to build the decay ring so that one straight section points towards a distant neutrino detector to allow studies of electron neutrino oscillations.

Intense neutron beams could be a spin-off from that facility. It has been proposed to use two production targets, one for nuclei suited for neutrino emission in the decay ring, and one for beta-delayed neutron emitters. Some unstable neutron-rich nuclei can beta decay to a nucleus that promptly emits a neutron (a process which by the way is of utmost importance to the stability of nuclear power reactors). This neutron has typically an energy of a few hundred keV in its rest frame. By accelerating the beta-delayed neutron emitters up to a few hundred MeV per nucleon, the Lorentz boost is sufficient to focus the beam to reasonable dimensions. All this can be done in parallel with the primary objective, since the accelerators for the neutrino emitters have a long cycle with a low duty factor. Thus, for most of the time there is no injection into the synchrotrons for neutrino emitter acceleration, and then the production targets can be used for beta-delayed neutron emitter production.

The resulting neutron beam has an energy in the 100–500 MeV range with an energy resolution of about 1 MeV, and intensities of about 10^{11} n/s are estimated. This should be compared with 10^6 for present-day technology, i.e., an improvement by a factor 100 000

(!). With such intensities, only imagination sets the limit for what can be achieved.

A second technique would be to use a similar production as above (1-2 GeV protons on a combined target-ion source) to produce ${}^6\text{He}$, which in turn would be accelerated to hit a target [95]. Roughly, ${}^6\text{He}$ can be described as an α particle with two loosely attached neutrons. When hitting a target, the two neutrons are dissociated with a large probability, and continue along the direction of the incident beam with the incident velocity. The charged particles (the remaining ${}^6\text{He}$ and residual ${}^4\text{He}$) are bent by a magnet system and a clean neutron beam is produced, with a divergence similar to that of a beta-delayed neutron beam. This latter technique does not have the potential to produce as intense fluxes as the beta-decay in flight, but on the other hand it requires much less advanced accelerators. This technique could possibly be installed at existing CERN facilities after some upgrades. Initial estimates indicate a factor a hundred to a thousand larger neutron fluxes than for present facilities to be within reach.

First and foremost, such intensities are not too far from what has been used in proton beam experiments for fundamental nuclear physics. This opens opportunities to study, e.g., the role of isospin in nuclei by conducting a carefully selected set of experiments where information from experiments with neutrons could be combined with previous information from proton-induced reactions. Many types of fundamental physics issues hitherto inaccessible to experiments could be within reach to address.

If we now restrict the discussion to nuclear data for ADS applications and turn to the problem list above, it seems feasible that we can address all of them through one experimental trick: tagging. If we use the neutron beam directly for experiments we have essentially only solved the intensity problem, but the other two remain; we end up in a 1 MeV resolution due to the inherent energy spread, and we are still plagued by the normalization problem. Tagging means that we produce a secondary neutron beam of less intensity, but with much better known intensity. One candidate reaction is to let neutrons scatter from a hydrogen target, and the recoil proton is detected. Since this is a two-body final state, detection of the associated proton means that a neutron must have been scattered to the corresponding direction. Thereby, the normalization problem can be circumvented, since we count the neutrons one by one through the associated particle. If this tagging is performed with a high resolution, both in energy and angle, we can also know the neutron energy event by event far better than the initial neutron beam energy resolution. If the tagging is performed with a magnetic spectrometer, the tagger can be made rather insensitive to the ambient background, and a proton energy resolution of better than 100 keV can be obtained, resulting in a comparable neutron energy resolution.

With reasonable estimates on tagger parameters, 10^4 tagged neutrons with an energy resolution of 100 keV should be possible to reach, given the beam intensity above. This might sound like a poor intensity, but with such a resolution, final states can be well resolved, which means that already a small number of events will result in a good precision. Moreover, since the intensity can be determined to about 1 % in a typical tagger system, the accuracy is far better than what can be obtained today. In cases when the demands on energy resolution are not as stringent, a thicker tagger target can be used, resulting in increased intensity. This goes faster than linear, because with a worse resolution, the intensity at the tagger is increased, thicker secondary experimental targets can be used, and the detection limitations are less severe. Therefore, even with resolutions that are on the limit to

be possible untagged today, we might have tagged beams of intensities exceeding what is presently available untagged in a not too distant future.

12.2. Integral Experiments

Integral experiments are an indispensable activity in the evaluation and validation of nuclear data. Procedures for such validation are since long well established in the classical neutron energy range up to 20 MeV, i.e., the neutron energy range of relevance to critical fission reactors (thermal and fast), as well as fusion applications. With the advent of accelerator-driven systems (ADS), the energy range in which information on neutron-induced nuclear reactions are required for design activities has been significantly increased. In a spallation-driven system, neutrons of energies all the way up to the incident proton energy, i.e., up to GeV energies, are present. Although relatively few neutrons reside at these high energies, their large capability to induce, e.g., materials damage necessitates the nuclear data libraries to be improved significantly above 20 MeV.

The ADS research activities have so far been dominated by measurements of microscopic cross sections, a fact that has been motivated by the state of knowledge at the time these projects were launched. The recent achievements of these projects now motivate an increased attention to integral experiments, especially at ADS-relevant energies, i.e., above 20 MeV, where such experiments are almost absent. Thus, a few existing high-quality integral experiments should be identified. Above 20 MeV some shielding experiments exist, notably the 43 and 68 MeV TIARA transmission measurements for concrete and iron, which is mainly important for structural material studies [96].

What is missing is a clean experiment on core material that allows analysis of the impact of high-energy neutrons. An integral experiment of neutrons on a block of uranium would enable a thorough test of evaluated neutron data files above 20 MeV. More complex systems, such as MEGAPIE [97], serve to test ADS calculations as a whole. The complex design of such target systems makes it, however, difficult to draw conclusions on the quality of the underlying nuclear data based on the performance of the full system. The necessity and feasibility of integral experiments above the present maximum energy of 68 MeV will be investigated in the CANDIDE project [98]. Simulations of the transmission of 96 and 175 MeV neutrons through slabs of iron and lead indicate that such experiments can be completed in reasonable beam time. The selections of materials have been dictated by the needs of ADS development. Iron is a representative construction material and lead is a candidate for coolant. Investigations of uranium constitute future work.

12.3. Bringing Nuclear Physics back into Reactor Engineering

As was pointed out in Section 9., an alternative approach to the data evaluation method would be one nuclear model supercode that produces better results than all data files combined for all energies and isotopes, and is so fast that it can be hardwired directly in a transport code. With such an approach, a direct link between reactor properties and the underlying nuclear physics would be established.

Recently, a first step in this direction has been taken [99]. In TALYS, a limited number of free parameters are determined by fits to the global database. In such fits, a best-fit

value is obtained, but also an uncertainty. A recent development is to issue a second step: massive computation. Literally 5000 calculations of the properties of a nuclear reactor are performed. In each calculation, TALYS is used to obtain a nuclear data library, which in turn is used as input in a reactor physics code. The input parameters to TALYS are randomised by the probability functions obtained in the preceding global fit. Thus, 5000 slightly different nuclear data libraries are used as input, leading to 5000 slightly different results on the reactor performance.

With this approach, the 5000 outputs can be analysed statistically. The reactor codes do not only provide a single value as before, but the uncertainties in key parameters can be obtained from the distribution of results on the same key parameters in the massive computation approach.

Already in the pioneering publication in which the methodology was applied in MCNP simulations of some Gen-IV and ADS cases, the method has revealed some very fundamental findings. The most important is that it has proven one basic assumption in all modelling today to be fundamentally wrong. It has been a common prejudgment in reactor physics that uncertainties follow Gaussian distributions, but in fast reactors, the results on criticality are far from a Gaussian distribution. This means that with conventional methodology, the results have over-estimated safety margins because tails in the probability distribution have been neglected, and in cases where calculations have indicated the reactor design to be stable, it could in reality be prompt critical.

That the values on k_{eff} deviate from Gaussian distributions in fast reactor systems but not in thermal makes sense from very fundamental considerations. In the fast energy range, some dominating cross sections are strongly correlated, and therefore the central limit theorem does not apply. Hence, Gaussian distributions cannot be a priori presumed.

We are convinced that this methodology has important use in also present nuclear power technology. Although the methodology can be used to improve safety of nuclear power plants, the main use is in cost-cutting. Present nuclear power plants are so safe that the nuclear information is not the dominating uncertainty in safety assessment. The method therefore has its cutting edge in economizing the operation. With improved handling of uncertainties in, e.g., core planning, less conservatism would be required, leading to, e.g., more efficient use of the fuel.

In the design of future reactors, the method has revolutionary implications. In such development and design, the massive computation approach can save enormous costs and efforts. By establishing direct links between fundamental physics and practical engineering, research can be guided far more efficiently than today. Basic physics can be used more, and trial-and-error less in the development process.

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Chapter 2

**AN OVERVIEW ABOUT MODELING APPROACHES
FOR TURBULENT MIXING AND VOID DRIFT
IN SUB-CHANNEL ANALYSIS**

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Abstract

If the boiling flow through fuel assemblies in a reactor core is to be predicted numerically by means of a sub-channel code, two important lateral exchange processes between neighboring sub-channels have to be taken into account: *turbulent mixing* and *void drift*. Whereas mixing is a kind of turbulent gradient diffusion occurring in both single-phase and two-phase flow, void drift is a two-phase phenomenon which is physically not yet well understood. However, there are a lot of phenomenological attempts to model this superimposed effect, which can act in the same direction as turbulent mixing, but also contrarily depending on sub-channel geometries and flow conditions.

The present paper will classify the physical background of both phenomena including a detailed overview about the flow conditions which have to be existent in order to cause the one or other phenomenon. Furthermore, it will provide a well-structured thread through the whole calculation methodology. Thereby, each single phenomenon will be discussed in detail (physically and mathematically) and an overview about both the particular state-of-the-art models and new approaches in open literature will be given.

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Abbreviations

Latin Symbols

A	- area relevant for lateral exchange (e.g., area of the gap between sub-channels i and j)	[m ²]
A_i	- cross-sectional area of sub-channel i	[m ²]
A_j	- cross-sectional area of sub-channel j	[m ²]
a	- conductivity of temperature	[m ² /s]
c	- concentration	[-]
C_L	- lift force coefficient	[-]
C_{lub}	- lubrication force coefficient	[-]
c_μ	- constant in the k - ε model ($c_\mu = 0.09$)	[-]
c_p	- specific heat capacity	[kJ/(kg K)]
D	- diffusion coefficient for mass transfer	[m ² /s]
d_{bub}	- bubble diameter	[m]
$d_{\text{bub,hor}}$	- maximum horizontal dimension of a bubble	[m]
d_{gap}	- gap width	[m]
d_{hyd}	- hydraulic diameter of sub-channel	[m]
d_{pitch}	- rod-to-rod pitch	[m]
d_{rod}	- rod diameter	[m]
Eo	- Eötvös number	[-]
G	- mass flux	[kg/(m ² s)]
g	- acceleration of gravity	[m/s ²]
h	- specific enthalpy	[kJ/kg]
$\dot{I}$	- momentum flow rate	[N]
j	- superficial velocity	[m/s]
k	- turbulent kinetic energy	[m ² /s ²]
L_t	- characteristic turbulent length scale	[m]
$\dot{m}$	- mass flow rate	[kg/s]
$\dot{Q}$	- heat flow rate	[kW]
$\dot{q}$	- heat flux	[kW/m ²]
P	- pressure	[Pa]
p_w	- wetted perimeter	[m]
Pr	- Prandtl number	[-]
r_b	- bubble radius	[m]
Re	- Reynolds number	[-]
T	- temperature	[K], [°C]

Tu	- turbulence intensity	[-]
U	- axial velocity	[m/s]
U_t	- characteristic turbulent velocity	[m/s]
V_{term}	- terminal velocity of a single isolated bubble rising through stagnant liquid	[m/s]
W'	- mass flow rate per axial length	[kg/(m s)]
x	- flow quality	[-]
y	- lateral coordinate	[m]

Greek Symbols

α	- volume fraction	[-]
β	- turbulent mixing coefficient (turbulent diffusion coefficient = TDC value)	[-]
Δy	- distance between centroids of two adjacent sub-channels	[m]
ε	- turbulent dissipation rate	[m ² /s ³]
ε	- wall roughness	[m]
Γ	- general diffusion coefficient	[m ² /s]
$\tilde{\Gamma}$	- artificial general diffusion coefficient	[m ² /s]
λ	- heat conductance	[kJ/(m K)]
μ	- dynamic viscosity	[kg/(m s)]
ν	- kinematic viscosity	[m ² /s]
ρ	- density	[kg/m ³]
σ	- surface tension	[N/m]
Θ_{Spacer}	- spacer multiplier	[-]
Θ_{TP}	- two-phase multiplier	[-]

Subscripts

liq	- continuous liquid
ent	- entrained liquid
EQ	- equilibrium
i	- sub-channel i
ij	- gap between sub-channels i and j
j	- sub-channel j
k	- phase or field
mix	- (two-phase) mixture
SP	- single-phase

t	- turbulent
tot	- total (tot = liq + ent + vap)
TM	- turbulent mixing
TP	- two-phase
vap	- vapor
VD	- void drift

Superscripts

$\overline{\phi}$	- mean value averaged over adjacent sub-channels i and j

1. Introduction

In core thermal hydraulics the lateral exchange mechanisms for liquid and vapor have to be predicted in all the different flow regimes occurring in both Boiling Water Reactors (BWR) and Pressurized Water Reactors (PWR). In sub-channel analysis those flow regimes are usually distinguished as depicted in Figure 1. As already mentioned in the abstract above the two superimposed phenomena turbulent mixing and void drift have to be taken into account. These phenomena will be outlined separately and in detail in Paragraphs 2 and 3 for use in sub-channel analysis. Condensed overviews will be given in Paragraphs 2.7 and 3.3, showing in a compact schematic on which conditions each of these phenomena occurs.

1.1. Comparison of Modeling Approaches in Sub-Channel Analysis and CFD

For a better understanding of the whole context the main features applied in sub-channel analysis are compared with the corresponding state-of-the-art methods in Computational Fluid Dynamics (CFD). There has also been a lot of progress in CFD methodologies within the last decade, but this will not be a main subject in the present paper.

Advanced sub-channel codes (like COBRA-TF, FIDAS, MONA-3, NASCA, etc.) solve the conservation equations for mass, momentum, and energy separately for the three fields *continuous liquid*, *entrained liquid*, and *vapor* (see Figure 2). State-of-the-art commercial CFD codes (like CFX, FLUENT, STAR-CD, etc.) solve only one *continuous* phase and one *disperse* phase simultaneously (either a liquid core flow with bubbles or a vapor core flow with droplets, see Figure 3, depending on flow regime). The occurrence of three fields or more in the same computational cell is still a topic of ongoing research (implemented in academic codes or codes for special applications). However, CFD codes solve in addition conservation equations for turbulence. Figure 3 shows the example of the well-established $k-\epsilon$ model applied to the continuous phase (either liquid or vapor).

In contrast to this, in sub-channel analysis turbulence is considered only by the so-called *turbulent mixing coefficient*. Usually, it is either a user-defined value or it is calculated by algebraic correlations depending on flow conditions and rod bundle geometry (see Figure 4).

The remaining forces on the dispersed vapor phase occurring in the form of bubbles, slugs, etc. are taken into account by means of interfacial friction correlations for each flow regime and a void drift model, whereas CFD codes attempt to model the relevant forces in more detail.

1.2. Experiments

In *Glück (2007)* the following experiments concerning lateral exchange processes in rod bundles under BWR operating conditions (70 bar) are comprehensively analyzed and results obtained with the sub-channel code F-COBRA-TF are discussed:

- GE 3x3 – see *Janssen (1971), Lahey et al. (1970)*
 - single-phase, no heating
 - two-phase, radial uniform heating
 - two-phase, radial non-uniform heating
- STUDSVIK 3x3 – see *Gustafsson et al. (1974)*
 - two-phase, strong radial non-uniform heating
- PELCO-S 4x4 – see *Herkenrath et al. (1981)*
 - two-phase, radial uniform heating.

These measurements still serve as a valuable and recognized database for program developers from all over the world to benchmark their codes with respect to mixing and void drift modeling. The GE 3x3 tests are of special interest, because of a certain physical phenomenon. In all two-phase experiments with radial uniform heating, the corner sub-channels have the highest thermal load (ratio of rod power to cross sectional area). However, most of the experimental state points show a lower equilibrium quality than in the side or center sub-channels. While turbulent mixing only results in a radial equalization of void fractions due to a gradient diffusion, in this case void drift is the phenomenon leading to a lower void fraction – and thus a lower quality – in the narrow corner sub-channels. This behavior can only be reproduced by sub-channel codes with an appropriate void drift model. Codes without any void drift modeling fail completely in predicting corner sub-channel qualities.

For PWR operating conditions (160 bar) there are comparable tests:

- EUROP 4x4 – see *Herkenrath et al. (1981)*
 - two-phase, radial uniform heating.

More recently, a comprehensive experimental database has been set up at Kumamoto University in Japan. It contains really fundamental lateral exchange experiments usually between two neighboring sub-channels only, however with various geometries. Furthermore, the experimental conditions were chosen in such a way that the single phenomena turbulent

mixing and void drift could be separated (see, e.g., *Sato and Sadatomi (1992)*, *Kano et al. (2002)*).

Moreover, fuel assembly vendors usually perform experiments to study the mixing behavior of new assembly or spacer grid designs.

1.3. Application Ranges of Turbulent Mixing and Void Drift

In principle, turbulent mixing has to be considered in all flow regimes depicted in Figure 1, including single phase liquid and single phase vapor. It should be applied to all three fields continuous liquid, entrained liquid, and vapor. Paragraph 2.7 will illustrate under which concrete circumstances turbulent mixing really occurs.

Void drift acts only in two-phase flow regimes and there only if vapor is the dispersed phase. *Sugawara and Miyamoto (1990)* recommended applying void drift in bubbly flow and slug flow, but not in annular flow. If there is a more detailed subdivision so that also churn flow occurs in the flow regime map (as in Figure 1), void drift can either be fully applied or a transition from full to zero void drift might be modeled within this flow regime.

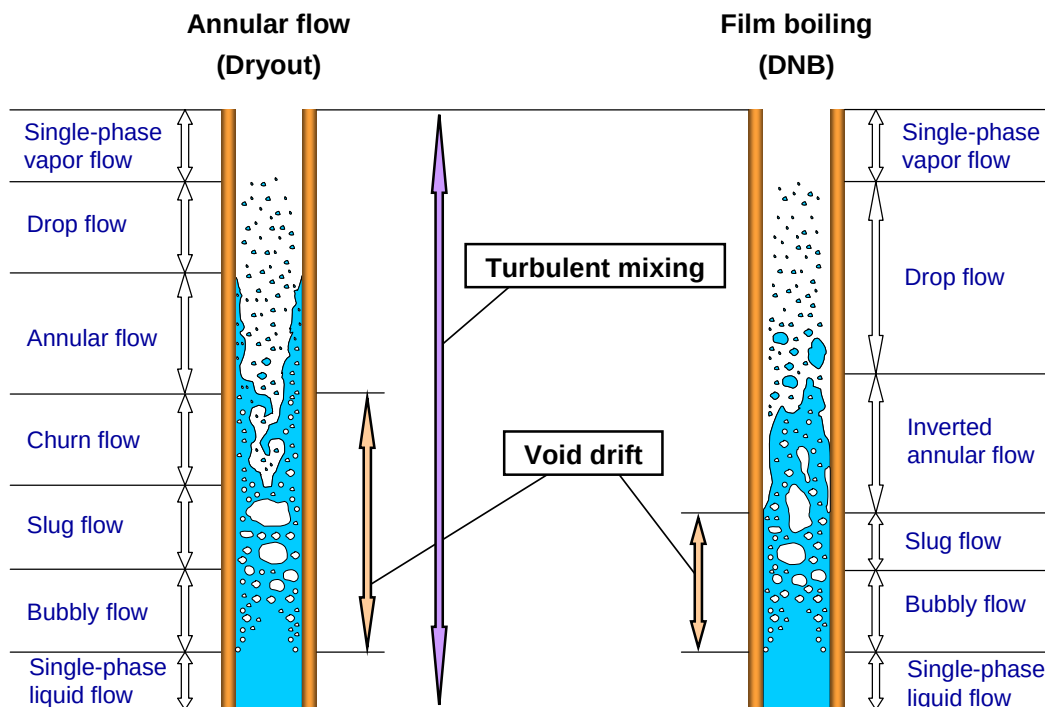


Figure 1. Flow regimes considered in sub-channel analysis and recommended application ranges of turbulent mixing and void drift (left: conditions leading to dryout boiling crisis, right: conditions leading to DNB boiling crisis).

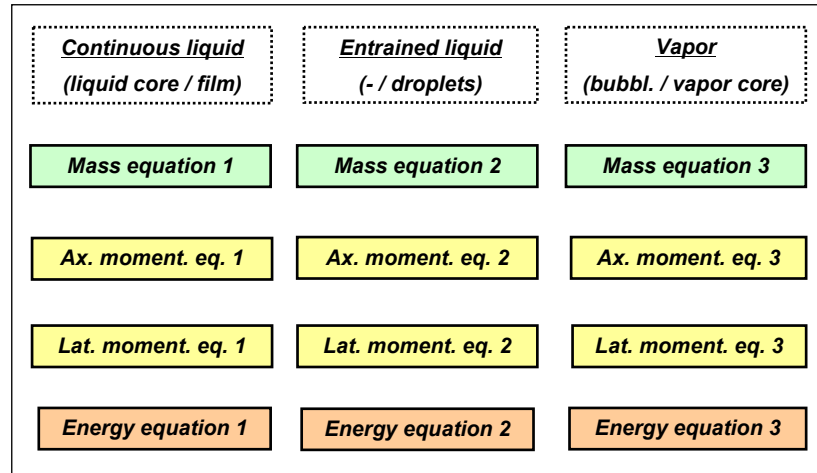


Figure 2. Schematic of basic equations used in advanced sub-channel codes (based on the two-fluid / three-field model).

In sub-channel codes both turbulent mixing and void drift have to be considered as additional source terms in the mass, axial momentum, and energy equations of the participating phases or fields, respectively (see Figure 2). Here, it should be mentioned that turbulent mixing and void drift do not only superimpose each other. They are furthermore superimposed by diversion cross flow, which is the lateral exchange due to pressure differences between neighboring sub-channels. Diversion cross flow follows implicitly from the basic conservation equations and does not need to be modeled especially.

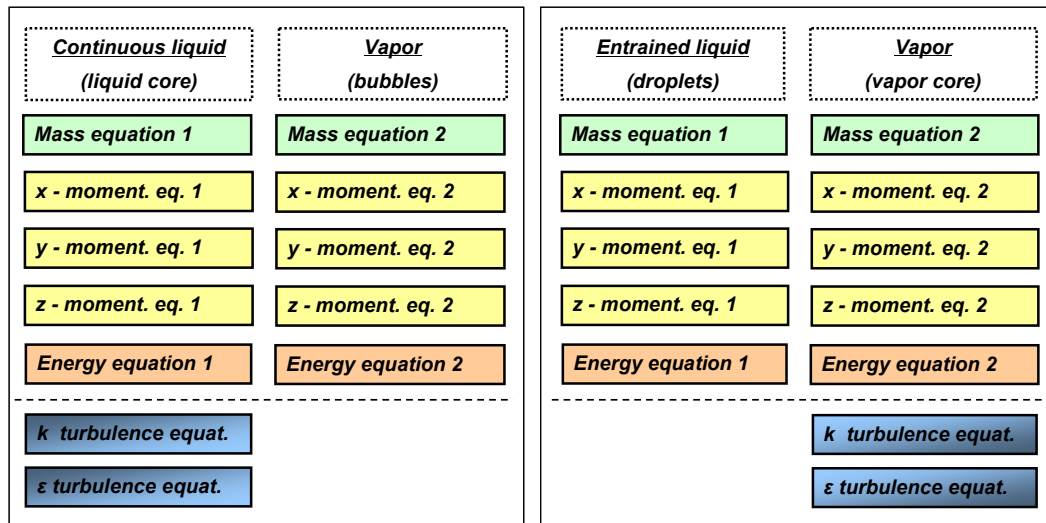


Figure 3. Schematic of basic equations used in state-of-the-art commercial CFD codes (left: liquid core flow with bubbles, right: vapor core flow with droplets).

Advanced sub-channel analysis

Computational Fluid Dynamics (CFD)

Turbulence (single-phase)

- Single-phase turbulent mixing coefficient:

$$\beta_{SP}(Re, d_{hyd}, d_{gap}, d_{rod})$$

- $k-\varepsilon$ model (RANS + 2 eqns.)

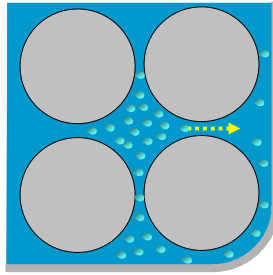
or

- Reynolds Stress Model (RANS + 7 eqns.)

or

- Large Eddy Simulation

Turbulence (two-phase)



- Two-phase turbulent mixing coefficient:

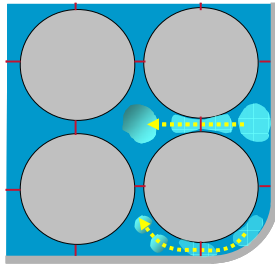
$$\beta_{TP} = \Theta_{TP}(x) \beta_{SP}(Re, d_{hyd}, d_{gap}, d_{rod})$$

- Modified $k-\varepsilon$ model in continuous phase (due to impact of disperse phase on turbulence)

Forces on dispersed vapor phase

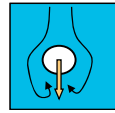
- Interfacial friction correlations for each flow regime

- Void drift model for lateral exchange between sub-channels:

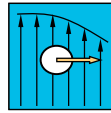


- Detailed modeling of bubble forces:

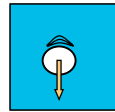
Drag force



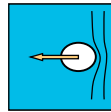
Lift force



Added mass force



Wall force



Coalescence / Break-up of bubbles

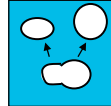
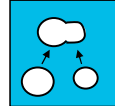


Figure 4. Treatment of single-phase and two-phase turbulence and of the remaining forces on the dispersed vapor phase in advanced sub-channel codes (left) and CFD codes (right).

2. Turbulent Mixing

2.1. Molecular Diffusion

The following diffusion processes are relevant in non-isothermal flows:

- diffusion of *mass* due to a lateral concentration gradient,
- diffusion of *momentum* due to a lateral velocity gradient,
- diffusion of *enthalpy* (thermal energy) due to a lateral temperature gradient.

For the sake of simplicity, a constant density is assumed first.

Molecular Diffusion of Mass

$$\dot{m} = -\rho D \frac{dc}{dy} A \quad (2.1)$$

Molecular Diffusion of Momentum

$$\dot{I} = -\underbrace{\rho \nu}_{\mu} \frac{dU}{dy} A \quad (2.2)$$

Molecular Diffusion of Heat

$$\dot{Q} = -\underbrace{\rho c_p a}_{\lambda} \frac{dT}{dy} A \quad (2.3)$$

D , ν , and a are the molecular diffusion coefficients, each with the unit [m²/s]. They are material properties of the fluid.

2.2. Turbulent Diffusion

Turbulent exchange processes are modeled in analogy to the molecular transport phenomena under the assumption that there is also a linear dependence between the exchange flux of mass, momentum, or energy and the gradient of the corresponding quantity.

Turbulent Diffusion of Mass

$$\dot{m} = -\rho D_t \frac{dc}{dy} A \quad (2.4)$$

Turbulent Diffusion of Momentum

$$\dot{I} = - \underbrace{\rho \nu_t}_{\mu_t} \frac{dU}{dy} A \quad (2.5)$$

Turbulent Diffusion of Heat

$$\dot{Q} = - \underbrace{\rho c_p a_t}_{\lambda_t} \frac{dT}{dy} A \quad (2.6)$$

D_t , ν_t , and a_t are the turbulent diffusion coefficients, each with the unit [m²/s]. They are *no* material properties and depend on the location in the flow domain. The molecular properties ν and a partially differ strongly, i.e., the Prandtl number $Pr = \nu / a$ deviates from 1. In contrast, the turbulent Prandtl number is approximately within the ratio $0.5 < Pr_t = \nu_t / a_t < 1.0$. That means, the turbulent kinematic viscosity ν_t is of the same order of magnitude as the turbulent temperature diffusivity a_t . For practical purposes the value $Pr_t = 0.9$ is applied for fluids with low heat conductivity like air, water, etc. (*Oertel and Laurien (2003)*). An analogous assumption is done for the turbulent Schmidt number $Sc_t = \nu_t / D_t$.

In case of a density gradient in y direction (e.g. due to a temperature gradient) and under the assumption that the turbulent diffusion coefficients for different quantities are equal

$$\Gamma_t := D_t = \nu_t = a_t, \quad (2.7)$$

Equations (2.4) through (2.6) yield for phase or field k :

$$\dot{m}_k = - \Gamma_t \frac{d(\alpha_k \rho_k)}{dy} A \quad (2.8)$$

$$\dot{I}_k = - \Gamma_t \frac{d(\alpha_k \rho_k U_k)}{dy} A = - \Gamma_t \frac{dG_k}{dy} A \quad (2.9)$$

$$\dot{Q}_k = - \Gamma_t \frac{d(\alpha_k \rho_k c_{p,k} T_k)}{dy} A = - \Gamma_t \frac{d(\alpha_k \rho_k h_k)}{dy} A \quad (2.10)$$

The variable for a general concentration c in Equation (2.4) was displaced by the volume fraction α_k of phase or field k . Concerning phases, one distinguishes between:

- liquid ,
- vapor .

Fields are for example:

- continuous liquid (“*liq*”),
- entrained liquid (“*ent*”),
- vapor (“*vap*”).

According to the nomenclature in the literature about nuclear engineering this turbulent diffusion will be called *turbulent mixing* from now on. The net lateral mixing mass, momentum, and energy flow rates in Equations (2.8), (2.9), and (2.10) can be applied to all three fields mentioned above under the assumption that the diffusion coefficients Γ_t are equal not only for all transport quantities, as assumed in Equation (2.7), but also for each field (see, e.g., Lahey and Moody (1993), p. 180)¹:

$$\begin{aligned} \dot{m}_{\text{liq}} &= -\Gamma_t \frac{d(\alpha_{\text{liq}} \rho_{\text{liq}})}{dy} A, & \dot{m}_{\text{ent}} &= -\Gamma_t \frac{d(\alpha_{\text{ent}} \rho_{\text{ent}})}{dy} A, \\ \dot{m}_{\text{vap}} &= -\Gamma_t \frac{d(\alpha_{\text{vap}} \rho_{\text{vap}})}{dy} A, \end{aligned} \quad (2.11)$$

$$\begin{aligned} \dot{I}_{\text{liq}} &= -\Gamma_t \frac{dG_{\text{liq}}}{dy} A, & \dot{I}_{\text{ent}} &= -\Gamma_t \frac{dG_{\text{ent}}}{dy} A, \\ \dot{I}_{\text{vap}} &= -\Gamma_t \frac{dG_{\text{vap}}}{dy} A, \end{aligned} \quad (2.12)$$

$$\begin{aligned} \dot{Q}_{\text{liq}} &= -\Gamma_t \frac{d(\alpha_{\text{liq}} \rho_{\text{liq}} h_{\text{liq}})}{dy} A, & \dot{Q}_{\text{ent}} &= -\Gamma_t \frac{d(\alpha_{\text{ent}} \rho_{\text{ent}} h_{\text{ent}})}{dy} A, \\ \dot{Q}_{\text{vap}} &= -\Gamma_t \frac{d(\alpha_{\text{vap}} \rho_{\text{vap}} h_{\text{vap}})}{dy} A. \end{aligned} \quad (2.13)$$

Replacing the *differential quotient* by the *difference quotient* (see Figure 5) yields for an arbitrary physical quantity Φ the following relations for the flux over the inter sub-channel gap ij :

$$\text{flux} = -\Gamma_t \frac{d\Phi}{dy} = -\tilde{\Gamma}_t \frac{\Delta\Phi}{\Delta y} = -\frac{\tilde{\Gamma}_t}{\Delta y} \Delta\Phi = -\frac{\tilde{\Gamma}_t}{\Delta y} \underbrace{(\Phi_j - \Phi_i)}_{\Delta\Phi} \quad (2.14)$$

¹ In contrast to this, Rudzinski et al. (1972) – e.g., referenced by Ninokata et al. (2004) and Sadatomi et al. (2004) – measured turbulent mixing rates separately for air and water in a two sub-channel system with identical inlet mass flow rates of air and water in both channels. The relation between air and water mixing rates changed with increasing void fraction while the relation of densities did not change. According to this, different turbulent mixing parameters for air and water were derived.

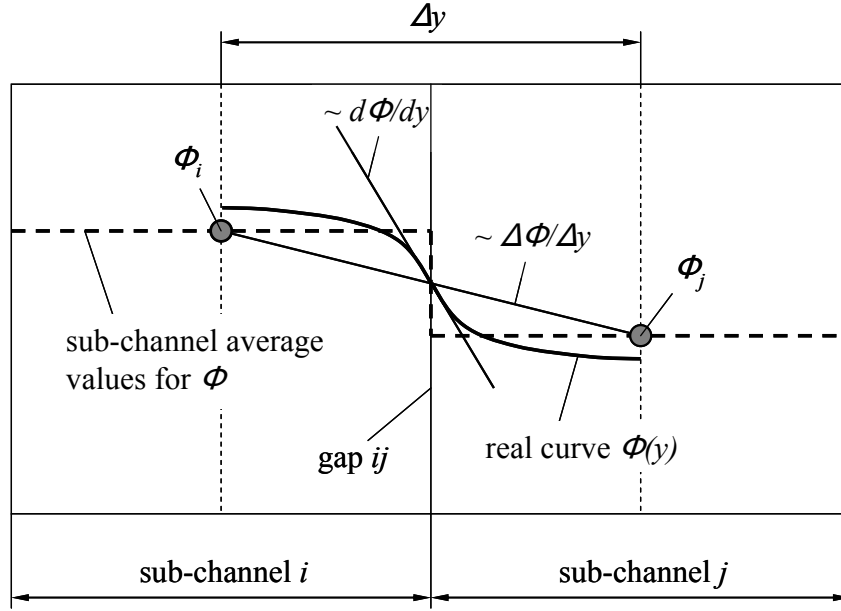


Figure 5. The sketch shows an example case of the real curve of a quantity Φ (void fraction, density, temperature, etc.) within two neighboring sub-channels. The differential quotient with respect to the lateral direction $d\Phi/dy$ is derived from the slope of the real curve at the inter sub-channel gap ij . Besides, the sub-channel averaged quantities Φ_i and Φ_j are added yielding the difference quotient $\Delta\Phi/\Delta y$.

Because the difference quotient does *not* represent the real slope of the curve $\Phi(y)$, the *real* diffusion coefficient Γ_t in the formulation above is replaced by an *artificial* diffusion coefficient $\tilde{\Gamma}_t$ defined as follows:

$$\tilde{\Gamma}_t = \frac{\frac{d\Phi}{dy}}{\frac{\Delta\Phi}{\Delta y}} \Gamma_t . \quad (2.15)$$

As depicted in Figure 5, the real slope of the curve at the gap is usually several times higher than the secant between the two sub-channel centers. Therefore, the artificial diffusion coefficient has to be higher than the real one.

In sub-channel analysis the following definition is commonly used:

$$\frac{\tilde{\Gamma}_t}{\Delta y} := \beta \bar{U} , \quad (2.16)$$

whereas β is named as *turbulent diffusion coefficient* or *turbulent mixing coefficient*. The derivation of this formula will be shown in the following.

1) Derivations Based on the Turbulent Diffusion Coefficient Γ_t

Starting from Equation (2.7) and inserting subsequently a turbulent length scale L_t , the eddy viscosity model from Equation (2.22), and the turbulence intensity Tu yields²:

$$\frac{\Gamma_t}{\Delta y} = \frac{\nu_t}{\Delta y} = \underbrace{\frac{\nu_t}{L_t}}_{c_\mu U_t} \frac{L_t}{\Delta y} = c_\mu U_t \frac{L_t}{\Delta y} = c_\mu \underbrace{\sqrt{\frac{3}{2}} Tu \bar{U}}_{U_t} \frac{L_t}{\Delta y} = \underbrace{\sqrt{\frac{3}{2}} c_\mu Tu \frac{L_t}{\Delta y} \bar{U}}_{\propto \beta \bar{U}} \propto \beta \bar{U} \quad (2.17)$$

2) Derivations Based on the *Artificial* Turbulent Diffusion Coefficient $\tilde{\Gamma}_t$

According to Equations (2.16) and (2.17) it follows for the artificial turbulent diffusion coefficient:

$$\frac{\tilde{\Gamma}_t}{\Delta y} = \frac{\tilde{\nu}_t}{\Delta y} = \underbrace{\sqrt{\frac{3}{2}} c_\mu \tilde{Tu} \frac{L_t}{\Delta y} \bar{U}}_{\beta} = \beta \bar{U} = \beta \underbrace{\frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}} \bar{U}}}_{\bar{U}} \rightarrow \beta = \frac{\tilde{\Gamma}_t}{\Delta y \bar{U}} \quad (2.18)$$

$$\rightarrow \beta = \frac{1}{\Delta y \bar{U}} \tilde{\nu}_t = \frac{\bar{\rho}_{\text{mix}}}{\Delta y \bar{G}_{\text{tot}}} \tilde{\nu}_t .$$

The mean values in the equations above are averaged over the adjacent sub-channels i and j as follows¹⁵:

$$\bar{G}_{\text{tot}} = \frac{G_{\text{tot},i} + G_{\text{tot},j}}{2}$$

$$\bar{\rho}_{\text{mix}} = \frac{\rho_{\text{mix},i} + \rho_{\text{mix},j}}{2} \text{ with } \rho_{\text{mix}} = \alpha_{\text{liq}} \rho_{\text{liq}} + \alpha_{\text{vap}} \rho_{\text{vap}} .$$

Using Equations (2.11), (2.12), (2.13), (2.14), and the *turbulent mixing coefficient* β derived in Equation (2.18), the following equations for each transport term yield:

² In a lot of publications in nuclear technology “ $\Gamma_t / \Delta y$ ” is written as “ ε / l ” whereas ε is the turbulent kinematic viscosity! Instead, the present paper uses ν_t for the turbulent kinematic viscosity and ε for the turbulent dissipation rate, as it became standard in turbulence theory.

$$\begin{aligned}\dot{m}_{\text{liq}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \Delta(\alpha_{\text{liq}} \rho_{\text{liq}}) A, & \dot{m}_{\text{ent}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \Delta(\alpha_{\text{ent}} \rho_{\text{ent}}) A, \\ \dot{m}_{\text{vap}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \Delta(\alpha_{\text{vap}} \rho_{\text{vap}}) A,\end{aligned}\quad (2.19)$$

$$\begin{aligned}\dot{I}_{\text{liq}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \Delta G_{\text{liq}} A, & \dot{I}_{\text{ent}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \Delta G_{\text{ent}} A, \\ \dot{I}_{\text{vap}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \Delta G_{\text{vap}} A,\end{aligned}\quad (2.20)$$

$$\begin{aligned}\dot{Q}_{\text{liq}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \Delta(\alpha_{\text{liq}} \rho_{\text{liq}} h_{\text{liq}}) A, & \dot{Q}_{\text{ent}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \Delta(\alpha_{\text{ent}} \rho_{\text{ent}} h_{\text{ent}}) A, \\ \dot{Q}_{\text{vap}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \Delta(\alpha_{\text{vap}} \rho_{\text{vap}} h_{\text{vap}}) A.\end{aligned}\quad (2.21)$$

Different Methods to Determine the Turbulent Kinematic Viscosity

Eddy viscosity models are the mainly used turbulence models for engineering applications. In all of them the turbulent kinematic viscosity is assumed to be proportional to a characteristic turbulent length scale L_t and a characteristic turbulent velocity U_t :

$$\nu_t = \underbrace{c_\mu}_{\Lambda} L_t U_t \propto L_t U_t. \quad (2.22)$$

Eddy viscosity models are subdivided depending on the way how L_t and U_t are determined. Zero-Equation models (e.g., Prandtl's mixing length model) calculate both by means of algebraic relations. One-Equation models determine U_t by means of an additional transport equation and L_t still by applying an algebraic equation. Two-Equation models (e.g., the widely used k - ε model) solve two additional transport equations for L_t and U_t , respectively. In the literature lots of different modeling approaches can be found. Below, only three examples should be mentioned.

In the special case of the well-known k - ε model the turbulent kinematic viscosity is

$$\nu_t = c_\mu \frac{k^2}{\varepsilon}, \quad (2.23)$$

with the turbulent kinetic energy k and the turbulent dissipation rate ε . According to Equation (2.18) it follows for the turbulent mixing coefficient:

$$\beta = \frac{c_\mu \bar{\rho}_{\text{mix}}}{\Delta y \bar{G}_{\text{tot}}} \frac{\tilde{k}^2}{\tilde{\varepsilon}}. \quad (2.24)$$

Nordsveen et al. (2003) use either a Prandtl type model to determine the turbulent viscosity³ of phase k

$$\nu_{t,k} = \underbrace{\left(\frac{d_{\text{gap}} \Delta y}{d_{\text{gap}} + \Delta y} \right)^2}_{l_m} \frac{\Delta U_k}{\Delta y} + \frac{\nu_{i,k} + \nu_{j,k}}{2} \quad (2.25)$$

or a model due to *Ingesson (1969)*

$$\nu_{t,k} = 6.12 \frac{d_{\text{pitch}}}{d_{\text{hyd}}} \frac{\nu_k Re_k}{20} \sqrt{\frac{0.18 Re_k^{-0.2}}{8}}. \quad (2.26)$$

2.3. Single-Phase Turbulent Mixing Coefficient β_{SP}

2.3.1. Correlations for β_{SP}

In sub-channel analysis, the turbulent kinematic viscosity is usually not directly estimated as shown in the paragraph above. Instead the mixing coefficient – also named as TDC (“turbulent diffusion coefficient”) value or as mixing Stanton number – is approximated. In the literature, there are a lot of different correlations for the mixing coefficient. Some selected approaches are listed below. Most of the correlations suggest that the turbulent mixing is a function of the following variables:

$$\beta_{\text{SP}} = f(\text{Re}, d_{\text{hyd}}, d_{\text{gap}}, d_{\text{rod}}, \Delta y)$$

- *Stewart et al. (1977)*⁴:

$$\beta_{\text{SP}} = a Re^b \quad (2.27)$$

$$\beta_{\text{SP}} = a Re^b \frac{d_{\text{hyd}}}{d_{\text{gap}}} \quad (2.28)$$

$$\beta_{\text{SP}} = a Re^b \frac{d_{\text{hyd}}}{\Delta y} \quad (2.29)$$

³ Strictly speaking, $\nu_{t,k}$ is the total kinematic viscosity, that means the sum of the turbulent viscosity (first term on the RHS of Equation (2.25)) and the laminar viscosity (second term). The turbulent part is calculated by means of Prandtl's mixing length theory. This model determines the turbulent viscosity by means of the lateral gradient of the axial velocity of phase k . However, it should be mentioned that in sub-channel analysis the gradients in lateral direction never represent the local conditions at the gap, but only the difference quotient between sub-channel average axial velocities (as depicted accordingly in Figure 5). This is a much too coarse formulation. Thus models based on such gradients might pretend a physical accuracy which they are not able to provide.

- *Beus (1970)*:

$$\beta_{\text{SP}} = 0.0035 \text{Re}^{-0.1} \frac{d_{\text{hyd}}}{d_{\text{gap}}} \quad (2.30)$$

- *Rowe and Angle (1967)*:

$$\beta_{\text{SP}} = 0.0062 \text{Re}^{-0.1} \frac{\bar{d}_{\text{hyd}}}{d_{\text{gap}}} \text{ with } \bar{d}_{\text{hyd}} = \frac{4(A_i + A_j)}{p_{w,i} + p_{w,j}} \quad (2.31)$$

- *Rogers and Rosehart (1972)*:

$$\beta_{\text{SP}} = \frac{1}{2} \underbrace{0.0058 \left(\frac{d_{\text{gap}}}{d_{\text{rod}}} \right)^{-1.46}}_{\lambda} \text{Re}^{-0.1} \left[1 + \left(\frac{d_{\text{hyd},j}}{d_{\text{hyd},i}} \right)^{1.5} \right] \frac{d_{\text{hyd},i}}{d_{\text{rod}}} \quad (2.32)$$

- *Rogers and Tahir (1975)*:

$$\beta_{\text{SP}} = 0.005 \left(\frac{d_{\text{gap}}}{d_{\text{rod}}} \right)^{0.106} \text{Re}^{-0.1} \frac{d_{\text{hyd}}}{d_{\text{gap}}} \quad (2.33)$$

- *Sadatomi et al. (1996) and Sadatomi et al. (2003)* (for a square bundle array):

$$\beta_{\text{SP}} = 0.00562 \left(\frac{d_{\text{hyd}}}{d_{\text{gap}}} \right) \frac{\text{Re}^{-1/8}}{F_i^*} + 0.0018 \left(\frac{d_{\text{gap}}}{d_{\text{rod}}} \right)^{-0.52} \quad (2.34)$$

Turbulent mixing is divided into two components of the turbulent diffusion and the convective transfer due to large-scale periodic eddies. In the equation above, the sub-channel geometry factor F_i^* explains the change in the gap clearance along the distance between the centroids of the sub-channels. F_i^* is integrated from the sub-channel contour.

- *Kim and Chung (2001)*:

The authors assumed that turbulent mixing is composed of a molecular motion, isotropic turbulent motion, and a flow pulsation and suggested a relatively complicated correlation⁵.

Unfortunately, in Equations (2.27) through (2.34) it is in most cases not clearly specified by the authors what exactly is d_{hyd} . The only exception is Equation (2.31) defining a mean

⁴ referenced in *Sadatomi et al. (1996)*

⁵ referenced in *Jeong et al. (2004)*

value $\bar{d}_{\text{hyd}}$ for both adjacent sub-channels i and j . This is probably the best choice for the remaining correlations, too.

Differences between Selected Correlations for β_{SP}

These correlations differ strongly from each other. In case of equal hydraulic diameters in adjacent sub-channels i and j ($d_{\text{hyd},i} = d_{\text{hyd},j} = \bar{d}_{\text{hyd}}$) the following relations yield:

$$\beta_{\text{SP, RoweAngle}} = 1.77 \beta_{\text{SP, Beus}}$$

$$\beta_{\text{SP, RogersRosehart}} = 1.66 \left(\frac{d_{\text{gap}}}{d_{\text{rod}}} \right)^{-0.46} \beta_{\text{SP, Beus}}$$

$$\beta_{\text{SP, RogersTahir}} = 1.43 \left(\frac{d_{\text{gap}}}{d_{\text{rod}}} \right)^{0.106} \beta_{\text{SP, Beus}}$$

For two example geometries the following relations yield:

- *GE 3x3 rod bundle:*
(see *Janssen (1971)*)

$\beta_{\text{SP, RogersRosehart}}$	$= 2.91 \beta_{\text{SP, Beus}}$
$\beta_{\text{SP, RogersTahir}}$	$= 1.26 \beta_{\text{SP, Beus}}$
- *STUDSVIK 3x3 rod bundle:*
(see *Gustafsson et al. (1974)*)

$\beta_{\text{SP, RogersRosehart}}$	$= 2.76 \beta_{\text{SP, Beus}}$
$\beta_{\text{SP, RogersTahir}}$	$= 1.27 \beta_{\text{SP, Beus}}$

Choice of Correlation

The choice of correlation depends mainly on the radial rod bundle design (square rod arrays, triangular rod arrays, etc.) and the particular spacer grid. In *Glück (2007)* and *Glück (2008)* recalculations of mixing tests and void measurements are comprehensively discussed. For square-array rod bundles with minimally invasive grids (with thin strips and without any vanes) the correlation by *Rogers and Tahir (1975)* and for rod bundles with spacer grids having vanes for effective swirl creation the correlation by *Rogers and Rosehart (1972)* is recommended. In case other correlations are used, it is highly recommended to study the original literature to figure out from which fluids (air-water, water-vapor, etc.) and under which flow conditions (pressure, mass flux, etc.) the particular correlation for the mixing coefficient was derived.

2.3.2. Evaluation of β_{sp} by Means of CFD

In principle, there are the following two different possibilities to determine the single-phase mixing coefficient by means of a CFD calculation.

1) Based on Local Turbulent Quantities at the Inter Sub-Channel Gap

At the first glance, it seems to be the most simple and straight forward way to calculate β from the local turbulent kinematic viscosity at the gap between the two corresponding sub-channels by means of Equation (2.18):

$$\beta = \frac{1}{\Delta y \bar{U}} \tilde{\nu}_t = \frac{\bar{\rho}_{\text{mix}}}{\Delta y \bar{G}_{\text{tot}}} \tilde{\nu}_t, \quad (2.35)$$

or in case the k - ε model is applied directly from the local turbulent kinetic energy and the turbulent dissipation rate according to Equation (2.24)

$$\beta = \frac{c_\mu}{\Delta y \bar{U}} \frac{\tilde{k}^2}{\tilde{\varepsilon}} = \frac{c_\mu \bar{\rho}_{\text{mix}}}{\Delta y \bar{G}_{\text{tot}}} \frac{\tilde{k}^2}{\tilde{\varepsilon}}. \quad (2.36)$$

However, the problem is that in the above mentioned formulas *not* the real local turbulent quantities occur, but the *artificial* ones ($\tilde{\nu}_t$, $\tilde{k}$, and $\tilde{\varepsilon}$) being related to the difference quotient which is based on Δy . A comprehensive derivation of that fact has already been shown in Paragraph 2.2 leading to Equations (2.17) and (2.18). In contrast to the real turbulent quantities (ν_t , k , and ε), the artificial ones can *not* be derived from a CFD calculation.⁶

Nevertheless, there are examples in literature studying the performance of this methodology. For example, *Ikeno* evaluated the turbulent mixing coefficient β by means of CFD calculations. These were based on the k - ε turbulence model *without* and *with* an analytical modification taking into account larger eddy structures which are neglected by the standard k - ε turbulence model. These large scale turbulent structures are claimed to contribute to inter sub-channel exchange by their cyclic flow pulsation through rod gaps. Doing the latter, he got one order of magnitude higher values for β than by taking the turbulent viscosity ν_t only from the not modified k - ε model and applying the following formula:

$$\beta = \frac{1}{\Delta y \bar{U}} \nu_t. \quad (2.37)$$

⁶ One exception is the turbulent kinematic viscosity ν_t which could be derived according to Equation (2.15). However, this formula is no more based on local quantities at the gap, only. Moreover, the difference quotient is used in this formula which can only be derived by calculating average sub-channel values. That means however, balances over a certain control volume have to be inserted here. But if balances are necessary in any case, it is much more recommendable to use methodology 2) at once, which will be described below.

In the authors opinion, the big difference in the resulting β is not only due to the described flow pulsation theory but is also due to the fact that Equation (2.37) is not correct – in contrast to Equation (2.35).

2) Based on Integral Balances over the Inter Sub-Channel Gap

Jeong et al. (2004) calculated the total heat flux between two neighboring sub-channels due to mixing (by means of a heat balance taking into account the inlet and outlet heat flow rates in two adjacent sub-channel control volumes) and reduced this by the molecular diffusion heat flux as follows:

$$\dot{q}_{\text{mix,turb}} = \dot{q}_{\text{mix,tot}} + \underbrace{\lambda \frac{\Delta T}{\Delta y}}_{-\dot{q}_{\text{mix,mol}}} . \quad (2.38)$$

According to Equations (2.6), (2.7), and (2.18) the single-phase mixing coefficient can be determined by

$$\beta_{\text{SP}} = - \frac{\dot{q}_{\text{mix,turb}}}{\bar{c}_p \bar{G} \Delta T} . \quad (2.39)$$

Jeong et al. (2004) also compared the mixing coefficient obtained by means of CFD to the mixing coefficients evaluated by different conventional correlations. They found that the mixing coefficient from CFD was similar to that obtained by the correlation according to *Kim and Chung (2001)*. However, it should be mentioned that in their example the molecular diffusion heat flux is higher than the turbulent one, because they dealt with liquid metal flows.

Usually, molecular diffusion can be neglected leading to the following simplifications:

$$\dot{q}_{\text{mix,turb}} \approx \dot{q}_{\text{mix,tot}} \rightarrow \beta_{\text{SP}} \approx - \frac{\dot{q}_{\text{mix,tot}}}{\bar{c}_p \bar{G} \Delta T} . \quad (2.40)$$

Jeong, Hahn et al. (2005) evaluated the turbulent mixing coefficient for the sodium flow in triangular rod arrays by means of the CFX code using a Reynolds stress model.

Avramova (2007) carried out a comprehensive study about possibilities to calibrate a newly developed spacer grid model in the sub-channel code F-COBRA-TF by means of preliminary CFD calculations determining the mixing behavior of state-of-the-art spacer grids. In her studies, the heat balance method 2) led to appropriate results, whereas method 1) based on local turbulent quantities under predicted β , in her case by a factor of three.

Conclusion

The only useful method to estimate the turbulent mixing coefficient β from CFD calculations seems to be method 2) being based on a (heat) *balance* by evaluating a (heat) flux over a defined gap area. In contrast to this, method 1) using local turbulent quantities at the gap seems to be not appropriate. It underestimates the turbulent mixing coefficient β by far.

2.4. Two-Phase Turbulent Mixing Coefficient β_{TP}

It has been observed, that turbulent mixing in two-phase flow can be much higher than in single-phase flow. The relation between the single-phase and the two-phase turbulent mixing coefficient can be described by means of a two-phase multiplier Θ_{TP} (see Faya (1979))⁷:

$$\beta_{\text{TP}} = \Theta_{\text{TP}} \beta_{\text{SP}} . \quad (2.41)$$

There are a lot of measurements to be found in literature examining the increase in mixing due to the two-phase effect. Some selected observations and derived methods to approximate the two-phase multiplier Θ_{TP} are listed below:

- Beus (1970)⁸:

The two-phase multiplier Θ_{TP} is assumed to be a function of the flow quality x accounting for the fact that the strength of turbulent exchange depends on the flow regime:

$$\Theta_{\text{TP}} = f(x) .$$

It reaches a maximum value at the transition to annular flow, where the flow quality is correlated according to Wallis (1969) as follows:

$$x_{\text{max}} = \frac{0.4 \sqrt{g \rho_{\text{liq}} (\rho_{\text{liq}} - \rho_{\text{vap}}) d_{\text{hyd}}} + 0.6}{\frac{G_{\text{tot}}}{\sqrt{\frac{\rho_{\text{liq}}}{\rho_{\text{vap}}}}} + 0.6} \quad (2.42)$$

In bubbly and slug flow there is a linear dependence between x and Θ_{TP} , whereas in annular flow Θ_{TP} decreases hyperbolically with x (see Figure 6):

⁷ Sugawara *et al.* (1991) propose that the *single-phase* turbulent mixing coefficient β_{SP} should be based already on the Reynolds number of the *two-phase* mixture when applied in Equation (2.34). The dependence of β_{SP} on the Reynolds number is usually supposed to be relatively weak (mostly $\beta_{\text{SP}} \propto \text{Re}^{-0.1}$ in Equations (2.27) through (2.34)). Moreover, the proportion of the dynamic viscosities of liquid and gas is not very high for water-vapor systems at high pressure. Thus, the values for β_{SP} calculated with μ_{liq} and β_{SP} calculated with μ_{vap} do not distinguish so much. For $P = 70$ bar follows $\beta_{\text{SP}}(\mu_{\text{vap}}) / \beta_{\text{SP}}(\mu_{\text{liq}}) = 0.86$ and for $P = 160$ bar follows $\beta_{\text{SP}}(\mu_{\text{vap}}) / \beta_{\text{SP}}(\mu_{\text{liq}}) = 0.90$. The proportion $\beta_{\text{SP}}(\mu_{\text{mix}}) / \beta_{\text{SP}}(\mu_{\text{liq}})$ is even closer to 1.0.

⁸ Beus did not use the two-phase multiplier terminology in his report. But in the present paper his model is formulated in terms of Θ_{TP} (according to Faya (1979) or Sugawara *et al.* (1991)).

$$\Theta_{TP} = 1 + (\Theta_{\max} - 1) \frac{x}{x_{\max}}, \quad \text{if } x \leq x_{\max}$$

$$\Theta_{TP} = 1 + (\Theta_{\max} - 1) \frac{x_{\max} - x_0}{x - x_0} \quad \text{with} \quad \frac{x_0}{x_{\max}} = 0.57 Re^{0.0417}, \quad \text{if } x > x_{\max}$$

whereas the Reynolds number here refers to the two-phase mixture:

$$Re = \frac{G_{\text{tot}} d_{\text{hyd}}}{\mu_{\text{mix}}} \quad \text{with} \quad \mu_{\text{mix}} = (1 - \alpha_{\text{vap}}) \mu_{\text{liq}} + \alpha_{\text{vap}} \mu_{\text{vap}}.$$

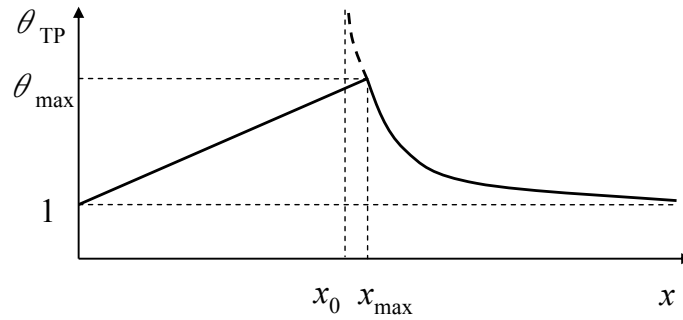


Figure 6. Two-phase multiplier Θ_{TP} as a function of flow quality x according to *Beus (1970)*.

$\Theta_{\max}$ is the maximum ratio of β_{TP} / β_{SP} occurring in this model at the transition to annular flow. Its value has to be specified as a parameter of the above mentioned mixing model. *Faya (1979)* recommends a value of

$$\Theta_{\max} = 5,$$

which he found from numerical experiments with the CANAL computer code in order to recalculate the GE 3x3 bundle mixing tests (see *Janssen (1971)*). *Kelly (1980)* found relatively small differences in the results when using the maximum two-phase multiplier in the range of $\Theta_{\max} = 5 \dots 10$.

- *Sugawara et al. (1991)*:

This model uses the same basic hypotheses as the model by *Beus (1970)*, however it is assumed that the two-phase multiplier Θ_{TP} reaches the highest value not at the transition from slug to annular flow as it is in *Beus (1970)* but short behind the transition from bubbly to slug flow, leading to the following relation, which was found by evaluating air-water tests:

$$x_{\max, \text{Sugawara}} = 0.22 x_{\max, \text{Beus}} .$$

$\Theta_{\max}$ is assumed to correlate with the superficial velocity of liquid j_{liq} as follows:

$$\Theta_{\max} = 37 \left(\frac{j_{\text{liq}}}{1.0 \text{ m/s}} - 1 \right)^2 + 13 .$$

- *Gonzalez-Santalo and Griffith (1972):*

From air-water measurements strong flow regime dependence was observed, showing an increase in the mixing coefficient by a factor of $\Theta_{\max} = 50$. Concerning the qualitative behavior of Θ_{TP} , no sharp peak as in *Beus (1970)* and *Sugawara et al. (1991)* was observed but a steep increase within the bubbly flow regime to the maximum value $\Theta_{\max}$, which lasts within the whole slug flow regime. Only behind the slug to annular transition a steep descent of Θ_{TP} was observed in the experiment.

- *Bellil et al. (1999):*

Air-water measurements were carried out in two adjacent sub-channels. The increase in turbulent mixing rate in slug flow relative to single-phase liquid flow reached values in the range of $\Theta_{\max} = 5 \dots 10$ depending on the liquid mass flux.

- *Rudzinski et al. (1972)*⁹:

Air-water measurements were carried out with two adjacent sub-channels. The increase of the turbulent mixing rate in the middle of churn flow¹⁰ related to single-phase liquid flow reached values of approximately $\Theta_{\max} = 5$.

- *Rowe and Angle (1969):*

The report by *Rowe and Angle (1969)*, p. 39, and the textbook by *Lahey and Moody (1993)*, p. 173, contain a lot of curves showing the two-phase mixing parameter over the steam quality - with and without spacers. In general there is a maximum to be found within the slug flow regime significantly before the flow regime turns to annular flow.

The studies mentioned above are selected examples only. There are much more air-water and steam-water experiments showing the influence of the flow regime on the intensity of turbulent mixing (e.g., air-water tests by *Kano et al. (2002)*, *Kano et al. (2003)*, ...).

⁹ referenced in a review paper by *Sato (1992)*

¹⁰ In this paper all flow regimes between bubbly flow and annular flow are called churn flow. Slug flow does not exist separately.

2.5. Influence of Spacer Grids on Turbulent Mixing

Not only flow regime characteristics influence turbulent mixing in rod bundles. There are also important geometrical effects. E.g., spacer grids cause a local increase in lateral exchange. The impact on the total turbulent mixing coefficient $\beta_{TP,tot}$ could be described in the simplest way by a special spacer multiplier Θ_{Spacer} :

$$\beta_{TP,tot} = \Theta_{Spacer} \underbrace{\Theta_{TP} \beta_{SP}}_{\beta_{TP}} \quad (2.43)$$

One possible qualitative curve for such a multiplier over axial bundle height could be the one depicted in Figure 7. The idea is to calibrate Θ_{Spacer} by means of CFD calculations resolving the relevant spacer grids in detail. But there are also spacer designs including vanes which have a more complicate impact on the lateral flow (e.g., preferred directions for lateral exchange leading to non-isotropic diffusion coefficients). Those can not be covered by the simple Equation (2.43).

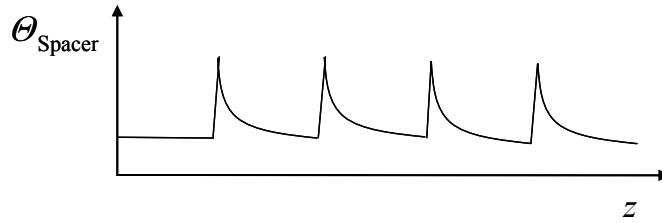


Figure 7. Possible qualitative characteristic of a mixing multiplier due to spacer effects.

A simple approach modeling the influence of a spacer on the turbulent viscosity is shown in *Lahey and Moody (1993)*, p. 184 and *Lahey et al. (1972)*.

As already mentioned in Paragraph 2.4, the report by *Rowe and Angle (1969)*, p.39, and the textbook by *Lahey and Moody (1993)*, p. 173, contain a lot of curves showing the two-phase mixing parameter over the steam quality - with and without spacers.

Hotta et al. (2004) mention a three-step strategy to improve cross flow models, which include as step (2) “The non-isotropic diffusion coefficient model will be introduced in order to include localized geometrical effects and the non-diffusive cross flow process.”

Ikeno, showed the behavior of the mixing coefficient β over the axial height within and above a spacer. These results were gained by means of CFD calculations (see also Paragraph 2.3.2).

Avramova (2007) developed a spacer grid model consisting of two sub-models. The first one is according to Equation (2.43). It is able to take into account enhanced swirl within a sub-channel downstream of a spacer grid. The second sub-model is applicable for spacer grids creating mainly directed cross flows due to their specific vane arrangement. Additional sources are placed into the momentum equation, selectively for certain rod-to-rod gaps. Both sub-models were calibrated at single-phase CFD calculations, each for a wide range of vane

angles. The whole model was implemented in the sub-channel code F-COBRA-TF yielding good results in recalculation of mixing tests.

2.6. Relation between Turbulent and Molecular Viscosity

There are a lot of correlations in literature – especially for rod bundles – which estimate the turbulent viscosity from the molecular viscosity.

- Many authors (e.g., *Rogers and Todreas (1968)*):

$$\frac{\nu_t}{\nu} \propto Re \sqrt{f} \quad \text{with the friction factor} \quad f = f\left(Re, \frac{\varepsilon}{d_{\text{hyd}}}\right)$$

- *Rehme (1992)*¹¹:

$$\frac{\nu_t}{\nu} = 0.05 Re \sqrt{f} \quad \text{with the friction factor} \quad f = 0.023 Re^{-0.2}$$

- *Sadatomi et al. (1996)*:

$$\frac{\nu_t}{\nu} = 0.04 Re \sqrt{f} \quad \text{with the friction factor} \quad f = 0.079 Re^{-0.25} \quad \text{according to Blasius.}$$

2.7. Overview over Net Transfer due to Single-Phase and Two-Phase Turbulent Mixing

An overview over the effect of turbulent mixing for different flow conditions is given in Table 1. *Lahey and Moody (1993)*, p. 179, referring to *Gonzalez-Santalo (1971)* made the assumption that turbulent mixing is based on an **equal volume** exchange. The following examples should illustrate the meaning of this kind of exchange:

- 1) If a gas bubble moves from one sub-channel to another the same volume of liquid flows in reverse direction.
- 2) In case of non-isothermal single-phase liquid flow that means, there is also a net mass exchange of liquid between the sub-channels because the densities of exchanged equal volumes are different.

¹¹ referenced in *Jeong et al. (2004)*

- 3) Only in case of isothermal single-phase flow that means, there is only momentum exchange and no net mass or energy exchange because same volumes contain same masses, too.

Table 1. Overview over the driving potentials leading to net transfer of mass, momentum, and energy due to single-phase and two-phase turbulent mixing

	Net lateral <i>mass</i> transfer of phase/field <i>k</i>	Net lateral <i>momentum</i> transfer of phase/field <i>k</i>	Net lateral <i>enthalpy</i> transfer of phase/field <i>k</i>
Lateral transport equation Quantity to be balanced in transport equation	$\dot{m}_k = -\Gamma_t \frac{d(\alpha_k \rho_k)}{dy} A$ $\alpha_k \rho_k$	$\dot{I}_k = -\Gamma_t \frac{d(\alpha_k \rho_k U_k)}{dy} A$ $G_k = \alpha_k \rho_k U_k$	$\dot{Q}_k = -\Gamma_t \frac{d(\alpha_k \rho_k h_k)}{dy} A$ $\alpha_k \rho_k h_k$
Single-phase flow (isothermal) $\alpha_{liq} = 1$ $\rho = \text{const}$ $h = \text{const}$	–	due to lateral gradient in: ➤ velocity U	–
Single-phase flow (non-isothermal) $\alpha_{liq} = 1$	due to lateral gradient in: ➤ density ρ	due to lateral gradient in: ➤ density ρ ➤ velocity U	due to lateral gradient in: ➤ density ρ ➤ enthalpy h
Two-phase flow (isothermal) $\rho_k = \text{const}$ $h_k = \text{const}$	due to lateral gradient in: ➤ volume fraction α_k	due to lateral gradient in: ➤ volume fraction α_k ➤ velocity U_k	due to lateral gradient in: ➤ volume fraction α_k
Two-phase flow (non-isothermal)	due to lateral gradient in: ➤ volume fraction α_k ➤ density ρ_k	due to lateral gradient in: ➤ volume fraction α_k ➤ density ρ_k ➤ velocity U_k	due to lateral gradient in: ➤ volume fraction α_k ➤ density ρ_k ➤ enthalpy h_k

3. Void Drift

Besides turbulent mixing which is due to a gradient diffusion process (see Paragraph 2) there is another important phenomena influencing the vapor distribution which is called “void drift”. *Schraub et al. (1969)* and *Shiralkar (1970)* observed in adiabatic and diabatic experiments in eccentric annuli and 3x3 rod bundles, that vapor apparently has a strong affinity for the *more open, higher velocity* regions. That means, under certain geometrical or

flow conditions the disperse vapor phase does *not* tend to be distributed homogeneously. Moreover, the flow adjusts itself to a *hydrodynamic equilibrium* void distribution, which is hypothesized to depend on both geometrical and flow parameters. One example for such an equilibrium condition is shown in Figure 8. A similar behavior has also been mentioned already in Paragraph 0 in connection with the GE 3x3 mixing tests.

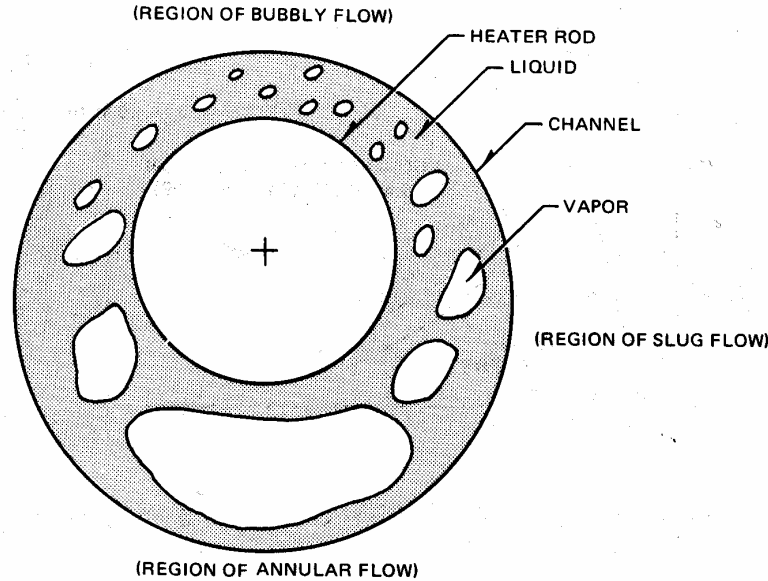


Figure 8. Typical void distribution in eccentric annuli (Source: *Lahey and Moody (1993)*).

Lahey and Moody (1993) wrote in their monograph: „The complete understanding and quantification of this ‚void drift’ phenomena in two-phase flow remains one of the fundamental unsolved problems in two-phase flow today. Nevertheless, to develop accurate sub-channel techniques, approximate void drift models must be synthesized.” This lack of accurate physical understanding seems to be still the state-of-the-art, nowadays. In the following, the well-known void drift model according to *Lahey and Moody (1993)* will be presented. Besides, more recent approaches will be discussed.

3.1. Void Drift Models on the Basis of Lahey’s Approach

Equation (2.19) describes the lateral mass flow rate over the gap from sub-channel i to sub-channel j due to turbulent mixing only. For the vapor phase this equation reads completely:

$$\dot{m}_{\text{vap}} = -\beta \underbrace{\frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}}}_{\frac{\bar{r}_t}{\Delta y}} \Delta(\alpha_{\text{vap}} \rho_{\text{vap}}) A = -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{vap},j} \rho_{\text{vap},j} - \alpha_{\text{vap},i} \rho_{\text{vap},i}) A \quad (3.1)$$

In the phenomenological void drift model by *Lahey and Moody (1993)* it is first assumed that net two-phase mixing (including void drift) is proportional to the *non-equilibrium* void fraction *gradient*:

$$\dot{m}_{\text{vap}} = -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \left[(\alpha_{\text{vap},j} \rho_{\text{vap},j} - \alpha_{\text{vap},i} \rho_{\text{vap},i}) - (\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}}) \right] A \quad (3.2)$$

This hypothesis implies that there is a strong trend toward the equilibrium distribution. When this state has been reached, which is indicated by

$$\alpha_{\text{vap},j} \rho_{\text{vap},j} = \alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} \quad \text{and} \quad \alpha_{\text{vap},i} \rho_{\text{vap},i} = \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}} ,$$

the net exchange due to mixing and void drift ceases. Rearranging of terms in Equation (3.2) clarifies that fact:

$$\dot{m}_{\text{vap}} = -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \left[(\alpha_{\text{vap},j} \rho_{\text{vap},j} - \alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}}) - (\alpha_{\text{vap},i} \rho_{\text{vap},i} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}}) \right] A \quad (3.3)$$

The RHS of Equation (3.2) can be divided into two different terms. That means, turbulent diffusion and void drift can clearly be separated:

$$\dot{m}_{\text{vap}} = \underbrace{-\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{vap},j} \rho_{\text{vap},j} - \alpha_{\text{vap},i} \rho_{\text{vap},i}) A}_{\text{turbulent diffusion (mixing)}} + \underbrace{\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}}) A}_{\text{void drift}} \quad (3.4)$$

Turbulent mixing tries to equalize the radial void distribution, whereas void drift tries to reach a hydrodynamic equilibrium void distribution. As a result, there are cases when the phenomena void drift and turbulent mixing oppose each other. In the special case that the equilibrium conditions of sub-channels i and j are equal (e.g., due to the same geometry and the same total mixture mass fluxes in both sub-channels), which is indicated by

$$\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} = \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}} ,$$

there will be *no* void drift. Only turbulent mixing is active, see Equation (3.4).

In case of constant vapor densities in the regarded sub-channels ($\rho_{\text{vap},i} = \rho_{\text{vap},j}$) the above mentioned Equations (3.2), (3.3), and (3.4) become simplified as follows:

$$\begin{aligned}
\dot{m}_{\text{vap}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \rho_{\text{vap}} \left[(\alpha_{\text{vap},j} - \alpha_{\text{vap},i}) - (\alpha_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}}) \right] A \\
\dot{m}_{\text{vap}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \rho_{\text{vap}} \left[(\alpha_{\text{vap},j} - \alpha_{\text{vap},j,\text{EQ}}) - (\alpha_{\text{vap},i} - \alpha_{\text{vap},i,\text{EQ}}) \right] A \\
\dot{m}_{\text{vap}} &= \underbrace{-\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \rho_{\text{vap}} (\alpha_{\text{vap},j} - \alpha_{\text{vap},i}) A}_{\text{turbulent diffusion (mixing)}} + \underbrace{\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \rho_{\text{vap}} (\alpha_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}}) A}_{\text{void drift}} .
\end{aligned}$$

The same derivation can be used for the lateral momentum exchange:

$$\begin{aligned}
\dot{i}_{\text{vap}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \left[(G_{\text{vap},j} - G_{\text{vap},i}) - (G_{\text{vap},j,\text{EQ}} - G_{\text{vap},i,\text{EQ}}) \right] A \\
\dot{i}_{\text{vap}} &= -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} \left[(G_{\text{vap},j} - G_{\text{vap},j,\text{EQ}}) - (G_{\text{vap},i} - G_{\text{vap},i,\text{EQ}}) \right] A \\
\dot{i}_{\text{vap}} &= \underbrace{-\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (G_{\text{vap},j} - G_{\text{vap},i}) A}_{\text{turbulent diffusion (mixing)}} + \underbrace{\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (G_{\text{vap},j,\text{EQ}} - G_{\text{vap},i,\text{EQ}}) A}_{\text{void drift}}
\end{aligned}$$

as well as for the lateral transfer of energy due to the combined mixing and void drift phenomena:

$$\begin{aligned}
\dot{Q}_{\text{vap}} &= \underbrace{-\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{vap},j} \rho_{\text{vap},j} h_{\text{vap},j} - \alpha_{\text{vap},i} \rho_{\text{vap},i} h_{\text{vap},i}) A}_{\text{turbulent diffusion (mixing)}} \\
&\quad + \underbrace{\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} h_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}} h_{\text{vap},i,\text{EQ}}) A}_{\text{void drift}}
\end{aligned}$$

All the above mentioned equations refer – as an example – to the vapor phase. They also hold in corresponding form for the continuous liquid field and the entrained liquid field.

3.1.1. Equilibrium Void Fraction Distribution

The aforementioned void drift model is easy to use, provided one knows the equilibrium void fraction and the equilibrium mass flux distribution. Several approaches were developed to predict these equilibrium distributions. The first and simplest one by *Levy (1963)* is discussed in Paragraph 3.1.1.1. A more complicate and recent approach by *Hotta et al. (2004)* which is

to be implemented in the national Japanese NASCA code is described in Paragraph 3.1.1.2. Paragraph 3.1.1.3 contains a short overview about these two and further models.

3.1.1.1. Approach by Levy (1963)

Lahey and Moody (1993) refer to the simple model by *Levy (1963)*, which had been applied successfully by *Lahey et al. (1972)*. Levy's model is also discussed in *Sato (1992)*. It implies that the equilibrium density distribution (as a measure for the equilibrium void fraction distribution) is related to the mass flux distribution by the expression^{12,14}:

$$\rho_{\text{mix, EQ}} = \alpha_{\text{liq}} \rho_{\text{liq}} + \alpha_{\text{vap}} \rho_{\text{vap}} = a G_{\text{tot, EQ}} + b \quad (3.5)$$

Based on this model and by means of regarding certain boundary conditions, the relevant equilibrium term for lateral mass exchange in Equation (3.4) becomes

- in general form for phase k :

$$\begin{aligned} (\alpha_{k,j,\text{EQ}} \rho_{k,j,\text{EQ}} - \alpha_{k,i,\text{EQ}} \rho_{k,i,\text{EQ}}) &= f_{\text{sign}} \frac{\overline{\alpha_{\text{vap}} \rho_k}}{\overline{G_{\text{tot}}}} (G_{\text{tot},j,\text{EQ}} - G_{\text{tot},i,\text{EQ}}) \\ \text{with } f_{\text{sign}} &= 1 \quad \text{for } k = \text{vap} \\ \text{with } f_{\text{sign}} &= -1 \quad \text{for } k = \text{liq} , \\ \text{and } f_{\text{sign}} &= -1 \quad \text{for } k = \text{liq} , \end{aligned} \quad (3.6)$$

- for the vapor phase¹³:

$$(\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}}) = \frac{\overline{\alpha_{\text{vap}} \rho_{\text{vap}}}}{\overline{G_{\text{tot}}}} (G_{\text{tot},j,\text{EQ}} - G_{\text{tot},i,\text{EQ}}) , \quad (3.7)$$

- for the liquid phase¹⁴:

¹² That means the equilibrium distribution in Levy's model depends on the total mixture mass flux and not directly on geometrical parameters. The sub-channel geometries influence the mass fluxes only indirectly via different pressure losses in sub-channels having different cross-sections. More recent void drift models which will be discussed in Paragraph 3.2 try to overcome this lack.

¹³ Physically, this reflects the above mentioned, observed trend that the void fraction is higher in sub-channels with higher velocity or in other words that the target void fraction gradient between neighboring sub-channels is proportional to the target total mass flux gradient which will later be assumed to be equal to the actual total mass flux gradient (see Equation (3.13)):

$$\frac{(\alpha_{\text{vap},j} \rho_{\text{vap},j} - \alpha_{\text{vap},i} \rho_{\text{vap},i})_{\text{EQ}}}{\overline{\alpha_{\text{vap}} \rho_{\text{vap}}}} = \frac{(G_{\text{tot},j} - G_{\text{tot},i})_{\text{EQ}}}{\overline{G_{\text{tot}}}} = \frac{G_{\text{tot},j} - G_{\text{tot},i}}{\overline{G_{\text{tot}}}}$$

¹⁴ Void drift is assumed to occur only if vapor is the disperse phase and liquid is the continuous phase. That means, in case of active void drift there is no entrained liquid ($\alpha_{\text{ent}} = 0$ and $\alpha_{\text{liq}} + \alpha_{\text{vap}} = 1$). A slide deviation from this basic assumption can hold in *churn flow*, where void drift can be assumed as still active, when the entrained liquid phase (droplets) can already develop. According to the formulas in the present paper a void drift exchange always takes place between continuous liquid and vapor based on an equal volume approach.

$$\left(\alpha_{\text{liq},j,\text{EQ}} \rho_{\text{liq},j,\text{EQ}} - \alpha_{\text{liq},i,\text{EQ}} \rho_{\text{liq},i,\text{EQ}} \right) = - \frac{\overline{\alpha_{\text{vap}} \rho_{\text{liq}}}}{\overline{G_{\text{tot}}}} \left(G_{\text{tot},j,\text{EQ}} - G_{\text{tot},i,\text{EQ}} \right), \quad (3.8)$$

whereas the mean values are averaged over the adjacent sub-channels i and j as follows¹⁵:

$$\overline{G_{\text{tot}}} = \frac{G_{\text{tot},i} + G_{\text{tot},j}}{2} \quad (3.9)$$

$$\overline{\alpha_{\text{vap}} \rho_{\text{vap}}} = \frac{\alpha_{\text{vap},i} \rho_{\text{vap},i} + \alpha_{\text{vap},j} \rho_{\text{vap},j}}{2} \quad (3.10)$$

$$\overline{\alpha_{\text{vap}} \rho_{\text{liq}}} = \frac{\alpha_{\text{vap},i} \rho_{\text{liq},i} + \alpha_{\text{vap},j} \rho_{\text{liq},j}}{2} \quad (3.11)$$

Addition of Equations (3.7) and (3.8) yield under the assumption of the special case of constant densities concerning the regarded sub-channels ($\rho_{\text{vap},i} = \rho_{\text{vap},j}$):

$$\begin{aligned} & \underbrace{\left(\alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}} + \alpha_{\text{liq},i,\text{EQ}} \rho_{\text{liq},i,\text{EQ}} \right)}_{\rho_{\text{mix},i,\text{EQ}}} - \underbrace{\left(\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} + \alpha_{\text{liq},j,\text{EQ}} \rho_{\text{liq},j,\text{EQ}} \right)}_{\rho_{\text{mix},j,\text{EQ}}} \\ &= - \left(\rho_{\text{liq}} - \rho_{\text{vap}} \right) \frac{\overline{\alpha_{\text{vap}}}}{\overline{G_{\text{tot}}}} \left(G_{\text{tot},i,\text{EQ}} - G_{\text{tot},j,\text{EQ}} \right) \end{aligned} \quad (3.12)$$

Entrained liquid never participates in void drift. However, entrained liquid can be subject to turbulent mixing between neighboring sub-channels.

¹⁵ If one would be really interested in *average* values the mass fluxes, densities, and volume fractions should be weighted by the sub-channel cross-sectional areas A_i and A_j as follows:

$$\begin{aligned} \overline{G_{\text{tot}}} &= \frac{A_i G_{\text{tot},i} + A_j G_{\text{tot},j}}{A_i + A_j} \\ \overline{\alpha_{\text{vap}} \rho_{\text{vap}}} &= \frac{A_i \alpha_{\text{vap},i} \rho_{\text{vap},i} + A_j \alpha_{\text{vap},j} \rho_{\text{vap},j}}{A_i + A_j} \\ \overline{\alpha_{\text{vap}} \rho_{\text{liq}}} &= \frac{A_i \alpha_{\text{vap},i} \rho_{\text{liq},i} + A_j \alpha_{\text{vap},j} \rho_{\text{liq},j}}{A_i + A_j} \end{aligned}$$

But, actually not the mean values over the two sub-channels are relevant but rather the *value* at the *gap* between both sub-channels, where the lateral exchange takes place. Therefore, the arithmetic mean value is probably the best compromise. Then one is also consistent with the remaining average values in the void drift terms in the phasic mass, momentum, and energy equations – see Equations (3.14) through (3.19). Whereas in most of the publications (e.g. in *Jeong et al. (2005)*) the weighting is simplified leading to Equations (3.14) and (3.15), *Ninokata (2004b)* uses here also an area averaging according to:

$$\left(\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}} \right) = K_{\text{M}} \frac{G_{\text{tot},j} - G_{\text{tot},i}}{A_i G_{\text{tot},i} + A_j G_{\text{tot},j}} \left(A_i \alpha_{\text{vap},i} \rho_{\text{vap},i} + A_j \alpha_{\text{vap},j} \rho_{\text{vap},j} \right)$$

This equation corresponds to the model by *Levy (1963)*, which is referenced in *Lahey and Moody (1993)*, Equation (4.161). The equilibrium total mass flux distribution is recommended to be taken as the existing mass flux distribution, as a proper first approximation¹⁶:

$$G_{\text{tot},i,\text{EQ}} = G_{\text{tot},i} \ , \quad G_{\text{tot},j,\text{EQ}} = G_{\text{tot},j} \ . \quad (3.13)$$

Under this assumption and by means of Equations (3.9) through (3.11) the equilibrium terms in Equations (3.7) and (3.8) become

- for the vapor phase:

$$\left(\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}} \right) = K_M \frac{G_{\text{tot},j} - G_{\text{tot},i}}{G_{\text{tot},i} + G_{\text{tot},j}} \left(\alpha_{\text{vap},i} \rho_{\text{vap},i} + \alpha_{\text{vap},j} \rho_{\text{vap},j} \right), \quad (3.14)$$

- for the liquid phase:

$$\left(\alpha_{\text{liq},j,\text{EQ}} \rho_{\text{liq},j,\text{EQ}} - \alpha_{\text{liq},i,\text{EQ}} \rho_{\text{liq},i,\text{EQ}} \right) = -K_M \frac{G_{\text{tot},j} - G_{\text{tot},i}}{G_{\text{tot},i} + G_{\text{tot},j}} \left(\alpha_{\text{vap},i} \rho_{\text{liq},i} + \alpha_{\text{vap},j} \rho_{\text{liq},j} \right). \quad (3.15)$$

The corresponding equilibrium *momentum flux* distribution becomes

- for the vapor phase:

$$\begin{aligned} \left(G_{\text{vap},j,\text{EQ}} - G_{\text{vap},i,\text{EQ}} \right) &= K_M \frac{G_{\text{tot},j} - G_{\text{tot},i}}{G_{\text{tot},i} + G_{\text{tot},j}} \left(\underbrace{\alpha_{\text{vap},i} \rho_{\text{vap},i} U_{\text{vap},i}}_{= G_{\text{vap},i}} + \underbrace{\alpha_{\text{vap},j} \rho_{\text{vap},j} U_{\text{vap},j}}_{= G_{\text{vap},j}} \right) \\ &= K_M \frac{G_{\text{tot},j} - G_{\text{tot},i}}{G_{\text{tot},i} + G_{\text{tot},j}} \left(G_{\text{vap},i} + G_{\text{vap},j} \right) \end{aligned} \quad (3.16)$$

- for the liquid phase:

$$\begin{aligned} \left(G_{\text{liq},j,\text{EQ}} - G_{\text{liq},i,\text{EQ}} \right) &= -K_M \frac{G_{\text{tot},j} - G_{\text{tot},i}}{G_{\text{tot},i} + G_{\text{tot},j}} \left(\underbrace{\alpha_{\text{vap},i} \rho_{\text{liq},i} U_{\text{liq},i}}_{\neq G_{\text{liq},i}} + \underbrace{\alpha_{\text{vap},j} \rho_{\text{liq},j} U_{\text{liq},j}}_{\neq G_{\text{liq},j}} \right) \\ &= -K_M \frac{G_{\text{tot},j} - G_{\text{tot},i}}{G_{\text{tot},i} + G_{\text{tot},j}} \left(\frac{\alpha_{\text{vap},i}}{\alpha_{\text{liq},i}} G_{\text{liq},i} + \frac{\alpha_{\text{vap},j}}{\alpha_{\text{liq},j}} G_{\text{liq},j} \right) \end{aligned} \quad (3.17)$$

¹⁶ But it is not assumed that the equilibrium phase mass fluxes are equal to the existing ones, because the equilibrium phase distributions should be reached due to the void drift phenomenon, i.e., $G_{\text{liq},\text{EQ}} \neq G_{\text{liq}}$ and

$G_{\text{vap},\text{EQ}} \neq G_{\text{vap}}$.

The corresponding equilibrium *energy* distribution becomes

- for the vapor phase:

$$\begin{aligned} & (\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} h_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}} h_{\text{vap},i,\text{EQ}}) \\ & = K_M \frac{G_{\text{tot},j} - G_{\text{tot},i}}{G_{\text{tot},i} + G_{\text{tot},j}} (\alpha_{\text{vap},i} \rho_{\text{vap},i} h_{\text{vap},i} + \alpha_{\text{vap},j} \rho_{\text{vap},j} h_{\text{vap},j}) \end{aligned} \quad (3.18)$$

- for the liquid phase:

$$\begin{aligned} & (\alpha_{\text{liq},j,\text{EQ}} \rho_{\text{liq},j,\text{EQ}} h_{\text{liq},j,\text{EQ}} - \alpha_{\text{liq},i,\text{EQ}} \rho_{\text{liq},i,\text{EQ}} h_{\text{liq},i,\text{EQ}}) \\ & = -K_M \frac{G_{\text{tot},j} - G_{\text{tot},i}}{G_{\text{tot},i} + G_{\text{tot},j}} (\alpha_{\text{vap},i} \rho_{\text{liq},i} h_{\text{liq},i} + \alpha_{\text{vap},j} \rho_{\text{liq},j} h_{\text{liq},j}) \end{aligned} \quad (3.19)$$

Note: It is important that in Equations (3.14) through (3.19) α_{vap} is included in all bracket terms on the RHS of each equation – even for the liquid phase. If by mistake α_{liq} were used instead of α_{vap} for the liquid phase, the mass balance would be violated. E.g., void drift would still be active even in case of single-phase liquid, when $\alpha_{\text{vap}} = 0$.

Factor of Proportionality K_M

In the original use of Levy's model by *Lahey and Moody (1993)* the empirical parameter K_M did not yet appear. It seems to be introduced later. Nowadays it is included in all citations and very often used as

$$K_M = 1.4 \quad (3.20)$$

(e.g., in *Kazimi and Kelly (1983)*, *Sugawara and Miyamoto (1990)*, *Sugawara et al. (1991)*, *Hotta et al. (1998)*). This value is based on the work of *Faya (1979)* who found best agreement with measurements, when K_M was in the range between 1.2 and 1.6. He evaluated the GE 3x3 mixing tests, the ISPRA PELCO-S 4x4 tests, and the STUDEVIK 3x3 tests (see Paragraph 1.2) with the sub-channel code CANAL (all at $P \approx 70$ bar).

Kelly (1980) varied the parameter K_M for case 2E2 of the GE 3x3 mixing tests ($P = 69$ bar, see *Janssen (1971)*) in a range of

$$K_M = 1.0 \dots 2.0$$

When K_M increases, the fully-developed void fraction profile (equilibrium distribution) becomes steeper and the amount of turbulent vapor diffusion will be greater. Kelly's results showed that as K_M is increased the quality in the corner sub-channel decreases and the mass velocity increases.

Jeong *et al.* (2006) proposed a pressure dependent correlation to determine K_M (see Figure 9):

$$K_M = 0.112 + 16.4 e^{-0.329 P} \quad \text{with } P \text{ in [MPa]} . \quad (3.21)$$

This correlation was derived from the GE 3x3 mixing tests ($P = 69$ bar) and the ISPRA EUROP 4x4 tests ($P = 160$ bar) and is thus recommended by the authors to be used for steam-water systems in the pressure range between ~ 69 bar and ~ 160 bar. The value from Equation (3.20), $K_M = 1.4$, corresponds to a pressure of $P = 77$ bar.

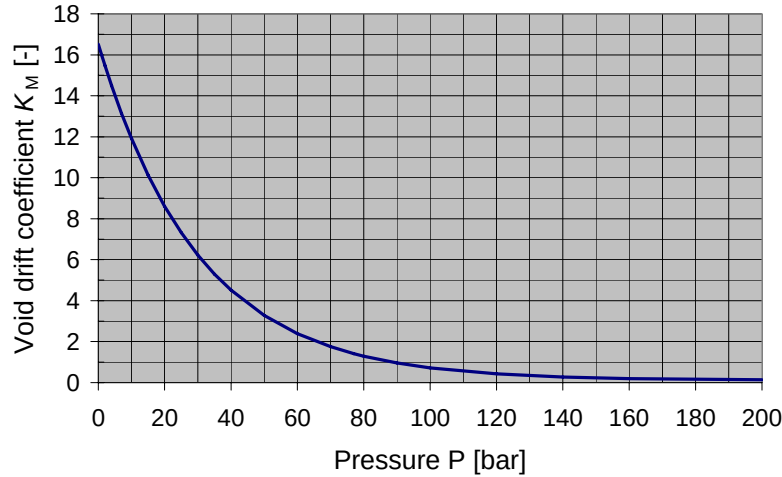


Figure 9. Pressure dependent correlation for void drift coefficient K_M according to Equation (3.21) proposed by Jeong *et al.* (2006).

Volume Constancy

In Paragraph 2.7 it was already mentioned that Lahey and Moody (1993), p. 179, referring to Gonzalez-Santalo (1971) made the assumption that turbulent mixing is based on an **equal volume** exchange. That means, if a gas bubble moves from one sub-channel to another the same volume of liquid flows in reverse direction. This holds also for the void drift phenomenon when it is modeled by the approach described above. One can see this from Equations (3.4), (3.14), and (3.15). Both exchange terms, for liquid and vapor, use the volume fraction of vapor α_{vap} , i.e., the net volume transfer of vapor from sub-channel i to sub-channel j is calculated first. As a result of this vapor movement the same volume of liquid is transferred in reverse direction from sub-channel j to sub-channel i .

Formal Acting of Void Drift in the Special Cases of Single Phase Liquid and Single Phase Vapor

In the special case of *single phase liquid*, when no vapor or gas exists at all, there is of course *no* void drift. If one looks at the formulas for the gradient in equilibrium void, mass flux, and

energy distribution, one can easily see that all mass, momentum and energy exchange terms become zero in case $\alpha_{\text{vap}} = 0$.

In the special case of *single phase vapor*, the void drift phenomenon is supposed not to act at all (see Paragraph 1.3). In the single phase vapor flow regime the void drift model *must not* be applied.

Remarks to the Driving Potential of Void Drift in Contrast to Turbulent Mixing

In the following, the driving potentials should be shown impressively at the example of lateral mass exchange due to turbulent mixing and void drift (The derivation of the formulas for the vapor phase will be shown step by step. The most important, corresponding equations for the liquid phase are added for the sake of comparison.):

$$\dot{m}_{\text{vap}} = \underbrace{-\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{vap},j} \rho_{\text{vap},j} - \alpha_{\text{vap},i} \rho_{\text{vap},i}) A}_{\text{turbulent diffusion (mixing)}} + \underbrace{\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}}) A}_{\text{void drift}}$$

$$\dot{m}_{\text{vap, TM}} = -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{vap},j} \rho_{\text{vap},j} - \alpha_{\text{vap},i} \rho_{\text{vap},i}) A$$

$$\dot{m}_{\text{liq, TM}} = -\beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{liq},j} \rho_{\text{liq},j} - \alpha_{\text{liq},i} \rho_{\text{liq},i}) A$$

$$\dot{m}_{\text{vap, VD}} = \beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}}) A$$

$$\dot{m}_{\text{liq, VD}} = \beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{liq},j,\text{EQ}} \rho_{\text{liq},j,\text{EQ}} - \alpha_{\text{liq},i,\text{EQ}} \rho_{\text{liq},i,\text{EQ}}) A$$

$$\dot{m}_{\text{vap, VD}} = \beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} K_M \frac{G_{\text{tot},j} - G_{\text{tot},i}}{G_{\text{tot},i} + G_{\text{tot},j}} (\alpha_{\text{vap},i} \rho_{\text{vap},i} + \alpha_{\text{vap},j} \rho_{\text{vap},j}) A$$

$$\dot{m}_{\text{vap, VD}} = \beta \frac{G_{\text{tot},i} + G_{\text{tot},j}}{2 \bar{\rho}_{\text{mix}}} K_M \frac{G_{\text{tot},j} - G_{\text{tot},i}}{G_{\text{tot},i} + G_{\text{tot},j}} (\alpha_{\text{vap},i} \rho_{\text{vap},i} + \alpha_{\text{vap},j} \rho_{\text{vap},j}) A$$

$$\dot{m}_{\text{vap, VD}} = \beta \frac{1}{\bar{\rho}_{\text{mix}}} K_M \frac{\alpha_{\text{vap},i} \rho_{\text{vap},i} + \alpha_{\text{vap},j} \rho_{\text{vap},j}}{2} (G_{\text{tot},j} - G_{\text{tot},i}) A$$

$$\dot{m}_{\text{vap, VD}} = \beta K_M \frac{\alpha_{\text{vap}} \rho_{\text{vap}}}{\bar{\rho}_{\text{mix}}} (G_{\text{tot},j} - G_{\text{tot},i}) A \quad \dot{m}_{\text{liq, VD}} = -\beta K_M \frac{\alpha_{\text{vap}} \rho_{\text{liq}}}{\bar{\rho}_{\text{mix}}} (G_{\text{tot},j} - G_{\text{tot},i}) A$$

In case of *turbulent mixing* the driving potentials are the lateral gradients in void fraction and phasic density (mass equation), void fraction, phasic density, and phasic velocity (momentum equation), and void fraction, phasic density, and phasic enthalpy (energy equation). The opposite sign of the lateral gradients of volume fractions of vapor and liquid determine the opposite directions of lateral vapor and liquid net mass flow rate (based on an equal volume exchange). Because the lateral gradients of volume fractions of vapor and liquid are exactly equal except their sign, the only difference in the amount of vapor and liquid mass flow rate is the difference in vapor and liquid densities.

In case of *void drift* the lateral gradient in total mass flux is the driving potential in all three basic transport equations. The a priori written plus and minus sign respectively in the formulas above determine the opposite directions of lateral vapor and liquid net mass flow rate (also based on an equal volume exchange). Because in **both** formulas (for vapor and liquid phase) the **volume fraction of vapor** is used, the only difference in the amount of vapor and liquid mass flow rate is again the difference in vapor and liquid densities. The latter aspect is also responsible for the effect that void drift is really only active in case void is available at all (as already described in the previous paragraph about acting of void drift in the special case of single-phase liquid).

The above mentioned driving potentials for turbulent mixing and void drift are also illustrated in Table 1 and Table 3.

3.1.1.2. Approach by Hotta et al. (2004)

The authors stated that in actual rod bundles, flow distribution will be influenced by many hydraulic factors such as the static pressure gradient, the lift force, the surface tension and the near-wall effects (Saffman force and lubrication force). The conventional formulae (by *Levy (1963)* or *Rowe et al. (1990)*) were too simple to incorporate these important effects.

Therefore, a new model has been developed. The bases for the decision, which improvements are necessary or desired, were the following available measurements:

- High resolution database at Kumamoto University (*Kawahara (1998)*, *Sadatomi et al. (2003)*)
- Lahey's GE 3x3 bundle tests at BWR conditions (*Lahey et al. (1972)*)
- Herkenrath's 4x4 bundle tests at BWR ("BWR-PELCO-S") and PWR ("PWR-EUROP") conditions (*Herkenrath et al. (1981)*).

The main physical phenomena of lateral liquid-vapor exchange were outlined. The new model is based on the following three macroscopic effects and satisfies volume constancy:

- *Fluid dynamic effect:*
This phenomenon, which depends on fluid properties like surface tension, density and viscosity, will be expressed as a function of the void fraction, because applying individual numerical models for each physical scale concerning the liquid-vapor interface seems to be impossible.
- *Geometrical effect:*
Although specific physical models (e.g. Saffman force or wall lubrication force) could be applied, this phenomenon, which depends on the bundle geometry, will be expressed as another function of the void fraction.

➤ *Narrow gap effect:*

This process will be expressed as a further function of the void fraction and is described in the paper as follows: In the bubbly flow, where the average void fraction is relatively low, the inlet non-equilibrium void distributions penetrate to the outlet with exhibiting little mixture behavior. Under these conditions, bubbles will flow mainly along the centerline of each channel and they seldom reach the gap region. Hence, the possibility of migration through gaps will be drastically lowered.

All three effects mentioned above are combined in one equation for the equilibrium void distribution (Model A in *Hotta et al. (2004)* – Pure fluid force model)¹⁷:

$$\begin{aligned}
 & \alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}} \\
 &= (1 - \Omega_{ij}(\alpha_{\text{vap}})) \left(\underbrace{K_{p,ij}(\alpha_{\text{vap}}) \frac{\frac{G_{\text{tot},j}^2}{\rho_{\text{mix},j}} - \frac{G_{\text{tot},i}^2}{\rho_{\text{mix},i}}}{\frac{G_{\text{tot},j}^2}{\rho_{\text{mix},j}} + \frac{G_{\text{tot},i}^2}{\rho_{\text{mix},i}}}}_{\text{fluid dynamic effect}} - \underbrace{K_{s,ij}(\alpha_{\text{vap}}) \frac{\frac{\sigma_j}{d_{\text{bub},j}} - \frac{\sigma_i}{d_{\text{bub},i}}}{\frac{\sigma_i}{d_{\text{bub},i}} + \frac{\sigma_j}{d_{\text{bub},j}}}}_{\text{geometrical effect}} \right) (\alpha_{\text{vap},i} \rho_{\text{vap},i} + \alpha_{\text{vap},j} \rho_{\text{vap},j}) \\
 &+ \underbrace{\Omega_{ij}(\alpha_{\text{vap}}) (\alpha_{\text{vap},j} \rho_{\text{vap},j} - \alpha_{\text{vap},i} \rho_{\text{vap},i})}_{\text{narrow gap effect}}
 \end{aligned} \tag{3.22}$$

with the following sensitivity parameters, which have to be calibrated by means of fundamental experiments:

$K_{p,ij}(\alpha_{\text{vap}})$ - for the fluid dynamic effect

$K_{s,ij}(\alpha_{\text{vap}})$ - for the geometrical effect

$\Omega_{ij}(\alpha_{\text{vap}})$ - for the narrow gap effect as the nonlinear function:

$$\Omega_{ij}(\alpha_{\text{vap}}) = 0.5 \Omega_0 \left[1 - \tanh \left(\frac{\pi (\alpha_{\text{vap}} - \alpha_{\text{threshold}})}{\Delta \alpha} \right) \right]$$

with the calibration factors Ω_0 , $\alpha_{\text{threshold}}$, and $\Delta \alpha$.

In the proposed model the equilibrium void distribution will be determined by a balance of major fluid forces (Bernoulli force, lateral forces induced by velocity gradients, surface tension force) acting on the vapor-liquid interface.

In conventional sub-channel codes, the local equilibrium void distribution equation is evaluated independently for one gap after the other. The idea of the present model is to involve the information about flow conditions in the whole axial bundle cross section. Therefore, all gaps are combined in a flow network system as springs. Equation (3.22) for

¹⁷ Quantities with the index ij are to be taken at the gap between sub-channels i and j .

each gap yield a linear system of equations, in which the sub-channel void fractions are regarded as spring displacements and the fluid forces on the right hand side are regarded as external forces. The coefficients of the spring stiffness matrix are calculated from the above mentioned parameters $K_{p,j}(\alpha_{\text{vap}})$ and $K_{s,j}(\alpha_{\text{vap}})$.

3.1.1.3. Short Summary of Different Equilibrium Void Distribution Models

In case of constant densities concerning the regarded sub-channels ($\rho_{\text{vap},i} = \rho_{\text{vap},j}$) and ($\rho_{\text{liq},i} = \rho_{\text{liq},j}$) – for the sake of an overview limited to the essentials – one can distinguish the different approaches as follows:

- *Levy (1963)*¹⁸:

$$\frac{\alpha_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}}}{\bar{\alpha}_{\text{vap}}} = K_{\text{M}} \frac{G_{\text{tot},j} - G_{\text{tot},i}}{\bar{G}_{\text{tot}}} \quad (3.23)$$

This model does not directly account for the influences of geometrical parameters.

- *Rowe et al. (1990)*:

$$\frac{\alpha_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}}}{\bar{\alpha}_{\text{vap}}} = -K \left(1 - \bar{\alpha}_{\text{vap}}\right) \left(\frac{\bar{d}_{\text{hyd}}}{d_{\text{hyd},j}} - \frac{\bar{d}_{\text{hyd}}}{d_{\text{hyd},i}} \right) \quad (3.24)$$

This model does not account for the influence of mass flow rates. K is a constant and strictly speaking the quantities $\bar{\alpha}_{\text{vap}}$ and $\bar{d}_{\text{hyd}}$ in this particular model are average values over the whole bundle, not only over the two adjacent sub-channels i and j .

- *Drew and Lahey (1979)*:

$$\begin{aligned} \alpha_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} = & M_j - M_i + \frac{N_i + N_j}{2} (G_{\text{vap},j} - G_{\text{vap},i}) + \frac{P_i + P_j}{2} (G_{\text{liq},j} - G_{\text{liq},i}) \\ & + (N_j - N_i) \left[\frac{G_{\text{vap},j} + G_{\text{vap},i}}{2} - \bar{G}_{\text{vap}} \right] + (P_j - P_i) \left[\frac{G_{\text{liq},j} + G_{\text{liq},i}}{2} - \bar{G}_{\text{liq}} \right] \end{aligned} \quad (3.25)$$

Equation (3.25) is a physically based result derived from turbulence relations. Under the simplifying assumptions $N_i = N_j = P_i = P_j = H$ and $M_i = M_j$ this equation is reduced to

$$\alpha_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} = H (G_{\text{tot},j} - G_{\text{tot},i}), \quad (3.26)$$

¹⁸ The parameter K_{M} seems to be introduced later. In the original use of Levy's model by *Lahey and Moody (1993)* it did not yet appear, but nowadays it is included in all citations.

which is similar to Levy's model in Equation (3.23).

- *Shirai and Ninokata (1999)*:

The prediction of hydraulic equilibrium flow distribution in sub-channel systems has been reduced to a minimization problem of the energy dissipation with the constraint of mass continuity. The pressure drop due to wall friction must be the same in all sub-channels when the energy dissipation is at its minimum value. Concerning the two-phase flow distribution the drag force between liquid and vapor is also taken into account. Finally, a system of $(4N + 1)$ nonlinear equations is solved by Newton-Raphson iterations in order to predict the two-phase equilibrium flow distribution in a system of N sub-channels. In the final code implementation a void drift driving force is derived and added to the traditional k -th phase cross-flow momentum equation.

- *Hotta et al. (2004)*:

The fluid dynamic effect, the geometrical effect, and the narrow gap effect are combined in one equation for the equilibrium void distribution (Model A in *Hotta et al. (2004)*)¹⁷:

$$\alpha_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} = (1 - \Omega_{ij}(\alpha_{\text{vap}})) \left(\underbrace{K_{p,ij}(\alpha_{\text{vap}}) \frac{\frac{G_{\text{tot},j}^2}{\rho_{\text{mix},j}} - \frac{G_{\text{tot},i}^2}{\rho_{\text{mix},i}}}{\frac{G_{\text{tot},i}^2}{\rho_{\text{mix},i}} + \frac{G_{\text{tot},j}^2}{\rho_{\text{mix},j}}}}_{\text{fluid dynamic effect}} - \underbrace{K_{s,ij}(\alpha_{\text{vap}}) \frac{\frac{\sigma_j}{d_{\text{bub},j}} - \frac{\sigma_i}{d_{\text{bub},i}}}{\frac{\sigma_i}{d_{\text{bub},i}} + \frac{\sigma_j}{d_{\text{bub},j}}}}_{\text{geometrical effect}} \right) (\alpha_{\text{vap},i} + \alpha_{\text{vap},j}) + \underbrace{\Omega_{ij}(\alpha_{\text{vap}}) (\alpha_{\text{vap},j} - \alpha_{\text{vap},i})}_{\text{narrow gap effect}} \quad (3.27)$$

with the following sensitivity parameters $K_{p,ij}(\alpha_{\text{vap}})$, $K_{s,ij}(\alpha_{\text{vap}})$, and $\Omega_{ij}(\alpha_{\text{vap}})$, which has to be calibrated by means of experiments. The equilibrium void distribution will be determined by balancing major fluid forces (Bernoulli force, lateral forces induced by velocity gradients, surface tension force) acting on the vapor-liquid interface. All gaps are combined in a flow network system (spring system) in order to involve the information about flow conditions in the whole axial bundle cross section at the same time.

3.1.2. Two-Phase Multipliers for Turbulent Mixing and Void Drift

There are suggestions in literature to use different turbulent diffusion coefficients β for turbulent mixing and void drift. Equation (3.4) would then become

$$\dot{m}_{\text{vap}} = \underbrace{-\beta_{\text{TP,TM}} \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{vap},j} \rho_{\text{vap},j} - \alpha_{\text{vap},i} \rho_{\text{vap},i}) A}_{\text{turbulent diffusion (mixing)}} + \underbrace{\beta_{\text{TP,VD}} \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}} (\alpha_{\text{vap},j,\text{EQ}} \rho_{\text{vap},j,\text{EQ}} - \alpha_{\text{vap},i,\text{EQ}} \rho_{\text{vap},i,\text{EQ}}) A}_{\text{void drift}}$$

with

$$\beta_{\text{TP,TM}} = \Theta_{\text{TP,TM}} \beta_{\text{SP}} \quad \text{and} \quad \beta_{\text{TP,VD}} = \Theta_{\text{TP,VD}} \beta_{\text{SP}} .$$

The differences in β_{TP} are reached by different two-phase multipliers. Examples are:

- *Ninokata et al. (2001)*:

Turbulent mixing and void drift are considered to be caused by different mechanisms. Therefore, the multiplier Θ_{VD} for void drift can take different dependency on the flow quality (resp. flow regime) than the multiplier Θ_{TM} for turbulent mixing and its maximum does not have to occur at x_{max} . Also the magnitude of Θ_{VD} is found to be much larger than that of Θ_{TM} to yield reasonable exit quality distributions in a bundle. Special parameter adjustments have been done in *Ninokata et al. (2001)* to recalculate the GE 3x3 mixing tests (see *Janssen (1971)*), but they are not documented in the paper.

- Presentation by Japanese authors about the sub-channel code NASCA:

$$\Theta_{\text{TM,max}} < 5 ,$$

$$\Theta_{\text{VD,max}} = 15 \quad (\text{occurring in churn-turbulent flow regime}).$$

- *Sato (1992)*:

According to Equations (2.7) and (2.12) there is the following relation:

$$\frac{\tilde{F}_t}{\Delta y} = \frac{\tilde{D}_t}{\Delta y} = \beta \frac{\bar{G}_{\text{tot}}}{\bar{\rho}_{\text{mix}}}$$

Sato (1992) uses different diffusion coefficients – the turbulent void diffusion coefficient $\tilde{D}_{\text{t,TM}}$ and the void drift coefficient $\tilde{D}_{\text{t,VD}}$:

$$\beta_{\text{TP,TM}} = \frac{\tilde{D}_{\text{t,TM}}}{\Delta y} \frac{\bar{\rho}_{\text{mix}}}{\bar{G}_{\text{tot}}} , \quad \beta_{\text{TP,VD}} = \frac{\tilde{D}_{\text{t,VD}}}{\Delta y} \frac{\bar{\rho}_{\text{mix}}}{\bar{G}_{\text{tot}}} .$$

3.2.3. CFD Calculations

By means of CFD calculations using an appropriate spatial resolution the void drift phenomenon does not have to be modeled especially. In case the interfacial forces are modeled properly, lateral exchange of liquid and vapor due to pressure differences (diversion cross flow), turbulent mixing, and void drift follows automatically.

As one example, *Azuma et al. (2004)* carried out CFD calculations in order to simulate the lateral exchange of an air-water flow through a slit between two parallel channels. They used a very high spatial resolution allowing them to apply the volume of fluid (VOF) interface tracking technique. Thus, they could resolve the shape and motion of single bubbles. By evaluating the velocity distributions of the liquid and the gas phase they visualized in detail whether air and water flow in the same direction (due to a lateral pressure gradient) or vice versa. By combining coherent structures and noise contributions, they identified causalities among major variables (as cross flow mass flux, inter-channel pressure, and channel average void fractions). Logical criteria were proposed to judge whether the flow is dominated by the void drift mechanism or by other mechanisms such as diversion cross flow.

Ninokata (2004a) generally stated that constitutive equations in sub-channel codes are unique per fuel rod bundle design. Therefore local and instantaneous information is needed either by experiment or by CFD in order to adjust built-in constitutive equations.

3.2.4. Remarks to the Lift Force on a Bubble

For *spherical* bubbles and negligible viscous forces ($Re \gg 1$) the lift coefficient C_L is equal to 0.5 and thus positive (see, e.g., *Tomiya (2004)*). Depending on the relative velocity between gas and liquid phase in a wall boundary layer it yields:

- If the bubble moves *faster* than the liquid, the lift force on the bubble acts towards the *wall*. That a bubble moves faster than the surrounding liquid is the normal case in vertical boundary layers due to the buoyancy force. This is the reason for the typical radial void fraction profiles in adiabatic pipe flow with a peak near the wall due to bubble migration (see, e.g., Figure 6 in *Drew and Lahey (1981)*, which is based on the data of *Serizawa et al. (1976)*).
- If the bubble moves *slower* than the liquid, the lift force on the bubble acts away from the wall towards the *center* of the pipe or channel. A bubble moves slower than the surrounding liquid, e.g., short after its creation by vaporization at a heated wall. When the bubble has detached from the wall it must be accelerated first.

In general, the lift coefficient is not constant (as it is the case for spherical bubbles with $C_L = 0.5$) and can even change its sign in case of larger and thus *deformed* bubbles. According to *Tomiya (2004)* the lift coefficient depends on the bubble *Reynolds* number

$$Re = \frac{V_{\text{term}} d_{\text{bub}}}{\nu_{\text{liq}}},$$

whereas V_{term} is the terminal velocity of a single isolated bubble rising through stagnant liquid, and furthermore on a modified *Eötvös* number, which is defined by using the maximum horizontal dimension of a bubble, $d_{\text{bub,hor}}$, as a characteristic length:

$$Eo = \frac{g (\rho_{\text{liq}} - \rho_{\text{vap}}) d_{\text{bub,hor}}^2}{\sigma} . \quad (3.31)$$

Tomiyama (2004) gave an empirical correlation for C_L :

$$C_L = \begin{cases} \min[0.288 \tanh(0.121 Re), f(Eo)] & \text{for } Eo < 4 \\ f(Eo) & \text{for } Eo \geq 4 \end{cases} \quad (3.32)$$

whereas

$$f(Eo) = 0.00105 Eo^3 - 0.0159 Eo^2 - 0.0204 Eo + 0.474 .$$

That means, for small bubbles ($Eo < 4$) yields $C_L = f(Re, Eo)$, whereas for large bubbles ($Eo \geq 4$) yields $C_L = f(Eo)$. The last mentioned dependence is depicted in Figure 10. According to Equation (3.32) C_L changes its sign at $Eo = 6$. Besides this, there are further measurements in *Tomiyama (2004)* leading to curves showing a change in sign of C_L at $Eo \approx 4$. Corresponding bubble diameters for both cases are calculated in Table 2.

Table 2. Bubble diameter (maximum horizontal dimension of a bubble) according to Equation (3.31) at which the lift coefficient C_L changes its sign for selected two-phase mixtures (Assumption 1: C_L becomes negative at $Eo = 4$, Assumption 2: C_L becomes negative at $Eo = 6$)

Two-phase mixture	P [bar]	T [°C]	ρ_{liq} [kg/m³]	ρ_{vap} [kg/m³]	$\Delta\rho$ [kg/m³]	σ [N/m]	$d_{\text{bub,hor}} (Eo = 4)$ [mm]	$d_{\text{bub,hor}} (Eo = 6)$ [mm]
water-air	1	20	998.3	1.2	997.1	0.073	5.5	6.7
water-vapor ¹⁹	70	285.3	739.7	36.5	703.2	0.0176	3.2	3.9
water-vapor ¹⁹	160	347.4	585.0	107.4	477.6	0.0042	1.9	2.3

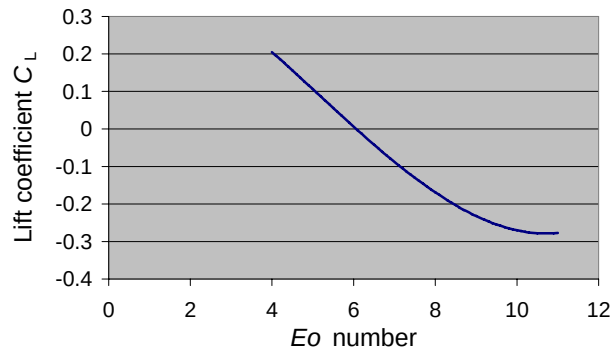


Figure 10. Lift coefficient C_L versus Eötvös number according to Equation (3.32), see *Tomiyama (2004)*.

¹⁹ The temperature is the saturation temperature at the selected pressure.

3.3. Overview over Net Transfer due to Void Drift

An overview over the effect of void drift for different flow conditions (referring to the void drift model parts described in Paragraphs 3.1 and 3.1.1.1 is given in Table 3. Levy's model is based on the following simple assumption: If the total mass fluxes of two neighboring sub-channels differ, the two-phase flow is supposed to be *not* in hydraulic equilibrium leading to a net mass transfer of vapor (void drift phenomenon) and a liquid reverse flow corresponding in such a way that **volume constancy** is satisfied. If there is mass transfer this entails also momentum and energy transfer. The driving potential for void drift in contrast to turbulent mixing is illustrated by means of formulas in the last paragraph of Paragraph 3.1.1.1.

Table 3. Overview over the driving potentials leading to net transfer of mass, momentum, and energy due to void drift (referring to the void drift model by *Lahey and Moody (1993)* and to the equilibrium void distribution model by *Levy (1963)*)

	Net lateral <i>mass</i> transfer of phase/field <i>k</i>	Net lateral <i>momentum</i> transfer of phase/field <i>k</i>	Net lateral <i>enthalpy</i> transfer of phase/field <i>k</i>
Lateral transport equation Quantity to be balanced in transport equation	$\dot{m}_k = \Gamma_t \frac{d(\alpha_k \rho_k)_{\text{EQ}}}{dy} A$ $(\alpha_k \rho_k)_{\text{EQ}}$	$\dot{i}_k = \Gamma_t \frac{d(\alpha_k \rho_k U_k)_{\text{EQ}}}{dy} A$ $G_{k,\text{EQ}} = (\alpha_k \rho_k U_k)_{\text{EQ}}$	$\dot{Q}_k = \Gamma_t \frac{d(\alpha_k \rho_k h_k)_{\text{EQ}}}{dy} A$ $(\alpha_k \rho_k h_k)_{\text{EQ}}$
Single-phase flow (isothermal) $\alpha_{\text{liq}} = 1$ $\rho = \text{const}$ $h = \text{const}$	—	—	—
Single-phase flow (non-isothermal) $\alpha_{\text{liq}} = 1$	—	—	—
Two-phase flow (isothermal) $\rho_k = \text{const}$ $h_k = \text{const}$	due to lateral gradient in: ➤ total mass flux G_{tot}	due to lateral gradient in: ➤ total mass flux G_{tot}	due to lateral gradient in: ➤ total mass flux G_{tot}
Two-phase flow (non-isothermal)	due to lateral gradient in: ➤ total mass flux G_{tot}	due to lateral gradient in: ➤ total mass flux G_{tot}	due to lateral gradient in: ➤ total mass flux G_{tot}

Conclusion

The present chapter gave an overview over the derivation of turbulent mixing and void drift models. Thereby, it was a special focus to show similarities and differences between classical sub-channel modeling applied in nuclear reactor core thermal-hydraulics on the one hand and both general turbulence theory and Computational Fluid Dynamics (CFD) on the

other hand. Furthermore, a state-of-the-art literature survey has been carried out with respect to different modeling approaches for each sub-model.

Single-phase turbulent mixing is in general well understood. Here, the main focus of today's research lies on the estimation of turbulent mixing behavior by means of modern CFD tools. Concerning *two-phase turbulent mixing*, CFD methodologies are still to be further developed, though a lot of progress has been achieved in this subject within the last years.

In contrast to turbulent mixing, there is not yet a common physical understanding of the *void drift* phenomenon. Several researchers have proposed their models being based on different physical approaches. Finally, all of them have still to be calibrated at experiments. There is the hope that once in the future also void drift will be understood better. More advanced measuring technologies or more advanced two-phase CFD methods could probably contribute to this.

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Chapter 3

QUANTUM THEORY WAY TO THE TWO-LASER IGNITION FACILITY

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Abstract

In introduction, we present the motivation for construction of the fusion reactors including the laser fusion facilities. Then, we consider the solution of the Dirac equation with the periodic potential which is called the Volkov solution. The one-photon and two-photon Compton process follows from this solution. The energy-momentum equation for the multi-photon process is also involved. In the following section, we solve the problem of the interaction of an electron with the Dirac delta-form impulsive force, which is an idealization of experimental situation in laser physics. We elaborate the quantum theory of the interaction of a charged particle with a such impulsive force. We determine the modified Compton formula for final frequency of photons generated by the scattering of the delta-form laser pulse on the electron in a rest. The one-photon Compton process is only special case of the multi-photon interaction of electron with N photons of laser pulse. The multi-photon interaction is nonlinear and differs from the situation where electron scatters twice or more as it traverses the laser focus.

The next problem of the laser physics is the electron interaction with the two electromagnetic waves. This is also the future direction of the laser physics of elementary particles. The two laser beams can be used in the thermonuclear reactor instead of many laser beams.

In the following text we consider the solution with massive photons in the laser beam. The mass of photon is of the dynamical origin corresponding to the radiative corrections. We determine the equation corresponding to the Dirac equation with periodic potential of massive photons. The resulting equation is the Riccati equation which cannot be solved in general. So, we derive only some approximative formulas. While massless photon is described by the Maxwell Lagrangian, the massive photon is described by the Proca Lagrangian from which the field equations follow. The massive electrodynamics can be considered as a generalization of massless electrodynamics.

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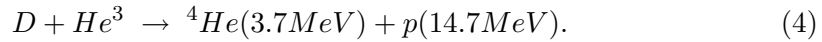
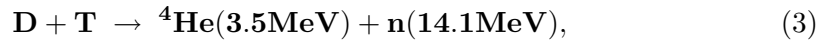
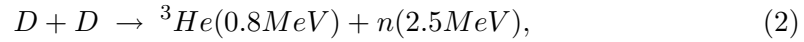
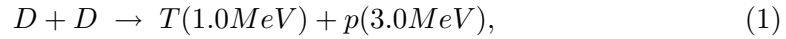
Massive photons are substantial in the theory of superconductivity, plasma physics, waveguides and so on. The massive photons can be produced in very strong magnetic pulses generated by the Kapitza gigantic solenoid method, or, by the Terletzkii method of the cumulation of the magnetic flux. It means that the deuterium-tritium pellet in the laser ignition facility can be compressed by the massive photons generated simultaneously with the magnetic delta-form pulses.

The presence of the magnetic field in which the pellet can be situated leads to the generation of the synchrotron radiation of charged particles. Motivated by this fact, we derive the synchrotron radiation formulas using the Volkov solution of the Dirac equation and the S-matrix formalism of QED for an electron moving in the constant magnetic field.

1. Introduction

The growing energy needs of mankind and limited fossil fuel resources is the motivation for the consideration of various alternative energy sources to satisfy the demand. There are criteria how to judge for the source of energy: availability and reliability of fuel supply, safety, environmental impact (e.g. greenhouse effect) geopolitical situation, and so on. The fusion energy will evidently play the long term energy source. So, TOKAMAK, JET, ITER, LIF (laser ignition facility) are the most advanced routes to fusion today.

Fusion as a power source has several attractive features. The end products are not radioactive, thereby considerably reducing the waste problem. The fuel supply is unlimited. Nevertheless, the final scientific demonstration of fusion as a potential energy source remains to be done. The basic fusion reactions are realized with the Deuterium and Tritium (D, T):



The D-T reaction, expressed in the bold-face equation (3), is of the highest cross-section and the maximal energy of neutrons. Even then, one must overcome the Coulomb repulsion between the positive charges. The particle energies required for the D-T reaction are around 100 keV or more. At such temperatures, atoms are fully stripped, and therefore fusion happens in plasmas. However, for a sufficient reaction rate, the plasma density must be sufficiently high.

The hot, dense, plasma must be confined and prevented from melting material containment walls. This can be accomplished with inertial confinement techniques, or with magnetic fields of appropriate strength and topology. The orbit of confinement of a charged particle in a uniform magnetic field is a combination of cyclotron rotation around the field line and constant parallel velocity along it.

The magnetic confinement systems with closed field line systems is precisely elaborated in the tokamak. The tokamak concept was developed in Russia in the 1950s and 60s [1]. The dominant magnetic field is toroidal, and (mostly) externally induced by poloidal currents flowing on copper or super-conducting coils constituting the ring-shaped solenoid. The resulting helical magnetic field can be shaped in closed, nested surfaces. The particle velocities along the magnetic field lines are so high that the pressure and charge are equalised on each nested surface.

If the confinement is sufficiently good, the fusion energy production overcomes the losses and the reaction becomes at least partially self-sustaining (heating of the plasma by the 3.5 MeV energetic particles resulting from fusion reactions). The achievement of these conditions is measured by the triple product, $n \cdot \tau_E \cdot T_i$, where n is the plasma density, τ_E is the energy confinement time and T_i is the ion temperature.

JET (Joint European Torus) is the largest tokamak in the world. The major radius is 3.1 m, the magnetic confinement field can be as high as 4 T, and the plasma current can be as high as 6 MA. Typically the energy confinement time is approximately 1 s, while pulse flat-top duration is typically 10 s. JET has a unique capability to operate with a D-T fuel mixture, essential for the study of alpha particle physics. Technologies needed in ITER are used in JET.

The next major step in fusion research is associated with the construction of a new device. The European Union, Japan, and the Russian Federation are working together on the design of the International Thermonuclear Experimental Reactor, ITER, which is demonstration of the scientific and technological feasibility of fusion energy for peaceful purposes.

The major radius of ITER is 6.2 m, the toroidal field 5.3 T, the plasma current 15 MA, pulse flat-top duration is typically 300 s at maximum capacity, can be 1000 s in Advanced Tokamak scenarios. The original approach is developed in USA as the NIF system, which is translated as the national ignition facility and it involves application of lasers to start the nuclear fusion.

2. Necessity of Quantum Theory for Understanding Fusion Processes

While in the past, Michael Faraday, Thomas Alva Edison, Nicola Tesla and others, invented their facilities without precise knowledge of the theoretical physics, the situation in case of such facilities as JET, ITER, NIF needs not only the experimental abilities, but at the same time the brilliant knowledge of the nuclear reactions, plasma physics at very high temperatures, magnetohydrodynamics, computer simulation of physical processes, knowledge of the basic quantum mechanical equations and other theoretical peaces of knowledge. The practical realization of fusion is based on the knowledge of the Lawson criterion which is the result of the theoretical investigation. The naive approach without knowledge of theory is misleading. If we want to realize the laser ignition facility, it is necessary to study the interaction of the laser beams with elementary particles. Then, we can predict the parameters which are necessary for the practical realization of fusion and its industrial application. We develop our approach to the ignition facilities from the viewpoint of the theoretical problems which can play fundamental role in the development of the ignition facilities.

We consider quantum theory based on the Volkov solution of the Dirac equation in order to get the acceleration of electron by delta-form laser pulse. We determine the Compton formula in case of scattering of the delta-form laser pulse on the electron in a rest. In case of the massive photons in a pulse we get first the Riccati equation which we solve only approximately. Then we find the solutions of the Dirac equation for two plane waves (laser beams) and we determine the modified Compton formula for the scattering of two photons on an electron.

The situation with the two electromagnetic wave is the next step and the future direction of the laser physics of elementary particles. The two laser beams can be used in the thermonuclear reactor instead of many laser beams. Two laser beams impinging on a target which is constituted from material in the form of foam, can replace 100-200 laser beams impinging on a normal target and it means that the nuclear fusion with two laser beams is realistic inside of the thermonuclear reactor such as ITER in Cadarache near Aix-en-Provence in France. ITER (International Experimental Thermonuclear Reactor) means “the way” in Latin and it will be prepared to produce 500 MW of fusion power in 2016 .

The system of two lasers can be considered also in chemistry as a specific catalyzer which was not known before the existence of the laser physics. So, laser methods in particle physics and chemistry can form in the near future the new scientific revolution, which is not described in the standard monographs on the scientific revolutions.

The Volkov solution involves also the description of the so called synchrotron radiation. We derive the synchrotron radiation from the Volkov solution of the Dirac equation and from the S-matrix theory.

3. The Volkov Solution of the Dirac Equation

The Volkov solution of the Dirac equation in vacuum. We use here the method of derivation and metric convention of Berestetskii et al. [2]:

$$(\gamma(p - eA) - m)\psi = 0, \quad (5)$$

where

$$A^\mu = A^\mu(\varphi); \quad \varphi = kx. \quad (6)$$

with the Lorentz gauge condition

$$\partial_\mu A^\mu = k_\mu (A^\mu)' = (k_\mu A^\mu)' = 0, \quad (7)$$

where the prime denotes derivation with regard to φ . From the last equation follows

$$kA = \text{const} = 0, \quad (8)$$

because we can put the constant to zero and with

$$(\gamma k)(kA)(\gamma k)(\gamma A) = -k^2 A^2 = 0, \quad (9)$$

we get the solution as follows [2]:

$$\psi_p = R \frac{u}{\sqrt{2p_0}} e^{iS} = \left[1 + \frac{e}{2kp} (\gamma k)(\gamma A) \right] \frac{u}{\sqrt{2p_0}} e^{iS}, \quad (10)$$

where u is an electron bispinor of the corresponding Dirac equation

$$(\gamma p - m)u = 0 \quad (11)$$

with the normalization condition $\bar{u}u = 2m$

The mathematical object S is the classical Hamilton-Jacobi function, which was determined in the form:

$$S = -px - \int_0^{kx} \frac{e}{(kp)} \left[(pA) - \frac{e}{2}(A)^2 \right] d\varphi. \quad (12)$$

The current density is

$$j^\mu = \bar{\psi}_p \gamma^\mu \psi_p, \quad (13)$$

where $\bar{\psi}_p$ is defined as the transposition of (10), or,

$$\bar{\psi}_p = \frac{\bar{u}}{\sqrt{2p_0}} \left[1 + \frac{e}{2kp} (\gamma A)(\gamma k) \right] e^{-iS}. \quad (14)$$

After insertion of ψ_p and $\bar{\psi}_p$ into the current density, we have:

$$j^\mu = \frac{1}{p_0} \left\{ p^\mu - eA^\mu + k^\mu \left(\frac{e(pA)}{(kp)} - \frac{e^2 A^2}{2(kp)} \right) \right\}. \quad (15)$$

which is in agreement with formula in the Meyer article [3].

4. The Compton Process from the Volkov Solution of the Dirac Equation

Let us consider electromagnetic monochromatic plane wave which is polarized in a circle. So, we write the four-potential in the form:

$$A = a_1 \cos \varphi + a_2 \sin \varphi, \quad (16)$$

where the amplitudes a_i are equal in magnitude and orthogonal, or,

$$a_1^2 = a_2^2 = a^2, \quad a_1 a_2 = 0. \quad (17)$$

Then, it is possible to show that the Volkov solution for this situation is of the form [2]:

$$\begin{aligned} \psi_p = & \left\{ 1 + \left(\frac{e}{2(kp)} \right) [(\gamma k)(\gamma a_1) \cos \varphi + (\gamma k)(\gamma a_2) \sin \varphi] \right\} \frac{u(p)}{\sqrt{2q_0}} \times \\ & \exp \left\{ -ie \frac{(a_1 p)}{(kp)} \sin \varphi + ie \frac{(a_2 p)}{(kp)} \cos \varphi - iqx \right\}, \end{aligned} \quad (18)$$

where

$$q^\mu = p^\mu - e^2 \frac{a^2}{2(kp)} k^\mu \quad (19)$$

q^μ is the mean value of quantity $p_0 j^\mu$.

We know that the matrix element M corresponding to the emission of photon by an electron in the electromagnetic field is as follows [2]:

$$S_{fi} = -ie^2 \int d^4x \bar{\psi}_{p'}(\gamma e'^*) \psi_p \frac{e^{ik'x}}{\sqrt{2\omega'}}, \quad (20)$$

where ψ_p is the wave function of an electron before interaction with the laser photons and $\psi_{p'}$ is the wave function of electron after emission of photon with components $k'^\mu = (\omega', \mathbf{k}')$. The quantity e'^* is the polarization four-vector of emitted photon.

The matrix element (20) involves the following linear combinations:

$$e^{-i\alpha_1 \sin \varphi + i\alpha_2 \cos \varphi}, \quad e^{-i\alpha_1 \sin \varphi + i\alpha_2 \cos \varphi} \cos \varphi; \quad e^{-i\alpha_1 \sin \varphi + i\alpha_2 \cos \varphi} \sin \varphi, \quad (21)$$

where

$$\alpha_1 = e \left(\frac{a_1 p}{kp} - \frac{a_1 p'}{kp'} \right); \quad \alpha_2 = e \left(\frac{a_2 p}{kp} - \frac{a_2 p'}{kp'} \right), \quad (22)$$

Now, we can expand exponential function in the Fourier series, where the coefficients of the expansion will be B_s, B_{1s}, B_{2s} . So we write:

$$e^{-i\alpha_1 \sin \varphi + i\alpha_2 \cos \varphi} = e^{-i\sqrt{\alpha_1^2 + \alpha_2^2} \sin(\varphi - \varphi_0)} = \sum_{s=-\infty}^{\infty} B_s e^{-is\varphi} \quad (23)$$

$$e^{-i\alpha_1 \sin \varphi + i\alpha_2 \cos \varphi} \cos \varphi = e^{-i\sqrt{\alpha_1^2 + \alpha_2^2} \sin(\varphi - \varphi_0)} \cos \varphi = \sum_{s=-\infty}^{\infty} B_{1s} e^{-is\varphi} \quad (24)$$

$$e^{-i\alpha_1 \sin \varphi + i\alpha_2 \cos \varphi} \sin \varphi = e^{-i\sqrt{\alpha_1^2 + \alpha_2^2} \sin(\varphi - \varphi_0)} \sin \varphi = \sum_{s=-\infty}^{\infty} B_{2s} e^{-is\varphi}. \quad (25)$$

The Coefficients B_s, B_{1s}, B_{2s} can be expressed by means of the Bessel function as follows [2]:

$$B_s = J_s(z) e^{is\varphi_0} \quad (26)$$

$$B_{1s} = \frac{1}{2} \left[J_{s+1}(z) e^{i(s+1)\varphi_0} + J_{s-1}(z) e^{i(s-1)\varphi_0} \right] \quad (27)$$

$$B_{2s} = \frac{1}{2i} \left[J_{s+1}(z) e^{i(s+1)\varphi_0} - J_{s-1}(z) e^{i(s-1)\varphi_0} \right], \quad (28)$$

where the quantity z are now defined through the α -components, or,

$$z = \sqrt{\alpha_1^2 + \alpha_2^2} \quad (29)$$

and

$$\cos \varphi_0 = \frac{\alpha_1}{z}; \quad \sin \varphi_0 = \frac{\alpha_2}{z}. \quad (30)$$

Functions B_s, B_{1s}, B_{2s} are related one to another as follows:

$$\alpha_1 B_{1s} + \alpha_2 B_{2s} = s B_s, \quad (31)$$

which follows from the well known relation for Bessel functions:

$$J_{s-1}(z) + J_{s+1}(z) = \frac{2s}{z} J_s(z). \quad (32)$$

The matrix element (20) can be written in the form [2]:

$$S_{fi} = \frac{1}{\sqrt{2\omega'2q_02q'_0}} \sum_s M_{fi}^{(s)} (2\pi)^4 i \delta^{(4)}(sk + q - q' - k'), \quad (33)$$

where the δ -function involves the law of conservation:

$$sk + q = q' + k'; \quad s = 1, 2, 3, \dots \quad (34)$$

with the relation

$$q^2 = q'^2 = m^2(1 + \xi^2) \equiv m_*^2 = m^2 \left(1 - \frac{e^2 a^2}{m^2} \right), \quad (35)$$

as it follows from eq. (19).

For $s = 1$, eq. (34) has the physical meaning of the conservation of energy- momentum of the one-photon Compton process, $s = 2$ has meaning of the two-photon Compton process and $s = n$ has meaning of the multiphoton interaction with n photons.

It is possible to show, that the differential probability per unit volume and unit time of the emission of the s harmonics is of the following form [2]:

$$dW_s = |M_{fi}^{(s)}|^2 \frac{d^3 k' d^3 q'}{(2\pi)^6 2\omega' 2q_0 2q'_0} (2\pi)^4 \delta^{(4)}(sk + q - q' - k'). \quad (36)$$

In order to obtain the probability of emission of photon, we must make some operation with the matrix element M . We will here not perform these operations. We concentrate our attention on the conservation law in the formula (36). It means that the multiphoton object with the momentum sk interacts with the electron of the momentum q , and the result is the electron with the momentum q' and one photon with the momentum k' .

Now, let us consider the equation (34) in the modified form

$$sk + q - k' = q'. \quad (37)$$

If we introduce the angle Θ between $\mathbf{k}$ and $\mathbf{k}'$, then, with $|\mathbf{k}| = \omega$ and $|\mathbf{k}'| = \omega'$, we get from the squared equation (37) in the rest system of electron, where $q = (m_*, 0)$, the following equation:

$$s \frac{1}{\omega'} - \frac{1}{\omega} = \frac{s}{m_*} (1 - \cos \Theta), \quad (38)$$

which is modification of the original equation for the Compton process

$$\frac{1}{\omega'} - \frac{1}{\omega} = \frac{1}{m} (1 - \cos \Theta). \quad (39)$$

So, we see that Compton effect described by the Volkov solution of the Dirac equation differs from the original Compton formula only by the existence of the renormalized mass and parameter s of the multiphoton interaction.

The last formula (39) of the original Compton effect can be written in the form suitable for the experimental verification, namely:

$$\lambda' - \lambda = \Delta\lambda = 4\pi \frac{\hbar}{mc} \sin^2 \frac{\Theta}{2}, \quad (40)$$

which was used by Compton for the verification of the quantum nature of light.

5. Quantum Theory of Emission of Photons by Electron Moving in the Impulsive Force

We idealize the impulsive force by the dirac δ -function. Newton's second law for the interaction of a massive particle with mass m with an impulsive force $P\delta(t)$ is as follows

$$m \frac{d^2 x}{dt^2} = P\delta(t), \quad (41)$$

where P is some constant.

If we express δ -function by the relation $\delta(t) = \dot{\eta}(t)$, then from equation (41) follows $\dot{x}(t) = P/m$, immediately. The physical meaning of the quantity P can be deduced from equation $F = P\delta(t)$. After t -integration we have $\int F dt = mv = P$, where m is mass of a body and v its final velocity (with $v(0) = 0$).

In case of the quantum theory it is necessary to consider the Volkov solution of the Dirac equation with the potential corresponding to the delta-force.

If the potential is dependent on $\varphi = kx$, then, there is explicit solution of the Dirac equation which was found by Volkov [2]. The quantum mechanical problem is to find solution of the Dirac equation with the δ -form four-potential and from this solution determine the quantum motion of the charged particle under this potential.

We know that the matrix element M corresponding to the emission of photon by electron in the electromagnetic field is as follows [2]:

$$M = -ie^2 \int d^4 x \bar{\Psi}_{p'} (\gamma e'^*) \Psi_p \frac{e^{ik'x}}{\sqrt{2\omega'}}, \quad (42)$$

where Ψ_p is the wave function of an electron before interaction with the delta-form pulse and $\Psi_{p'}$ is the wave function of electron after emission of photon with components $k'^\mu = (\omega', \mathbf{k}')$. The quantity e'^* is the four polarization vector of emitted photon.

If we write Volkov wave function Ψ_p in the form (10), then, for the impulsive vector potential $A_\mu = a_\mu \eta(\varphi)$ we have:

$$S = -px - \left[e \frac{ap}{kp} - \frac{e^2}{2kp} a^2 \right] \varphi, \quad R = \left[1 + \frac{e}{2kp} (\gamma k)(\gamma a) \eta(\varphi) \right]. \quad (43)$$

So, we get the matrix element in the form:

$$M = g \int d^4x \bar{\Psi}_{p'} O \Psi_p \frac{e^{ik'x}}{\sqrt{2\omega'}}, \quad (44)$$

where $O = \gamma e'^*$, $g = -ie^2$ in case of the electromagnetic interaction and

$$\bar{\Psi}_{p'} = \frac{\bar{u}}{\sqrt{2p'_0}} \bar{R}(p') e^{-iS(p')}. \quad (45)$$

In such a way, using above definitions, we write the matrix element in the form:

$$M = \frac{g}{\sqrt{2\omega'}} \frac{1}{\sqrt{2p'_0 2p_0}} \int d^4x \bar{R}(p') O R(p) e^{-iS(p') + iS(p)} e^{ik'x}. \quad (46)$$

The quantity $\bar{R}(p')$ follows immediately from eq. (10), namely:

$$\bar{R}' = \overline{\left[1 + \frac{e}{2kp'} (\gamma k)(\gamma a) \eta(\varphi) \right]} = \left[1 + \frac{e}{2kp'} (\gamma a)(\gamma k) \eta(\varphi) \right]. \quad (47)$$

Using

$$-iS(p') + iS(p) = i(p' - p) + i(\alpha' - \alpha)\varphi, \quad (48)$$

where

$$\alpha = \left(e \frac{ap}{kp} - \frac{e^2}{2} \frac{a^2}{kp} \right), \quad \alpha' = \left(e \frac{ap'}{kp'} - \frac{e^2}{2} \frac{a^2}{kp'} \right), \quad (49)$$

we get:

$$M = \frac{g}{\sqrt{2\omega'}} \frac{1}{\sqrt{2p'_0 2p_0}} \int d^4x \bar{u} \bar{R}(p') O R(p) u e^{i(p'-p)x} e^{i(\alpha'-\alpha)\varphi} e^{ik'x}. \quad (50)$$

Putting

$$\bar{R}(p') O R(p) e^{i(\alpha'-\alpha)\varphi} = \frac{1}{2\pi} \int_{-\infty}^{\infty} D(s) e^{-is\varphi} ds, \quad (51)$$

we get after x -integration:

$$M = \frac{g}{\sqrt{2\omega'}} \frac{1}{\sqrt{2p'_0 2p_0}} \int ds \bar{u}(p') D(s) u(p) \delta^{(4)}(ks + p - k' - p'). \quad (52)$$

We see from the presence of the δ -function in eq. (52) that during the process of the interaction of electron with the laser pulse the energy-momentum conservation law holds good:

$$sk + p = k' + p'. \quad (53)$$

The last equation has physical meaning for $s = 1, 2, \dots, n$, n being positive integer. For $s = 1$ it has meaning of the conservation of energy momentum of the one-photon Compton process, $s = 2$ has meaning of the two-photon Compton process and $s = n$ has meaning of the multiphoton interaction with n photons of laser pulse with electron. The multiphoton interaction is nonlinear and differs from the situation where electron scatters twice or more as it traverses the laser focus. It also means that the original Einstein photoelectric equation can be replaced by the more general multiphoton photoelectric equation in the form:

$$n\hbar\omega = \frac{1}{2}mv^2 + E_i, \quad (54)$$

where E_i is the binding energy of the outermost electron in the atomic system. It means that the ionization effect occurs also in the case that $\hbar\omega < E_i$ in case that number of participating photons is $n > E_i/\hbar\omega$. We will not solve furthermore this specific problem.

The total probability of the emission of photons during the interaction of the laser pulse with electron is as follows ($l = \alpha - \alpha'$):

$$\begin{aligned} W &= \int \frac{1}{2} \sum_{spin.polar.} \frac{|M|^2 d^3p' d^3k'}{VT (2\pi)^6} = \\ &= \int \frac{d^3p' d^3k'}{(2\pi)^6} \frac{1}{2} \delta^{(4)}(lk + p - p' - k') 4 \left\{ (p'e'^*)(pe'^*) + (e'^*e'^*)(m^2 - (pp')) \right\}. \end{aligned} \quad (55)$$

The presence of the δ -function in the last formula is expression of the conservation law $lk + p = k' + p'$, which we write in the form:

$$lk + p - k' = p'. \quad (56)$$

If we introduce the angle Θ between $\mathbf{k}$ and $\mathbf{k}'$, then, with $|\mathbf{k}| = \omega$ and $|\mathbf{k}'| = \omega'$, we get from the squared equation (56) in the rest system of electron, where $p = (m_*, 0)$, the following equation:

$$l \frac{1}{\omega'} - \frac{1}{\omega} = \frac{l}{m_*} (1 - \cos \Theta); \quad l = \alpha - \alpha', \quad (57)$$

which is modification of the original equation for the Compton process

$$\frac{1}{\omega'} - \frac{1}{\omega} = \frac{1}{m} (1 - \cos \Theta). \quad (58)$$

We see that the substantial difference between single photon interaction and δ -pulse interaction is the factor $l = \alpha - \alpha'$.

We know that the last formula of the original Compton effect can be written in the form suitable for the experimental verification, namely:

$$\Delta\lambda = 4\pi \frac{\hbar}{mc} \sin^2 \frac{\Theta}{2}, \quad (59)$$

which was used by Compton for the verification of the quantum nature of light.

We can express equation (57) in new form. From equation $lk + p = k' + p'$ we get after multiplication it by k in the rest frame of electron:

$$kp' = \omega m_* - \omega \omega' (1 - \cos \Theta). \quad (60)$$

Then, l in eq. (57) is given by the formula ($a \equiv (v, \mathbf{w})$):

$$l = \frac{2evm_* - e^2 a^2}{2\omega m_*} - \frac{2eap' - e^2 a^2}{2\omega[m_* - \omega'(1 - \cos \Theta)]}. \quad (61)$$

This equation (61) can be experimentally verified by the similar methods which was used by Compton for the verification of his formula. However, it seems that the interaction of the photonic pulse substantially differs from the interaction of a single photon with electron.

The equation $lk + p = k' + p'$ is the symbolic expression of the nonlinear Compton effect in which several photons are absorbed at a single point, but only single high-energy photon is emitted. The nonlinear Compton process was experimentally confirmed for instance by Bulla et al. [4].

The δ -form laser pulses are here considered as an idealization of the experimental situation in laser physics. Nevertheless, it was demonstrated theoretically that at present time the zeptosecond and subzeptosecond laser pulses of duration $10^{-21} - 10^{-22}$ s can be realized by the petawatt lasers [5]. It means that the generation of the ultrashort laser pulses is the keen interest in development of laser physics.

6. Volkov Solution of the Dirac Equation for Massive Photons

The original Volkov solution is based on the assumption that photon has zero rest mass, or, $k^2 = 0$. Massive photons was considered early by author [6], [7], and in the classical electromagnetism, the vector potential of massive photons is the solution of the Proca equation. If photons move in electromagnetic field, then they have nonzero rest mass [8]. This mass is complex quantity, while we will consider here, for simplicity, only real quantity. Mass of photons in the electromagnetic field follows from the polarization operator in these fields and it is not generated by the Higgs mechanism, or by the Schwinger mechanism.

The formal introduction of the rest mass of photon exist in quantum electrodynamics, where for instance the processes with soft photons are calculated. In these calculations the photon mass is introduced in order to avoid the infrared divergences [2].

Introducing the nonzero photon mass modifies Coulomb law. Such modification is discussed in literature [9]. It is evident that massive photons play crucial role in gravity. However, this problem was not discussed in the prestige articles [10].

On the other hand, the possibility that photon may be massive particle has been treated by many physicists. The established fact is that the massive electrodynamics is a perfectly consistent classical and quantum field theory [11, 12, 13].

Our goal is to consider the solution of the Dirac equation in case that $k^2 = M^2$, where M is the rest mass of photon. We use here the metrical notation of Berestetskii et al. [2].

We apply the procedure of the preceding section for the case of the massive photon, and we write:

$$\psi = e^{-ipx} F(\varphi), \quad (62)$$

where for F we get the following equation

$$M^2 F'' - 2i(kp)F' + G(\varphi)F = 0 \quad (63)$$

with

$$G(\varphi) = 2e(pA) - e^2 A^2 + ie(\gamma k)(\gamma A'). \quad (64)$$

The equation (63) differs from the original Volkov equation only by means of the massive term. However, the equation is substantially new, because of the second derivative of the function F . The solution of the last equation can be easily obtained in the approximative form in case that $M \rightarrow 0$. However, let us try to find the exact solution, which was not described, to our knowledge in physical or mathematical journals.

In order to find such solution, we transcribe this equation in the form:

$$F'' + aF' + bF = 0, \quad (65)$$

where

$$a = -\frac{2i(kp)}{M^2}, \quad b(\varphi) = \frac{G(\varphi)}{M^2}. \quad (66)$$

Using the substitution

$$F = e^{\int_0^\varphi T(\varphi) d\varphi} e^{-\frac{1}{2}a\varphi}, \quad (67)$$

we get from eq. (65)

$$T' + T^2 + P(\varphi) = 0. \quad (68)$$

Equation (68) is so called Riccati equation. The mass term is hidden in $P(\varphi)$. It is well known that there is no general form of solution of this equation. There is only some solution expressed in the elementary functions for some specific functions $P(\varphi)$. Nevertheless, there is interesting literature concerning the Riccati equation. For instance, Riccati equation was applied in the supersymmetric quantum mechanics by Cooper et al. [14], in variational calculus by Zelekin [15], in nonlinear physics by Matveev et al. [16], in renormalization group theory by Buchbinder et al. [17], or, Milton et al. [18] and in thermodynamics by Rosu et al. [19].

With regard to circumstances, we are forced to find some approximative solution with form similar to the original Volkov solution. Let us show the derivation of such the approximative solution.

There are many approximative methods for solution of this problem. We choose the elementary method which was also applied to the Schrödinger equation and which is described for instance in the monograph of Mathews et al. [20].

The approximation consists at the application of the following inequalities:

$$|F''(\varphi)| \ll |F'(\varphi)|; \quad |F'''(\varphi)| \ll |F''(\varphi)|. \quad (69)$$

Then, we get the original Volkov solution with the difference that the existence of the nonzero photon mass will be involved only in the exponential expansion. Or, with $U = e(\gamma k)(\gamma A)/2(kp)$, we perform the expansion:

$$e^U = \sum_{k=0}^{\infty} \frac{U^k}{k!} = \left\{ 1 + \frac{e}{2(kp)}(\gamma k)(\gamma A) + \frac{1}{2!} \left(\frac{e}{2(kp)} \right)^2 (-M^2 A^2) + \right. \\ \left. \frac{1}{3!} \left(\frac{e}{2(kp)} \right)^3 (\gamma k)(\gamma A)(-M^2 A^2) + \frac{1}{4!} \left(\frac{e}{2(kp)} \right)^4 (M^4 A^4) + \dots \right\}, \quad (70)$$

where we have used equation

$$(\gamma k)(\gamma A)(\gamma k)(\gamma A) = -(\gamma k)(\gamma k)(\gamma A)(\gamma A) + 2(kA)(\gamma k)(\gamma A) = -k^2 A^2 = -M^2 A^2. \quad (71)$$

with $k^2 = M^2$ for massive photons. We see that in this method of approximation the Massive solution involves the Volkov solution as the basic term and then the additional terms containing photon mass.

After performing some algebraic operations, we get the first approximation of the Volkov solution with the massive photon in the following form

$$\Psi_p = \left[1 + \frac{e}{2kp}(\gamma k)(\gamma A) - \left(\frac{e}{2kp} \right)^2 M^2 A^2 + \dots \right] \frac{u}{\sqrt{2p_0}} e^{iS}. \quad (72)$$

Now, we are prepared to solve some physical problems with the Volkov solution with massive photons.

6.1. Emission of Massive Photons by Electron Moving in the Impulsive Force

We use the δ -function form of the ultrashort laser pulse [21]

$$A_\mu = a_\mu \eta(\varphi), \quad (73)$$

where $\eta(\varphi)$ is the Heaviside unit step function defined as follows: $\eta(\varphi) = 0; \varphi < 0$, and $\eta(\varphi) = 1; \varphi \geq 0$. Then, the function S and R in the Volkov solution Ψ_p are as follows [21]:

$$S = -px - \left[e \frac{ap}{kp} - \frac{e^2}{2kp} a^2 \right] \varphi, \quad R = \left[1 + \frac{e}{2kp}(\gamma k)(\gamma a)\eta(\varphi) + \dots \right]. \quad (74)$$

So, we get the matrix element in the form:

$$M = g \int d^4x \bar{\Psi}_{p'} O \Psi_p \frac{e^{ik'x}}{\sqrt{2\omega'}}, \quad (75)$$

where $O = \gamma e'^*$, $g = -ie^2$ in case of the electromagnetic interaction.

Using

$$\alpha = \left(e \frac{ap}{kp} - \frac{e^2}{2} \frac{a^2}{kp} \right), \quad \alpha' = \left(e \frac{ap'}{kp'} - \frac{e^2}{2} \frac{a^2}{kp'} \right), \quad (76)$$

we get:

$$M = \frac{g}{\sqrt{2\omega'}} \frac{1}{\sqrt{2p'_0 2p_0}} \bar{u}(p') R(p') O R(p) u(p) \delta^{(4)}(kl + p - k' - p'). \quad (77)$$

We see from the presence of the δ -function in eq. (77) that during the process of the interaction of electron with the laser pulse the energy-momentum conservation law holds good:

$$lk + p = k' + p'; \quad l = \alpha - \alpha'. \quad (78)$$

The last equation describes the so called multiphoton process, which can be also described using Feynman diagrams and which are studied in the different form intensively in the modern physics of multiphoton ionization of atoms [22].

If we introduce the angle Θ between $\mathbf{k}$ and $\mathbf{k}'$, then, with $|\mathbf{k}| = \omega$ and $|\mathbf{k}'| = \omega'$, we get from the squared equation (78) in the rest system of electron, where $p = (m_*, 0)$, the following equation $k = (\omega, \mathbf{k})$:

$$(l^2 + 1) \frac{M^2}{2m_*} \frac{1}{\omega\omega'} + l \frac{1}{\omega'} - \frac{1}{\omega} = \frac{l}{m_*} (1 - \cos \Theta); \quad l = \alpha - \alpha', \quad (79)$$

which is modification of the original equation for the Compton process

$$\frac{1}{\omega'} - \frac{1}{\omega} = \frac{1}{m} (1 - \cos \Theta). \quad (80)$$

We see that the substantial difference between single photon interaction and δ -pulse interaction is the factor $l = \alpha - \alpha'$.

We know that the last formula of the original Compton effect can be written in the form suitable for the experimental verification, namely:

$$\Delta\lambda = 4\pi \frac{\hbar}{mc} \sin^2 \frac{\Theta}{2}, \quad (81)$$

which was used by Compton for the verification of the quantum nature of light [23].

6.2. Emission of Massive Photons by Electron Moving in the Periodic Field

Let us consider the monochromatic circularly polarized electromagnetic wave with the four potential

$$A = a_1 \cos \varphi + a_2 \sin \varphi; \quad a_3 = 0; \quad \varphi = kx \quad (82)$$

with $k^\mu = (\omega, \mathbf{k})$ being a wave 4-vector and $k^2 = M^2$, the 4-amplitudes a_1 and a_2 are the same and one another perpendicular, or

$$a_1^2 = a_2^2 = a^2; \quad a_1 a_2 = 0. \quad (83)$$

We shall also use the Lorentz gauge condition, which gives $a_1 k = a_2 k = 0$.

The wave function is then of the form:

$$\begin{aligned} \psi_p = & \left\{ 1 + \frac{e}{2(kp)} \left[(\gamma k)(\gamma a_1) \cos \varphi + (\gamma k)(\gamma a_2) \sin \varphi - \frac{e}{2(kp)} a^2 M^2 + \dots \right] \right\} \frac{u(p)}{\sqrt{2q_0}} \times \\ & \exp \left\{ -ie \frac{a_1 p}{(kp)} \sin \varphi + ie \frac{a_2 p}{(kp)} \cos \varphi - iqx \right\}, \end{aligned} \quad (84)$$

where

$$q^\mu = p^\mu - e^2 \frac{a^2}{2(kp)} (k^\mu). \quad (85)$$

The corresponding matrix element is of the obligate form [2].

After performing the appropriate mathematical operation we get the δ -function in the matrix element, from which the conservation laws follow in the form

$$sk + q = q' + k'. \quad (86)$$

The interpretation of this formula is as follows: s massive photons with momentum k are absorbed by electron with momentum q and only one massive photon is emitted with the 4-vector k' , and the final momentum of electron is q' . So, we see that the Volkov solution gives the multiphoton processes, which are intensively studied in the modern physics [22].

For the periodic wave it is

$$q^2 = q'^2 = m_*^2; \quad m_* = m \sqrt{1 + \frac{e^4 a^4}{(2kp)^2} \frac{M^2}{m^2} - \frac{e^2}{m^2} a^2} \quad (87)$$

which can be interpreted as a mass shift of electron in the periodic field, or, the mass renormalization.

If we consider an electron at rest ($\mathbf{q} = 0, q_0 = m_*$), then from the formulas (85), (86) and (87) follows

$$(s^2 + 1) \frac{M^2}{2m_*} \frac{1}{\omega \omega'} + s \frac{1}{\omega'} - \frac{1}{\omega} = \frac{s}{m_*} (1 - \cos \Theta); \quad s = 1, 2, 3, \dots n. \quad (88)$$

The massless limit of the last formula is the well known Compton formula (with $M = 0$)

$$\omega' = \frac{s\omega}{1 + \frac{s\omega}{m_*}(1 - \cos \theta)}, \quad (89)$$

where θ is an angle between $\mathbf{k}$ and $\mathbf{k}'$. So we see that frequencies ω' are harmonic frequencies of ω .

7. The Probability of Emission of Photons by Electron in a Circularly Polarized Wave

Now, let us consider electromagnetic monochromatic plane wave which is polarized in circle. We write the four-potential in the form $A = a_1 \cos \varphi + a_2 \sin \varphi$, where $ak = 0$ and the amplitudes a_i are the same and mutually perpendicular, or, $a_1^2 = a_2^2 = a^2$, $a_1 a_2 = 0$.

Volkov solution for this periodic potential is of the form (90) and the matrix element can be written in the form [2]:

$$S_{fi} = \frac{1}{\sqrt{2\omega'2q_02q'_0}} \sum_s M_{fi}^{(s)} (2\pi)^4 \delta^{(4)}(sk + q - q' - k'), \quad (90)$$

where the δ -function involves the law of conservation in the form $sk + q = q' + k'$, where

$$q^\mu = p^\mu - \frac{e^2}{2(kp)} \overline{A^2} k^\mu. \quad (91)$$

Using the last equation we can introduce the so called "effective mass" of electron immersed in the periodic wave potential as follows:

$$q^2 = m_*^2; \quad m_* = m \sqrt{\left(1 - \frac{e^2}{m^2} \overline{A^2}\right)} = m \sqrt{\left(1 - \frac{e^2}{m^2} a^2\right)} \quad (92)$$

Formula (92) represents the mass renormalization of an electron mass in the field A . In other words the mass renormalization is defined by the equation $m_{\text{phys}} = m_{\text{bare}} + \delta m$, where δm follows from eq. (92). The quantity m_{phys} is the physical mass that an experimenter would measure if the particle were subject to Newton's law $\mathbf{F} = m_{\text{phys}} \mathbf{a}$. In case of the periodic field of laser, the quantity δm has the finite value. The renormalization is not introduced here "by hands" but it follows immediately from the formulation of the problem of electron in the wave field.

We can write

$$q^2 = q'^2 = m^2(1 + \xi^2) \equiv m_*^2, \quad (93)$$

where for plane wave with relations (93)

$$\xi = \frac{e}{m} \sqrt{-a^2}. \quad (94)$$

According to S-matrix formalism of QED [2], the matrix element in (90) is of the form:

$$\begin{aligned}
M_{fi}^{(s)} = & -e\sqrt{4\pi}\bar{u}(p') \left\{ \left((\gamma e') - e^2 a^2 \frac{(ke')}{2(kp)} \frac{(\gamma k)}{(kp')} \right) B_s + \right. \\
& e \left(\frac{(\gamma a_1)(\gamma k)(\gamma e')}{2(kp')} + \frac{(\gamma e')(\gamma k)(\gamma a_1)}{2(kp)} \right) B_{1s} + \\
& \left. e \left(\frac{(\gamma a_2)(\gamma k)(\gamma e')}{2(kp')} + \frac{(\gamma e')(\gamma k)(\gamma a_2)}{2(kp)} \right) B_{2s} \right\} u(p), \quad (95)
\end{aligned}$$

with B_s, B_{1s}, B_{2s} defined by eqs. (26), (27), (28).

Now, we can express the total probability in terms of

$$u = \frac{(kk')}{(kp')}, \quad u_s = \frac{2s(kp)}{m_*^2}. \quad (96)$$

Then, we must also express α_1 and α_2 using

$$kq = kp, \quad kq' = kp', \quad a_1p = a_1q, \quad a_1p' = a_1q', \quad a_2p = a_2q, \quad a_2p' = a_2q', \quad (97)$$

as it follows from eq. (96).

Then, introducing the quantity Q , $Q = eq/kq - eq'/kq'$, we get $\alpha_1 = a_1Q$, $\alpha_2 = a_2Q$ and $z^2 = (\alpha_1^2 + \alpha_2^2) = ((a_1Q)^2 + (a_2Q)^2) = (\mathbf{a}_1\mathbf{Q})^2 + (\mathbf{a}_2\mathbf{Q})^2 = a^2(Q_1^2 + Q_2^2) = a^2Q^2$, noting that $Q_0 = Q_3$.

Let us only remark that the last relations follows by choosing a frame of reference in which $(a_1)_0 = (a_2)_0 = 0$ and the vectors $\mathbf{a}_1, \mathbf{a}_2, \mathbf{k}$ are along axes x^1, x^2, x^3 , and $Q_0 = Q_3$ because of $kQ = 0$.

The quantity z can be expressed in terms of $u, u_s\xi, m, s$ as follows [2]:

$$z = 2sm^2 \frac{\xi}{\sqrt{1 + \xi^2}} \sqrt{\frac{u}{u_s} \left(1 - \frac{u}{u_s} \right)}. \quad (98)$$

The amplitude of the process of emission of the s -th harmonics is M_{fi}^s . The expression corresponding to the differential probability per volume and time of the process of emission is as follows:

$$dW_s = |M_{fi}^s|^2 \frac{d^3k' d^3q'}{(2\pi)^6 2\omega' 2q_0 2q'_0} (2\pi)^4 \delta^{(4)}(sk + q - q' - k'). \quad (99)$$

It is necessary to remark that the normalization of the electron function Ψ_p to unit density corresponds to normalization of the δ -function “on the $q/2\pi$ scale”, where the factor q_0/p_0 on the right will now be absent. It is for this reason that the number of the final states of the electron must be measured by the element $d^3q/(2\pi)^3$.

The amplitude M_{fi}^s have the a structure similar to that of the scattering amplitude with plane waves $\bar{u} \dots u$. The operations of summation over polarizations of the particles are there carried out in the usual manner. After summation over the polarizations of the initial electron, we have

$$dW_s = \frac{e^2 m^2}{4\pi} \frac{d^3 k' d^3 q'}{\omega' q_0 2q'_0} \delta^{(4)}(sk + q - q' - k') \times$$

$$\left\{ -2J_s^2(z) + \xi^2 \left(1 + \frac{kk'}{2(kp)(kp')} \right) \left(J_{s+1}^2(z) + J_{s-1}^2(z) - 2J_s^2(z) \right) \right\}, \quad (100)$$

In order to integrate expression dW_s , we note that, owing to the axial symmetry of the field of a circularly polarized wave, the differential probability is independent on the azimuth angle φ around the direction of $\mathbf{k}$. This fact, together with the presence of the delta-function, enables us to integrate over all variables except one, which we take to be the invariant $u = (kk')/(kp')$. Then, after integration over $d^3 k d\varphi d(q'_0 + \omega')$, we find

$$\delta^4(sk + q - q' - k') \frac{d^3 q' d^3 k'}{2(kp)(kp')} \rightarrow \frac{2\pi u du}{(1+u)^2}. \quad (101)$$

The last relation can be easily explained using the center-of-mass system in which $s\mathbf{k} + \mathbf{q} = \mathbf{q}' + \mathbf{k}' = 0$. This integration gives

$$2\pi|\mathbf{q}'|d\cos\theta/E_s; \quad E_s = s\omega + q_0 = \omega' + q'_0 \quad (102)$$

and the quantity θ is the angle between $\mathbf{k}$ and $\mathbf{q}'$. In the same system we have moreover

$$u = \frac{E_s}{q'_0 - |\mathbf{q}'| \cos\theta} - 1; \quad d\cos\theta = \frac{E_s du}{|\mathbf{q}'|(1+u)^2}. \quad (103)$$

The range of variable $\cos\theta$ is $-1 \leq \cos\theta \leq 1$. Then the corresponding range of the quantity u is as follows:

$$0 \leq u \leq u_s \equiv \frac{E_s^2}{m_*^2} - 1 = \frac{2s(kp)}{m_*^2}, \quad (104)$$

where we used the relation $kp = kq$. Then the total probability of the emission of photons from unit volume in unit time is [2]:

$$W = \frac{e^2 m^2}{4q_0} \sum_{s=1}^{\infty} \int_0^{u_s} \frac{du}{(1+u)^2} \times$$

$$\left\{ -4J_s^2(z) + \xi^2 \left(2 + \frac{u^2}{1+u} \right) \left(J_{s+1}^2(z) + J_{s-1}^2(z) - 2J_s^2(z) \right) \right\}, \quad (105)$$

When $\xi \ll 1$ (the condition for perturbation theory to be valid), the integrand (105) can be expanded in powers of ξ . For the first term of the sum W_1 , we get

$$W_1 = \frac{e^2 m^2}{4p_0} \xi^2 \int_0^{u_1} \left[2 + \frac{u^2}{1+u} - 4 \frac{u}{u_1} \left(1 - \frac{u}{u_1} \right) \right] du =$$

$$\frac{e^2 m^2}{4p_0} \xi^2 \left[\left(1 - \frac{4}{u_1} - \frac{8}{u_1^2} \right) \ln(1+u_1) + \frac{1}{2} + \frac{8}{u_1} - \frac{1}{2(1+u_1)^2} \right] \quad (106)$$

with

$$u_1 \approx \frac{2(kp)}{m^2}. \quad (107)$$

It is possible to determine the second and the next harmonics as an analogy with the Berestetskii approach [2]. We have:

$$W_2 = \frac{e^2 m^2}{p_0} \xi^4 \int_0^{u_2} \frac{du}{(1+u)^2} \frac{u}{u_2} \left(1 - \frac{u}{u_2}\right) \left[2 + \frac{u^2}{1+u} - 4 \frac{u}{u_2} \left(1 - \frac{u}{u_2}\right)\right] =$$

$$\frac{e^2 m^2}{p_0} \xi^4 \left[\frac{1}{2} + \frac{1}{3u_1} - \frac{4}{u_1^2} - \frac{2}{u_1^3} - \frac{1}{2(1+2u_1)} - \left(\frac{1}{2u_1} - \frac{3}{2u_1^2} - \frac{3}{u_1^3} - \frac{1}{u_1^4} \right) \ln(1+2u_1) \right] \quad (108)$$

where $u_2 \approx 4(kp)/m^2$.

The term proportional to ξ^2 is the formula of Klein and Nishina.

For $u_s \ll 1$ we get so called the classical limit and in this limit we have:

$$W_1 = \frac{e^2 m^2}{4p_0} \xi^2 u_1 \left(1 - \frac{2}{5} \xi^2\right) \quad (109)$$

and

$$W_2 = \frac{e^2 m^2}{4p_0} \xi^4 u_2 (1 - \dots). \quad (110)$$

In the system where an electron is in the state of in average at rest, the total radiation of energy is formed by the following sum:

$$I = (\omega W_1 + 2\omega W_2 + \dots)/n = e^2 \omega^2 \xi^2 (1 + \xi^2)/6\pi = I_{classical}. \quad (111)$$

The last formula is exact classical quantity although the sum was performed only involving $s = 2$. It means that the contribution of higher harmonics with $\xi = 6$ are compensated by the harmonics with $s \geq 3$.

8. The Solution of the Dirac Equation for Two Plane Waves

The problem was solved by Gupta [24, 25] without some application. We follow the author article [26]. We suppose that the total vector potential is given as a superposition of the potential A and B as follows:

$$V_\mu = A_\mu(\varphi) + B_\mu(\chi), \quad (112)$$

where $\varphi = kx$ and $\chi = \kappa x$ and $k \neq \kappa$.

We suppose that the Lorentz condition is valid. Or,

$$\partial_\mu V^\mu = 0 = k_\mu \frac{\partial A^\mu}{\partial \varphi} + \kappa_\mu \frac{\partial B^\mu}{\partial \chi} = k_\mu A^\mu_\varphi + \kappa_\mu B^\mu_\chi, \quad (113)$$

where the subscripts φ, χ denote partial derivatives. The equation (113) can be written in the more simple form if we notice that partial differentiation with respect to φ concerns only A and partial differentiation with respect to χ concerns only B . So we write instead eq. (113).

$$\partial_\mu V^\mu = 0 = k_\mu (A^\mu)' + \kappa_\mu (B^\mu)' = kA' + \kappa B'. \quad (114)$$

Without loss of generality, we can write instead of equation (114) the following one

$$k_\mu (A^\mu)' = 0; \quad \kappa_\mu (B^\mu)' = 0; \quad \text{or,} \quad kA = \text{const} = 0; \quad \kappa B = \text{const} = 0, \quad (115)$$

putting integrating constant to zero.

The electromagnetic tensor $F_{\mu\nu}$ is expressed in the new variables as

$$F_{\mu\nu} = k_\mu A'_\nu - k_\nu A'_\mu + \kappa_\mu B'_\nu - \kappa_\nu B'_\mu. \quad (116)$$

Now, we can write Dirac equation for the two potentials the form

$$[-\partial^2 - 2ie(V\partial) + e^2 V^2 - m^2 - \frac{i}{2}eF_{\mu\nu}\sigma^{\mu\nu}]\psi = 0. \quad (117)$$

where $V = A + B$, $F_{\mu\nu}$ is given by eq. (116) and the combination of it with σ is defined as follows:

$$\frac{i}{2}eF_{\mu\nu}\sigma^{\mu\nu} = ie(\gamma k)(\gamma A') + ie(\gamma \kappa)(\gamma B'). \quad (118)$$

We will look for the solution in the standard Volkov form, or:

$$\psi = e^{-ipx} F(\varphi, \chi). \quad (119)$$

After performing all operations prescribed in eq. (117), we get the following partial differential equation for the unknown function $F(\varphi, \chi)$:

$$\begin{aligned} & -2k\kappa F_{\varphi\chi} + (2ipk - 2ikB)F_\varphi + (2ip\kappa - 2ieA\kappa)F_\chi + \\ & (e^2(A+B)^2 - 2e(A+B)p - ie(\gamma k)(\gamma A') - ie(\gamma \kappa)(\gamma B'))F = 0. \end{aligned} \quad (120)$$

8.1. The Solution of the Dirac Equation for Two Orthogonal Waves

We write eq. (120) in the following form:

$$aF_\varphi + bF_\chi + cF = 2k\kappa F_{\varphi\chi}, \quad (121)$$

where

$$a = 2ipk - 2iekB; \quad b = 2ip\kappa - 2ie\kappa A \quad (122)$$

and

$$c = e^2(A + B)^2 - 2e(A + B)p - ie(\gamma k)(\gamma A') - ie(\gamma \kappa)(\gamma B'). \quad (123)$$

and the term of with the two partial derivations is not present because of $k\kappa = 0$.

For the field which we specify by the conditions

$$kB = 0; \quad \kappa A = 0; \quad AB = 0, \quad (124)$$

we have:

$$2ipkF_\varphi + 2ip\kappa F_\chi + (e^2A^2 + e^2B^2 - 2epA - 2epB - ie(\gamma k)(\gamma A') - ie(\gamma \kappa)(\gamma B'))F = 2k\kappa F_{\varphi\chi}. \quad (125)$$

Now, we are looking for the solution in the most simple form

$$F(\varphi, \chi) = X(\varphi)Y(\chi). \quad (126)$$

After insertion of (126) into (125) and division the new equation by XY we get the terms depending only on φ , and on χ . Or, in other words we get:

$$\begin{aligned} & \left(2i(pk + ik\kappa) \frac{X'}{X} + e^2A^2 - 2epA - ie(\gamma k)(\gamma A') \right) + \\ & \left(2i(p\kappa + ik\kappa) \frac{Y'}{Y} + e^2B^2 - 2epB - ie(\gamma \kappa)(\gamma B') \right) = 0 \end{aligned} \quad (127)$$

So, there are terms dependent on φ and terms dependent on χ only in eq. (127). The only possibility is that they are equal to some constant λ and $-\lambda$. Then,

$$2i(pk + ik\kappa)X' + (e^2A^2 - 2epA - ie(\gamma k)(\gamma A'))X = \lambda X \quad (128)$$

and

$$2i(p\kappa + ik\kappa)Y' + (e^2B^2 - 2epB - ie(\gamma \kappa)(\gamma B'))Y = -\lambda Y \quad (129)$$

We put $\lambda = 0$ without lost of generality. Now, the solution of eq. (127) is reduced to the solution of two equations only. Because the form of the equations is similar to the standard Volkov form, we can write the solution of these equations as follows:

$$X = \left[1 + \frac{e}{2(kp + ik\kappa)} (\gamma k)(\gamma A) \right] \frac{u}{\sqrt{2p_0}} e^{iS_1}, \quad (130)$$

with

$$S_1 = \int_0^{kx} \frac{e}{(kp + ik\kappa)} \left[(pA) - \frac{e}{2}(A)^2 \right] d\varphi. \quad (131)$$

and

$$Y = \left[1 + \frac{e}{2(\kappa p + ik\kappa)} (\gamma\kappa)(\gamma B) \right] \frac{u}{\sqrt{2p_0}} e^{iS_2}, \quad (132)$$

with

$$S_2 = - \int_0^{\kappa x} \frac{e}{(\kappa p + ik\kappa)} \left[(pB) - \frac{e}{2}(B)^2 \right] d\chi. \quad (133)$$

The total solution is then of the form:

$$\psi_p = \left[1 + \frac{e}{2(\kappa p + ik\kappa)} (\gamma k)(\gamma A) \right] \left[1 + \frac{e}{2(\kappa p + ik\kappa)} (\gamma\kappa)(\gamma B) \right] \frac{u}{\sqrt{2p_0}} e^{i(S_1(A) + S_2(B))}. \quad (134)$$

8.2. The Compton Process with Two Photons

In case of the two laser beams which are not collinear the experimental situation involves possibility that the two different photons can interact with one electron. The theory does not follow from the standard one-photon Volkov solution because in the standard approach the multiphoton interaction involve the collinear photon s and not photons from the two different lasers. The problem was solved by Lyulka in 1974 [27] for the case of the two linearly polarized waves

$$A = a_1 \cos \varphi; \quad B = a_2 \cos(\chi + \delta) \quad (135)$$

with the standard conditions for φ, χ, k, κ . The quantity δ is the phase shift.

The two-wave Volkov solution is given by eq. (134) and the matrix elements and appropriate ingredients of calculations are given by the standard approach as it was shown by Lyulka [27].

It was shown, that

$$q^\mu = p^\mu - e^2 \frac{a_1^2}{2(\kappa p)} k^\mu - e^2 \frac{a_2^2}{2(\kappa p)} \kappa^\mu \quad (136)$$

and

$$m_*^2 = m^2 \left(1 - \frac{e^2 a_1^2}{m^2} - \frac{e^2 a_2^2}{m^2} \right). \quad (137)$$

The matrix element involves the extended law of conservation. Namely:

$$sk + t\kappa + q = q' + k' + \kappa', \quad (138)$$

where s and t are natural numbers and the interpretation of the last equation is evident. The multiphoton objects with momenta sk and $t\kappa$ interact with electron with momentum q . After interaction the electron has a momentum q' and two photons are emitted with momenta k' and κ' .

Instead of equation (138), we can write

$$sk + q - k' = q' + \kappa' - t\kappa. \quad (139)$$

From the squared form of the last equation and after some modification, we get the following generalized equation of the double Compton process for $s = t = 1$:

$$\frac{1}{\omega'} - \frac{1}{\omega} = \frac{1}{m_*}(1 - \cos \Theta) + \frac{\Omega' - \Omega}{\omega\omega'} - \frac{\Omega\Omega'}{\omega\omega'm_*}(1 - \cos \Xi), \quad (140)$$

where the angle Ξ is the angle between the 3-momentum of the κ -photon and the 3-momentum of the κ' -photon with frequency Ω and Ω' respectively.

Let us remark that if the frequencies of the photons of the first wave substantially differs from the frequency of photons of the second electromagnetic wave, then, the derived formula (140) can be experimentally verified by the same way as the original Compton formula. To our knowledge, formula (140) is not involved in the standard textbooks on quantum electrodynamics because the two laser physics is at present time not sufficiently developed.

It is possible to consider the situation with sum of N waves, or,

$$V = \sum_{i=1}^N A_i(\varphi_i) \quad \varphi_i = k_i x. \quad (141)$$

The problem has obviously physical meaning because the problem of the laser compression of target by many beams is one of the prestigious problems of the today laser physics. The goal of the experiments is to generate the physical process of implosion. When an intense petawatt laser light is uniformly impinged on a spherical fuel pellet, the laser energy is absorbed on the surface to generate a high-temperature plasma of 2-3 keV and an extremely high pressure of a few hundred megabars is generated. The pressure accelerates the outer shell of the target toward the target center. If the dynamics is sufficiently spherically symmetric, the central area is heated up to 5-10 keV and fusion reaction starts [28]. The solution of that problem in the general form is not elementary and can be solved only by some laser institution such as the Lebedev institution of physics, the Livermore laser national laboratory and so on.

On the other hand, if the target is constituted from material in the form a foam, then instead of using 100-200 laser beams it is possible to use only two laser beams, as it is supposed in our article. Then the nuclear fusion is more realistic in the situation of the thermonuclear reactor [29].

Nuclear fusion involves the bringing together of atomic nuclei. The sum of the individual masses of the nucleons is greater than the mass of the whole nucleus. This is because the strong nuclear force holds the nucleons together. Then, the combined nucleus is a lower energy state than the nucleons separately. The energy difference is released in the fusion process.

There are two major fusion processes. The magnetic confinement and inertial confinement. The inertial fusion occurs inside targeted fuel pellets by imploding them with laser or particle beam irradiation in brief pulses. It produces extremely high densities in the targeted where the laser pulse creates a shock wave in the pellet that it intensified by its internal

geometry. On the other hand, magnetic fusion devices, like the tokamak, operate at lower densities, but use magnetic fields to confine the plasma for longer time.

To achieve a burning plasma, a sufficiently high density of fuel must be heated to temperatures about 100 million degrees of Celsius that the nuclei collide often enough despite their natural repulsive forces and energy losses.

The fuels to be used are two isotopes of hydrogen. Namely, deuterium and tritium. While deuterium occurs naturally in sea water and it means it is inexhaustible, tritium can be produced in a fusion system when the light element, lithium, absorbs neutrons produced in the fusion reaction. World resources of lithium are inexhaustible and it means that also the energy obtained by fusion process is practically infinite.

9. Synchrotron Radiation from the Volkov Solution for the Dirac Electron Moving in Magnetic Field

The presence of the magnetic field in which the pellet can be situated leads to the generation of the synchrotron radiation of charged particles. Motivated by this fact, we derive the synchrotron radiation formulas using the Volkov solution of the Dirac equation and the S-matrix formalism of QED for an electron moving in the constant magnetic field.

Schott in 1907 [30], [31] was developing the classical theory of electromagnetic radiation of electron moving in the uniform magnetic field. His calculation was based on the Poynting vector. The goal of Schott was to explain the spectrum of radiation of atoms. Of course the theory of Schott was unsuccessful because only quantum theory is adequate to explain the emission spectrum of atoms. On the other hand the activity of Schott was not meaningless because he elaborated the theory of radiation of charged particles moving in the electromagnetic field. His theory appeared to be only of the academical interest for 40 years. Then, it was shown that the theory and specially his formula has deep physical meaning and applicability. His formula is at the present time the integral part of the every textbook on the electromagnetic field.

The Schott formula was rederived many times by the different methods. The most elegant derivation was given by Schwinger in his classical articles [32, 33], [34] and from the so called source theory which is quantum electrodynamics and gravity without renormalization and divergences [35].

Around year 1947 Floyd Haber, a young staff member and technician in the laboratory of prof. Pollock, visually observed radiation of electrons moving circularly in the magnetic field of the chamber of an accelerator [36]. It occurred during adjustment of cyclic accelerator-synchrotron which accelerated electrons up to 100 MeV. The radiation was observed as a bright luminous patch on the background of the chamber of the synchrotron. It was clearly visible in the daylight. In this way the “electron light” was experimentally revealed for the first time as the radiation of relativistic electrons of large centripetal acceleration. The radiation was identified with the Ivanenko and Pomeranchuk radiation, or with the Schwinger radiation and later was called the synchrotron radiation since it was observed for the first time in synchrotron. The radiation was considered as the mysterious similarly to the Roentgen mysterious x-rays.

A number of theoretical studies on the emission of a relativistic accelerating electron

had been carried out long before the cited experiment. The first steps in this line was treaded by Lienard [37].

The classical derivation of the Schott formula is based on the Poynting vector $\mathbf{S}$ [38, 39]:

$$\mathbf{S} = \frac{c}{4\pi} \mathbf{E} \times \mathbf{H} \quad (142)$$

and $\mathbf{E}$ and $\mathbf{H}$ are intensities of the electromagnetic field of an electron moving in the constant magnetic field, where the magnetic field is in the direction of the axis z . In this case electron moves along the circle with radius R and the electromagnetic field is considered in the wave zone and in a point with the spherical coordinates r, θ, φ . The nonzero components of the radiated field are calculated from the vector potential $\mathbf{A}$ which is expressed as the Fourier integral [38].

Lienard [37] used the Larmor formula

$$P = \frac{2}{3} \frac{e^2}{c^3} \left(\frac{d\mathbf{v}}{dt} \right)^2 = \frac{2}{3} \frac{e^2}{m^2 c^3} \left(\frac{d\mathbf{p}}{dt} \right)^2, \quad (143)$$

and extended it to the high-velocity particles. He also received the total radiation of an electron following a circle of an circumference $2\pi R$.

In modern physics, Schwinger [32, 33] used the relativistic generalization of the Larmor formula to get the total synchrotron radiation. Schwinger also obtained the spectrum of the synchrotron radiation from the method which was based on the electron work on the electromagnetic field, $P = - \int (\mathbf{j} \cdot \mathbf{E}) d\mathbf{x}$, where the intensity of electric field he expressed as the subtraction of the retarded and advanced electric field of a moving charge in a magnetic field, $\mathbf{E} = \frac{1}{2}(\mathbf{E}_{ret} - \mathbf{E}_{adv})$, [33].

The circular classical trajectory of the electron is created by the Lorentz force $F = (e/c)(\mathbf{v} \times \mathbf{H})$. The trajectory is stationary when the radiative reaction is not considered. The radiative reaction causes the transformation of the circular trajectory to the spiral trajectory. In quantum mechanics, the trajectory is stationary when neglecting the interaction of an electron with the vacuum field. The interaction of an electron with the vacuum field, causes the electron jumps from the higher energetic level to the lower ones. In quantum electrodynamics description of the motion of electron in a homogeneous magnetic field, the stationarity of the trajectories is broken by including the mass operator into the wave equation. Then, it is possible from the mass operator to derive the power spectral formula [40]. Different approach is involved in the Schwinger et al. article [34].

We will show in the next text how to determine formula for the intensity of the synchrotron radiation from the Volkov solution of the Dirac equation in the magnetic field and from the S-matrix method.

In order to derive the classical limit of the quantum radiation formula, we will suppose that the motion of the Dirac electron is performed in the following four potential:

$$A_\mu = a_\mu \varphi; \quad \varphi = kx; \quad k^2 = 0. \quad (144)$$

From equation (144), it follows that $F_{\mu\nu} = \partial_\mu A_\nu - \partial_\nu A_\mu = k_\mu a_\nu - k_\nu a_\mu = const.$, which means that electron moves in the constant electromagnetic field with the components

E and **H**. The parameters a and k can be chosen in a such a way that $\mathbf{E} = 0$. So the motion of electron is performed in the constant magnetic field.

The Volkov [41] solution of the Dirac equation for an electron moving in a field $A(\varphi)$ is [2]:

$$\psi_p = \frac{u(p)}{\sqrt{2p_0}} \left[1 + e \frac{(\gamma k)(\gamma A(\varphi))}{2kp} \right] \exp [(i/\hbar)S] \quad (145)$$

and S is an classical action of an electron moving in the potential $A(\varphi)$.

$$S = -px - \int_0^{kx} \frac{e}{(kp)} \left[(pA) - \frac{e}{2}(A)^2 \right] d\varphi. \quad (146)$$

It was shown that for the potential (10) the Volkov wave function is [2]:

$$\psi_p = \frac{u(p)}{\sqrt{2p_0}} \left[1 + e \frac{(\gamma k)(\gamma a)}{2kp} \varphi \right] \exp [(i/\hbar)S] \quad (147)$$

with

$$S = -e \frac{ap}{2kp} \varphi^2 + e^2 \frac{a^2}{6kp} \varphi^3 - px. \quad (148)$$

During the following text we will suppose that we will work in the unit system where $c = \hbar = 1$.

9.1. S-matrix Element for Photon Emission by Electron Moving in the Constant Magnetic Field

While the Larmor formula (143) involves explicitly the dependence of the radiation on the derivative of the particle velocity over the time, the quantum field theory works only with the matrix elements and power spectrum must be determined from the correct definition of the matrix element in case that electron is moving in potential (144). The alternative method can be considered and it consists in using the quantization of the Poynting vector (142) in the differential intensity as $dI = (\mathbf{r} \cdot \mathbf{S})d\Omega$. However, to our knowledge, this method was not elaborated and published. Similarly, the gravity radiation was not determined from the general relativistic definition of the Poynting vector.

The situation in the quantum physics differs from the situation in the classical one. The emission of photons by electron moving in the homogeneous magnetic field is the result of the transition of electron from the stationary state with energy E_a to stationary state with energy E_b , where $E_a > E_b$. The transition between stationary states is called spontaneous, however it is stimulated by the interaction of an electron with the virtual electromagnetic field of vacuum, or, in other words, by the interaction of electron with virtual photons. So, it is necessary to find the interaction term of an electron with vacuum photons and to solve the Dirac equation with this term and then to determine the matrix elements of the transition.

The quantum field theory expressed as the source theory was used to solved the synchrotron radiation by Schwinger [40]. In this language and methodology the original action term for the spin-0 charged particle was supplemented by the mass operator in the homogeneous magnetic field and it was shown that this mass operator involves as an integral part

the power spectral formula of the synchrotron radiation. Here we use the Volkov solution of the Dirac equation and the S-matrix approach to find the probability of emission and the intensity of the synchrotron radiation. The method is total because the Volkov solution of the Dirac equation can be expressed in the total form.

While the Feynman diagram approach requires renormalization procedure and the Schwinger source methods requires contact terms as some form of renormalization, our method does not work with renormalization. The wave function of an electron involves the total interaction of an electron with magnetic field.

It is possible to show in the quantum field theory, that the corresponding S-matrix element which describes transition from the state ψ_p to $\psi_{p'}$ with simultaneous emission of photon with polarization e' and four-momentum $k'^\mu = (k'_0, \mathbf{k}') = (\omega', \mathbf{k}')$ is given by the following expression [2], with $k' \rightarrow -k'$, $S \rightarrow -S$, to be in accord with the Ritus article [8]:

$$M = e \int d^4x \bar{\psi}_{p'}(\gamma e'^*) \psi_p \frac{e^{-ik'x}}{\sqrt{2\omega'}}, \quad (149)$$

where ψ_p is given by the relation

$$\psi_p = \exp i \left\{ e \frac{(ap)}{2(kp)} \varphi^2 - e^2 \frac{a^2}{6(kp)} \varphi^3 + px \right\} \left[1 + e \frac{(\gamma k)(\gamma a)}{2(kp)} \varphi \right] \frac{u(p)}{\sqrt{2p_0}} \quad (150)$$

and $\bar{\psi}_p$ is the the conjugated function to ψ_p , or,

$$\bar{\psi}_p = \frac{\bar{u}(p)}{\sqrt{2p_0}} \left[1 + e \frac{(\gamma a)(\gamma k)}{2(kp)} \varphi \right] \exp(i) \left\{ -e \frac{(ap)}{2(kp)} \varphi^2 + e^2 \frac{a^2}{6(kp)} \varphi^3 - px \right\}. \quad (151)$$

Afer insertion of eq. (150) and (151) into eq. (149) and putting

$$\exp \left\{ i \left(\frac{\alpha \varphi^2}{2} - \frac{i4\beta \varphi^3}{3} \right) \right\} = \int_{-\infty}^{\infty} ds e^{is\varphi} A(s, \alpha, \beta) \quad (152)$$

with

$$\alpha = e \left(\frac{ap}{kp} - \frac{ap'}{kp'} \right); \quad \beta = \frac{e^2 a^2}{8} \left(\frac{1}{kp} - \frac{1}{kp'} \right), \quad (153)$$

we get [8]:

$$M = e \int_{-\infty}^{\infty} \frac{ds}{\sqrt{2\omega'}} (2\pi)^4 \delta(p + sk - p' - k') \bar{u}(p') \times \left\{ (\gamma e'^*) A + e \left(\frac{(\gamma a)(\gamma k)(\gamma e'^*)}{2(kp')} + \frac{(\gamma e'^*)(\gamma k)(\gamma a)}{2(kp)} \right) i \frac{\partial A}{\partial s} + \frac{e^2 a^2 (k e'^*)(\gamma k)}{2(kp)(kp')} \frac{\partial^2 A}{\partial s^2} \right\} u(p). \quad (154)$$

It evidently follows from eq. (152), that

$$A(s, \alpha, \beta) = \frac{1}{2\pi} \int_{-\infty}^{\infty} d\varphi \exp \left\{ i \left(\frac{\alpha\varphi^2}{2} - \frac{4\beta\varphi^3}{3} - s\varphi \right) \right\}. \quad (155)$$

The terms $i^n \partial^n A / \partial s^n$ are the Fourier mapping of functions

$$\varphi^n \exp(i\alpha\varphi^2/2 - i4\beta\varphi^3/3). \quad (156)$$

The matrix element M is analogical to the emission of photons generated by the electron in the plane electromagnetic wave $A_\mu = a_\mu \cos(kx)$ which was proved by Ritus [8], and the difference is in replacing the discrete s by the continual quantity. So the summation over s is replaced by the integration.

Function $A(s, \alpha, \beta)$ can be expressed by the Airy function $\Phi(y)$:

$$A(s, \alpha, \beta) = \frac{1}{\pi} (4\beta)^{-1/3} \exp \left\{ -is \frac{\alpha}{8\beta} + i \frac{8\beta}{3} \left(\frac{\alpha}{8\beta} \right)^3 \right\} \Phi(y), \quad (157)$$

where the Airy function $\Phi(y)$ is defined by the equation

$$\frac{d^2 \Phi}{dy^2} - y\Phi = 0 \quad (158)$$

with the solution

$$\Phi(y) = \frac{1}{2} \int_{-\infty}^{\infty} du e^{-i\left(\frac{u^3}{3} + yu\right)} = \int_0^{\infty} du \cos \left(\frac{u^3}{3} + yu \right), \quad (159)$$

where in our case

$$y = (4\beta)^{2/3} \left[\frac{s}{4\beta} - \left(\frac{\alpha}{8\beta} \right)^2 \right], \quad (160)$$

where $\beta \geq 0$. Landau et al. [42] uses the Airy function expressed as $\Phi/\sqrt{\pi}$.

Using the formula (157) it is easy to find the differential equation for $A(s)$:

$$sA - i\alpha A' - 4\beta A'' = 0, \quad (161)$$

where $A' = \partial A / \partial s$, $A'' = \partial^2 A / \partial s^2$.

The evaluation of the squared matrix elements, which has physical meaning of the probability of the radiation process, involves, as can be seen, the double integral for which we use the identity:

$$\begin{aligned} \int_{-\infty}^{\infty} ds \int_{-\infty}^{\infty} ds' F(s) \delta(sk + p - p' - k') \delta(s'k + p - p' - k') = \\ \int_{-\infty}^{\infty} ds \int_{-\infty}^{\infty} ds' F(s) \frac{\delta(s - s')}{\delta(0)} \delta(sk + p - p' - k') = \\ \frac{VT}{(2\pi)^4} \int_{-\infty}^{\infty} ds \frac{F(s)}{\delta(0)} \delta(sk + p - p' - k'). \end{aligned} \quad (162)$$

So, now, we are prepared to determine the probability of the emission of photons and we perform it in the following section.

9.2. Probability of Emission of Photons

Using the ingredients of the quantum field theory, we get for the probability of the emission of one photon an electron in unit volume per unit time [8]:

$$\sum_{r,r'} \frac{|M|^2}{VT} = \frac{(2\pi)^5 e^2}{\delta(0)} \int_{-\infty}^{\infty} \frac{ds}{2p_0 p'_0 \omega'} \delta(sk + p - p' - k') \times \left\{ |pe''^* A - ieae''^* A'|^2 - 2\beta(kk')(|A'|^2 + \text{Re}AA''^*) \right\}, \quad (163)$$

where

$$e''_{\alpha} = e'_{\alpha} - k'_{\alpha}(ke')/(kk'); \quad A' = \partial A / \partial s; \quad A'' = \partial^2 A / \partial s^2. \quad (164)$$

After summation of eq. (163) over directions of polarization e' and using differential equation (161), $\sum |M|^2 / VT$ will be expressed only by means of $|A|^2$ and $|A'|^2 + \text{Re}AA''^*$, which can be expressed using eq. (157) in the following way:

$$|A|^2 = \frac{\Phi^2(y)}{\pi^2(4\beta)^{2/3}}; \quad |A'|^2 + \text{Re}AA''^* = \frac{y\Phi^2(y) + \Phi'^2(y)}{\pi^2(4\beta)^{2/3}}. \quad (165)$$

Then, with

$$x = \frac{ea}{m}, \quad \chi = -\frac{kp}{m^2}x, \quad \chi' = -\frac{kp'}{m^2}x, \quad \kappa = -\frac{kk'}{m^2}x, \quad (166)$$

we have ($k'_0 = \omega'$)

$$\sum_{r,r'} \frac{|M|^2}{VT} = \frac{2e^2 m^2}{\delta(0)x^2 p_0 p'_0 k'_0} \int_{-\infty}^{\infty} ds \left(\frac{2\chi\chi'}{\kappa} \right)^{2/3} \delta(sk + p - p' - k') \times \left\{ -\Phi^2(y) + \left(\frac{2\chi\chi'}{\kappa} \right) \left(1 + \frac{\kappa^2}{2\chi\chi'} \right)^{2/3} [y\Phi^2(y) + \Phi'^2(y)] \right\}. \quad (167)$$

In order to obtain the probability of the emission of photon by electron, it is necessary to integrate equation (167) over the final states $d^3p' d^3k' (2\pi)^{-6}$ of the electron and photon and the result divide by 1/2 and to average over polarizations of the initial electron. Integration over $\mathbf{p}'$ eliminates space δ -function and the time δ -function can be transformed into the explicit Lorentz invariant form as follows:

$$\delta(sk + p - p' - k') \frac{d^3p'}{p'_0} \rightarrow \frac{\delta(sk_0 + p_0 - p'_0 - k'_0)}{p'_0} = -\frac{\delta(s - \tilde{s})}{kp'}; \quad \tilde{s} = \frac{k'p'}{kp} \quad (168)$$

with the use of the relation

$$p'_0 = \sqrt{m^2 + (s\mathbf{k} + \mathbf{p} - \mathbf{k}')^2}. \quad (169)$$

Using the equation (167) and after integration over s , we get the differential probability of the emission of photon per unit time:

$$dW = \frac{e^2 c}{4\pi^3 x \delta(0) \chi'} \left(\frac{2\chi}{u} \right)^{2/3} \times \left[-\Phi^2(y) + \left(\frac{2\chi}{u} \right)^{2/3} \left(1 + \frac{u^2}{2(1+u)} \right) (y\Phi^2(y) + \Phi'^2(y)) \right] \frac{d^3 k'}{k'_0}, \quad (170)$$

where

$$u = \frac{\kappa}{\chi'} = \frac{k k'}{k p'}, \quad c = \frac{1}{p_0}. \quad (171)$$

The equation (170) is evidently relativistic and gauge invariant. The further properties are as follows. It does not depend on k'_1 , which is the component of the photon momentum along the electric field $\mathbf{E}$. It means it does not depend on ϱ . We use further the transform

$$\frac{d^3 k'}{k'_0} = \frac{x m^2 \chi' u}{\chi(1+u)^2} d\varrho d\tau du \quad (172)$$

with the obligate relation [8]:

$$\int_{-\infty}^{\infty} d\varrho = \delta(0). \quad (173)$$

After integration of (170) over ϱ and with regard to (173), we get the probability of emission of photons in variables u, τ without dependence of the localization of the emission the following formula:

$$dW = \frac{e^2 m^2 c}{2\pi^3 (1+u)^2} \left(\frac{u}{2\chi} \right)^{1/3} \times \left[-\Phi^2(y) + \left(\frac{2\chi}{u} \right)^{2/3} \left(1 + \frac{u^2}{2(1+u)} \right) (y\Phi^2(y) + \Phi'^2(y)) \right] du d\tau. \quad (174)$$

The probability (172) has a dimension of $\text{cm}^{-3} \text{s}^{-1}$.

Formula (174) describes the dependence of the distribution of probability on two variables u, τ . If we use equation

$$y\Phi^2(y) + \Phi'^2(y) = \frac{1}{2} \frac{d^2}{dy^2} \Phi^2(y) \quad (175)$$

and the transformation $t = a\tau^2$; $a = \left(\frac{u}{2\chi} \right)^{2/3}$, $d\tau = \frac{dt}{2\sqrt{at}}$, then, we get with $y = a + t$ the following result

$$\frac{dW}{du} = \frac{e^2 m^2 c}{2\pi^3 (1+u)^2} \times \left\{ -1 + \left(\frac{2\chi}{u} \right)^{2/3} \left[1 + \frac{u^2}{2(1+u)} \right] \frac{1}{2} \frac{d^2}{da^2} \right\} \int_0^\infty \frac{dt}{\sqrt{t}} \Phi^2(a+t); \quad a = \left(\frac{u}{2\chi} \right)^{2/3}. \quad (176)$$

Now, let us use the integral transformation by Aspnes [43],

$$\int_0^\infty \frac{dt}{\sqrt{t}} \Phi^2(a+t) = \frac{\pi}{2} \int_{2^{2/3}a}^\infty dy \Phi(y). \quad (177)$$

Then, we get from the formula (176)

$$\frac{dW(\chi, u)}{du} = -\frac{e^2 m^2 c}{4\pi^2 (1+u)^2} \times \left\{ \int_z^\infty dy \Phi(y) + \frac{2}{z} \left[1 + \frac{u^2}{2(1+u)} \right] \Phi'(z) \right\}; \quad z = \left(\frac{u}{\chi} \right)^{2/3}. \quad (178)$$

The last formula can be written easily for small and big u as follows:

$$\frac{dW}{du} = -\Phi'(0) \frac{e^2 m^2 c}{2\pi^2} \left(\frac{\chi}{u} \right)^{2/3}; \quad u \ll 1, \quad \chi; \quad (179)$$

where

$$\begin{aligned} \Phi'(0) &= -\frac{1}{3^{1/3}} \int_0^\infty x^{-1/3} \sin x \, dx = -\frac{1}{3^{1/3}} \int_0^\infty x^{\mu-1} \sin(ax) \, dx \Big|_{\mu=2/3; a=1} = \\ &= -\frac{1}{3^{1/3}} \frac{\Gamma(\mu)}{a^\mu} \sin\left(\frac{\mu\pi}{2}\right) \Big|_{\mu=2/3; a=1} = -3^{1/6} \frac{\Gamma(2/3)}{2}, \end{aligned} \quad (180)$$

$$\frac{dW}{du} = \frac{e^2 m^2 c}{8\pi^{3/2} u^{3/2}} \sqrt{\chi} \exp\left(\frac{-2u}{3\chi}\right); \quad u \gg 1, \quad \chi. \quad (181)$$

If we integrate the formula (178) over u and using the per partes method in the first term, we get the following formula:

$$W(\chi) = -\frac{e^2 m^2 c}{8\pi^2} \chi \int_0^\infty dz \frac{5+7u+5u^2}{\sqrt{z}(1+u)^3} \Phi'(z); \quad u = \chi z^{3/2}. \quad (182)$$

The last formula was derived for the first time by Goldman [44, 45] in 1964 by the different way. This formula can be expressed approximately for small and big χ as follows [8]:

$$W(\chi) = \frac{5e^2 m^2 c}{8\sqrt{3}\pi} \chi \left(1 - \frac{8\sqrt{3}}{15} \chi + \dots \right); \quad \chi \ll 1, \quad (183)$$

$$W(\chi) = \frac{7\Gamma(2/3)e^2 m^2 c}{54\pi} (3\chi)^{2/3} \left(1 - \frac{45}{28\Gamma(2/3)} (3\chi)^{-2/3} + \dots \right); \quad \chi \gg 1. \quad (184)$$

9.3. Intensity of Radiation

Using the Ritus proof [8], we can see that the intensity of radiation can be obtained from formula (172) putting $c/p_0 \rightarrow 1$ and by the multiplication by the term $u(1+u)^{-1}$. We get:

$$dI = \frac{e^2 m^2}{2\pi^3} \frac{u}{(1+u)^3} \times \left(\frac{u}{2\chi} \right)^{1/3} \left\{ -\Phi^2(y) + \left(\frac{2\chi}{u} \right)^{2/3} \left(1 + \frac{u^2}{2(1+u)} \right) (y\Phi^2(y) + \Phi'^2(y)) \right\} dud\tau. \quad (185)$$

Then the u -distribution over intensity is of the form:

$$\frac{dI}{du} = -\frac{e^2 m^2}{4\pi^2} \frac{u}{(1+u)^3} \left\{ \int_z^\infty dy \Phi(y) + \frac{2}{z} \left(1 + \frac{u^2}{2(1+u)} \right) \Phi'(z) \right\}; \quad z = \left(\frac{u}{\chi} \right)^{2/3}. \quad (186)$$

This formula is a quantum generalization of the classical expression for the spectral distribution of radiation of an ultrarelativistic charged particle in a magnetic field [42] by Landau et al. (74.13).

After integration of (186) over u and using the per partes method in the first term, we get the formula of the total radiation of photons by electron in the constant magnetic field. The formula is as follows [8]:

$$I = -\frac{e^2 m^2}{2\sqrt{\pi}\hbar^2} \chi^2 \int_0^\infty dz z \frac{4+5u+4u^2}{2(1+u)^4} \Phi'(z); \quad u = \chi z^{3/2}. \quad (187)$$

There is no doubt that the Schott formula can be derived by the formalism used in this text.

10. Conclusion

We have seen that the Volkov solution is applicable not only in the description of the classical quantum electrodynamical effect such as the Compton effect, one-photon process, or multiphoton process, but it is also applicable to the acceleration of an electron by a laser pulse, or to the process of the compression of the nuclear pellet by the two laser pulse. The method of the two lasers considered here can be generalized for the situation where the very short laser pulses are immersed in the very strong magnetic field which can be generated by the Kapitza solenoid or by the magnetic flux compression method described by Terletzkii et al., [46]. The Terletzkii method of the cumulation of magnetic flux can be realized in such a way that the pellet is immersed in the conductive magnetic cylinder and the cylinder is compressed simultaneously with the compression of the pellet by laser pulses. The compression of the cylinder produces the ultrastrong magnetic field and it means that laser photons in such field are massive. In other words, the ignition is performed with massive

photons. It is possible to expect some improvement of the fusion process in the ignition facility.

We know that the process of ignition of fusion by two lasers or by many lasers consists in the four steps: 1) **Atmospheric formation** - laser beams rapidly heat the surface of the fusion target forming a surrounding plasma envelope, 2) **Compression** - fuel is compressed by the rocket-like blowoff of the hot surface material, 3) **Ignition** - during the final part of the laser pulse, the fuel core reaches 20 times the density of lead and ignites at 100 000 000 degrees of Celsius, 4), **Burn** - thermonuclear burn spreads rapidly through the compressed fuel, yielding many times the input energy. So, the improvement of the experiment consists in preparation of the four steps in the magnetic pulse generated simultaneously with the laser pulses. To our knowledge such experiments were not still performed. The problem consists probably in the scientific and the technological difficulty in the creation of the magnetic pulse. To our knowledge there is not any monograph where it is described how to synchronize the magnetic pulse with the laser pulse.

Kurčatov discussed in his article [47] the possibility to generate the nuclear fusion by the gigantic discharge in gas, or, in the appropriate nuclear medium. Experiments were in past performed not only by the Kurčatov team but also by the experimenters from Great Britain. Kurčatov described many results of such experiments. This way is still not in the final form. Nevertheless, it seems that this way of the nuclear fusion is also realistic and the future will reveal the efficiency of such method.

There are many other trials how to realize the nuclear fusion. For instance by the table-top experiments. All of these methods was criticised as the trials without brilliant theoretical basis. Let us remember the trials with the cold fusion, the pyroelectric fusion, the piezoelectric fusion, the nanotube fusion and so on. We can say that without the theoretical basis there is no way to the success of such experiments.

The realistic way is ITER in Europe and NIF in USA. The design of ITER has been completed in early 2001 and the construction of this facility started. Fusion can contribute to the world-wide long term energetic needs and this is the main driver for continuing research in this promising but challenging field [48].

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Chapter 4

**THE DEVELOPMENT OF FUEL CLADDING
CHEMICAL INTERACTION ZONES IN IRRADIATED
U-ZR AND U-PU-ZR FUEL ELEMENTS
WITH STAINLESS STEEL CLADDING**

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Abstract

The Advanced Fuel Cycle Initiative is responsible for the development of advanced nuclear energy systems. One of these nuclear energy systems is the Sodium Fast Reactor (SFR). To maximize the performance of this type of nuclear reactor, it will be important to improve on the performance of the nuclear fuel, i.e., allow for higher fuel burnup and/or operation of the fuel at higher reactor operating temperatures. One type of nuclear fuel currently being evaluated is a metallic U-Pu-Zr alloy, and one phenomenon that could limit the ability of the fuel to perform at temperatures that are higher than what have been typically employed is the chemical compatibility of the fuel and cladding. During irradiation, the fuel swells and eventually contacts the cladding, at which time fuel cladding chemical interaction (FCCI) can occur. During this process, interdiffusion occurs between the fuel, cladding, and fission products, which can result in the formation of interaction zones on the inner surface of the cladding that can become brittle or may contain relatively low-melting phases. The result of this process can be cracking and failure of the cladding. Minimal detailed information on FCCI in irradiated metallic SFR fuels is available in the literature. Thus, in order to facilitate an increased understanding of FCCI, this chapter describes results that have been generated from the destructive examinations of individual fuel elements that were irradiated in the Experimental Breeder Reactor-II over the course of a thirty-year timeframe. This chapter particularly focuses on any interaction zones that developed on the inner surface of the cladding. Three examination techniques were employed to characterize FCCI in these fuel elements: optical metallography, electron probe micro-analysis, and scanning electron microscopy. The results of these examinations were evaluated and compared to provide information about FCCI and what effects it could have on fuel performance. Fuel elements that included U-Zr or U-Pu-Zr alloy fuels and HT-9, D9, or Type 316 stainless steel cladding were assessed. The irradiation conditions, cladding type, and axial location on fuel elements, where the thickest layers could be expected to develop, were identified, and it was found that

the largest interaction zones developed at the combined high-power and high-temperature regions of the fuel elements and for the fuel elements with U-Pu-Zr alloy fuel and D9 stainless steel cladding. The most prevalent, non-cladding constituent observed in the developed interaction layers were the lanthanide fission products.

1. Introduction

The Advanced Fuel Cycle Initiative (AFCI) is a U.S. government-sponsored program that was created to enable a transition from the current once-through nuclear fuel cycle to a future sustainable, closed nuclear fuel cycle. The primary goals of the AFCI program are as follows: (1) Develop technologies that will reduce the cost of geologic disposal of high-level waste from spent nuclear fuel, enhancing the repository performance, and (2) Develop reactor fuel and fuel cycle technologies to support Generation IV nuclear energy systems. By achieving these goals, the potential is created to significantly delay the need for a second geologic repository and dramatically reduce the inventories of civilian Pu in the U.S. while at the same time using the energy value in spent nuclear fuel [1].

One reactor type that is being investigated as part of the AFCI program is the sodium fast reactor (SFR). In order to maximize the performance of this type of nuclear reactor, it will be imperative to improve the performance of the nuclear fuel, i.e., allow for higher fuel burnup and/or operation of the fuel at higher reactor operating temperatures. One phenomenon that can impact the performance of SFR fuels is fuel cladding chemical interaction (FCCI), which can occur after the fuel has swelled and contacted the cladding. During this process, interdiffusion occurs between the fuel, cladding, and fission products, which can result in the formation of interaction zones on the inner surface of the cladding that can become brittle or that may contain relatively low-melting phases. The result of this process can be cracking and failure of the cladding, and as a result, FCCI could be a limiting factor for long-term operation of the fuel.

Several papers describe the general phenomena that occur during FCCI in irradiated metallic SFR fuels [2-9], but no information is available that describes in great detail the attributes of the interaction layers that can develop at the fuel/cladding interface in a metallic fuel element during irradiation (e.g., specific phase compositions or the diffusion behaviors of specific fuel, fission product, and cladding constituents).

This chapter will report and compare the results of destructive examinations that were performed on fuel elements irradiated in the Experimental Breeder Reactor II (EBR-II) that was successfully operated by Argonne National Laboratory for over 30 years [10]. Operation of this reactor was discontinued in 1994, and since then it has been decommissioned. The characterization of this fuel provides valuable information with regard to general fuel performance in sodium-cooled fast reactors. This chapter was written to describe, in reasonable detail, the development of interaction layers on the inner surface of cladding, based on post-irradiation examinations of specific EBR-II fuel elements. These fuel elements had either U-10 wt% Zr or U-x wt% ($x \leq 26$) Pu-10 wt% Zr as the fuel and the martensitic stainless steel HT9 or the austenitic stainless steel D9 or Type 316 as the cladding. The nominal compositions of the cladding materials are listed in Table 1. In particular, the composition and thickness of the interaction layers are discussed, along with the irradiation conditions, cladding type, and axial location on a fuel element where the thickest interaction

layers can be expected to develop. This chapter is organized to focus on interaction layer development in the irradiated fuel elements according to the fuel composition and cladding type.

Table 1. Nominal Compositions for Stainless Steel Cladding Materials (wt%).^a

	HT9	D9	316
Ni	0.5	15.5	10–14
Cr	12.0	13.5	16–18
Mn	0.2	2.0	2 (max)
Mo	1.0	2.0	2–3
Si	0.25	0.75	1 (max)
Ti	—	0.25	—
C	0.2	0.04	0.08 (max)
W	0.5	—	—
V	0.5	—	—
S	—	—	0.03 (max)
P	—	—	0.045 (max)

a. Balance Fe

2. Result of Post Irradiation Analyses of Fuel Elements

Table 2 summarizes the results that will be discussed in this chapter. The table gives the fuel compositions (all fuel compositions are listed in at% so that correlations can be made with observed phases), the cladding material, the local burnup, the fuel surface temperature at the analysis location, the thickness of the total diffusion layer, the analysis instrument, and the prominent fuel and fission product constituents found in the cladding. Sample analyses have been completed using optical metallography (OM), scanning electron microscopy (SEM), and/or electron probe micro-analysis (EPMA).

2.1. Layers in Fuel Elements with HT9 Cladding

Referring to Table 2, nine irradiated fuel elements clad with the martensitic stainless steel HT9 were analyzed, with particular focus being placed on the fuel-cladding interface. Three elements had U-16 at% Pu-23 at% Zr fuel, while the rest had U-23 at% Zr fuel.

Table 2. Results from Examinations of Fuel Elements Irradiated in EBR-II.

Fuel Element ID	Cladding Material	Fuel Composition (at%)	Burnup (at%)	≈Temp at Fuel/Clad Interface at BOL (°C) ^a	Maximum Zone Thickness Observed (μm)	Type of Analysis	Components Originating in the Fuel that are Found in Cladding Layers
DP-81	HT9	U-23Zr	5.0	660	70	EPMA	Ce, Pr, Nd
DP-11	HT9	U-23Zr	10.0	660	90	EPMA	Ce, Pr, Nd, La, Sm, Pd
DP-04	HT9	U-23Zr	10.0	660	90	EPMA	Ce, Pr, Nd, La, Sm, Pd
DP-70 ^b	HT9	U-23Zr	10.0	660	140	EPMA	Ce, Pr, Nd, La, Sm, Pd
DP-75	HT9	U-23Zr	10.0	660	170	EPMA	Ce, Pr, Nd, La, Sm, Pd
T459	HT9	U-16Pu-23Zr	3.0	ND ^c	10	EPMA	Ce, Pr, Nd
DP-16(1)	HT9	U-16Pu-23Zr	9.7	540	40	SEM	Ce, Nd, La, U, Pu, Pd
DP-16(2)	HT9	U-16Pu-23Zr	10.1	550	40	EPMA	Ce, Nd, Pr, Pu
DP-21	HT9	U-16Pu-23Zr	11.4	ND	ND	EPMA	ND
C709	D9	U-23Zr	9.3	650	111	EPMA	Ce, Nd, La
T225	D9	U-23Zr	10.0	ND	25	OM	ND
T141	D9	U-23Zr	11.9	ND	17	OM	ND
T-159	D9	U-16Pu-23Zr	3.0	ND	ND	EPMA	Ce, Pr, Nd, Pu
^d	D9	U-16Pu-23Zr	6.0	ND	75	EPMA	Ce, Pr, Nd, La
T042	D9	U-7Pu-23Zr	6.0	540	20	EPMA	Ce, Pr, Nd, La
T-087	D9	U-16Pu-23Zr	10.0	ND	50	EPMA	Ce, Pr, Nd, La, Pu
A-850	D9	U-16Pu-23Zr	10.1	550	100	EPMA	Ce, Nd, La, Pu
T-112	D9	U-16Pu-23Zr	11.9	ND	72	EPMA	ND
T-106	D9	U-16Pu-23Zr	17.0	ND	20	EPMA	Ce, Pr, Nd, La
T341	316SS	U-16Pu-23Zr	0.4	ND	ND	EPMA	ND

a. Temperatures calculated for beginning of life (BOL) of fuel elements using the computer code LIFEMETAL.

b. DP-70 and DP-75 were fuel elements run in the reactor beyond breach of the cladding.

c. Not determined.

d. The label for this fuel element was not found.

2.1.1. HT9-Clad Elements with Ternary U-Pu-Zr Alloy Fuel

2.1.1.1. DP-16

Fuel element DP-16 consisted of U-16Pu-23Zr fuel and HT9 cladding and was irradiated to a burnup of 11.3 at%. This fuel element is of particular interest because it was the only fuel element where a sample was sliced from the element and analyzed using SEM/energy dispersive x-ray spectroscopy (EDS), in addition to the more typical OM and EPMA analyses historically performed on fuel elements.

The OM and EPMA analysis for DP-16 was performed on a sample from a position of $x/L=0.78^1$ (with a local burnup of 10.1 at%). The observations at the fuel-cladding interface for this fuel element proved to be representative of what was observed for the other U-16Pu-23Zr/HT9 fuel elements, based on EPMA and OM.

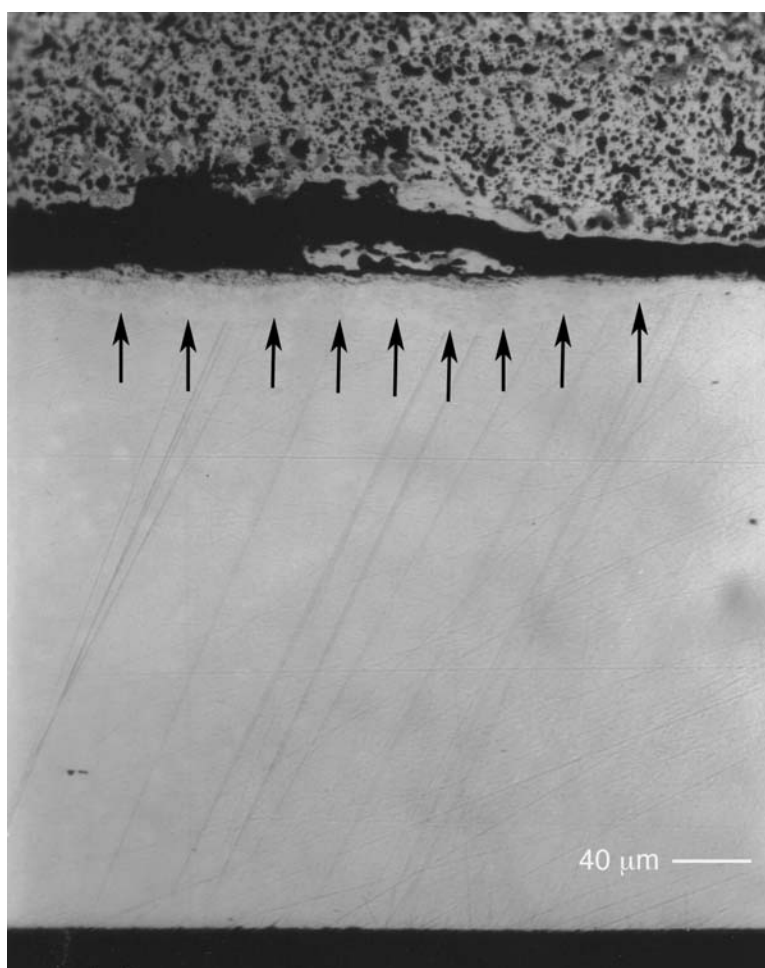


Figure 1. Optical micrograph of typical fuel-cladding interface from DP-16 samples. Note the interaction on the cladding side of the interface (black arrows).

¹ x/L refers to the radial position from the bottom of the fuel element, x , divided by the total length of the fuel element, L .

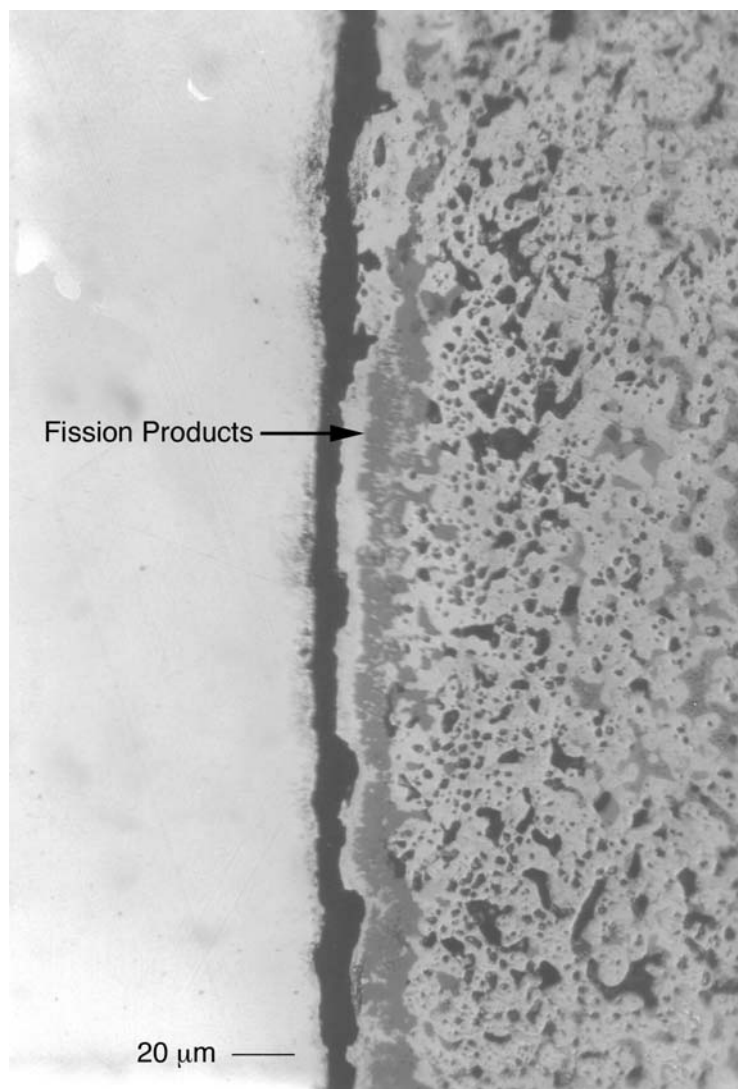


Figure 2. Optical micrograph showing the accumulation of fission product phases at the fuel-cladding interface.

Figure 1 shows an optical micrograph of a small region of the fuel-cladding interface in fuel element DP-16. Interdiffusion layers are observed on the inner-surface of the cladding. Another optical metallograph is presented in Figure 2 that shows some of the fission product phases that can be found in regions of the fuel near the cladding.

Figure 3 shows a specimen current image and x-ray maps, produced using EPMA, of a region in the vicinity of the fuel-cladding interface. X-ray maps were generated for the fuel constituents U, Pu, and Zr; the cladding components Fe and Cr; and the fission products Ce, Nd, Pd, and Ru. The x-ray maps for Cr and Ru are not shown because Cr did not appear to diffuse into the fuel (though the Cr concentration was elevated in the immediate vicinity of the fuel-cladding interface), and Ru traced the Pu and is observed in the same localized regions of the fuel, but in slightly lower amounts. Based on the x-ray maps shown in Figure 3,

Fe diffuses into the fuel, Ce and Nd diffuse into the cladding, and Pd enriches in localized areas of the fuel near the fuel-cladding interface.

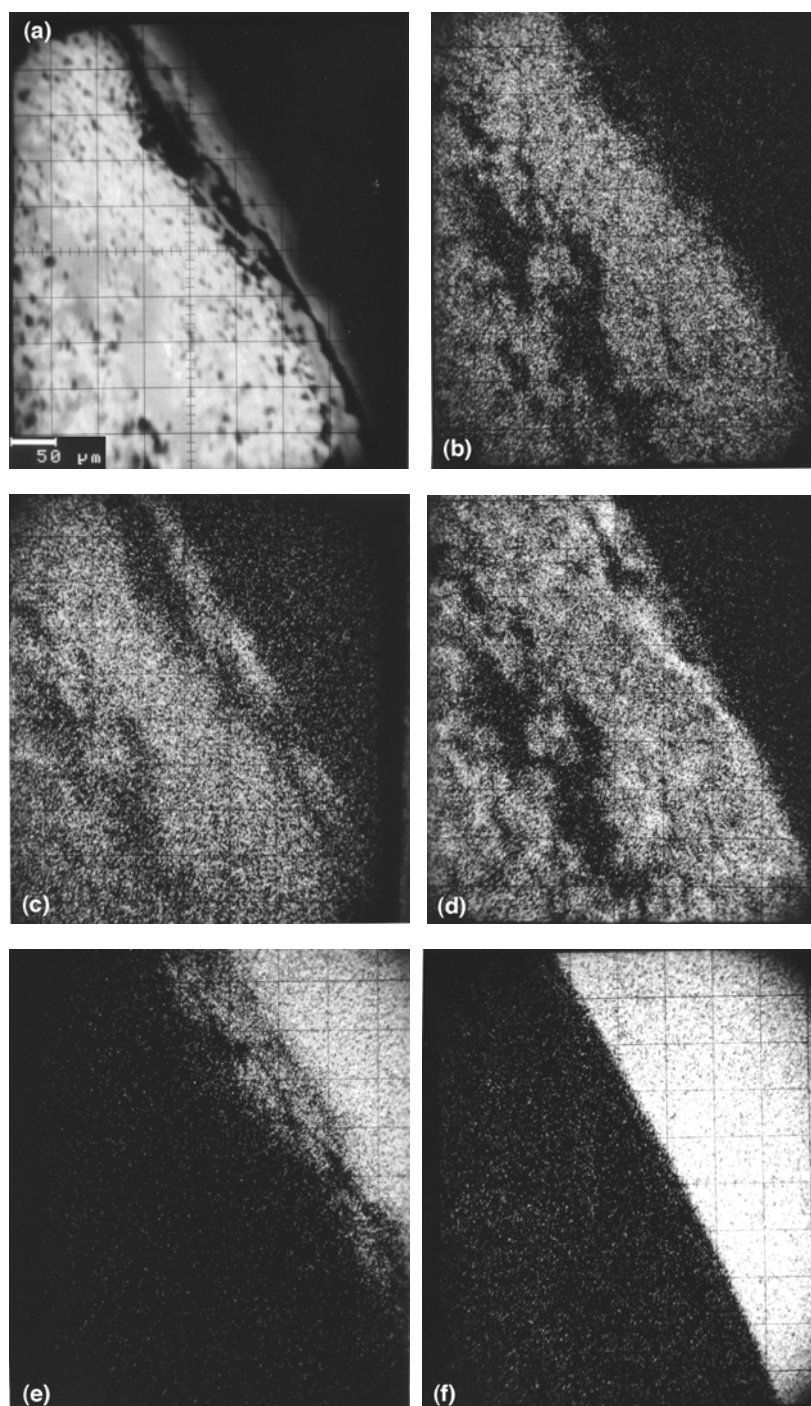


Figure 3. Continued on next page.

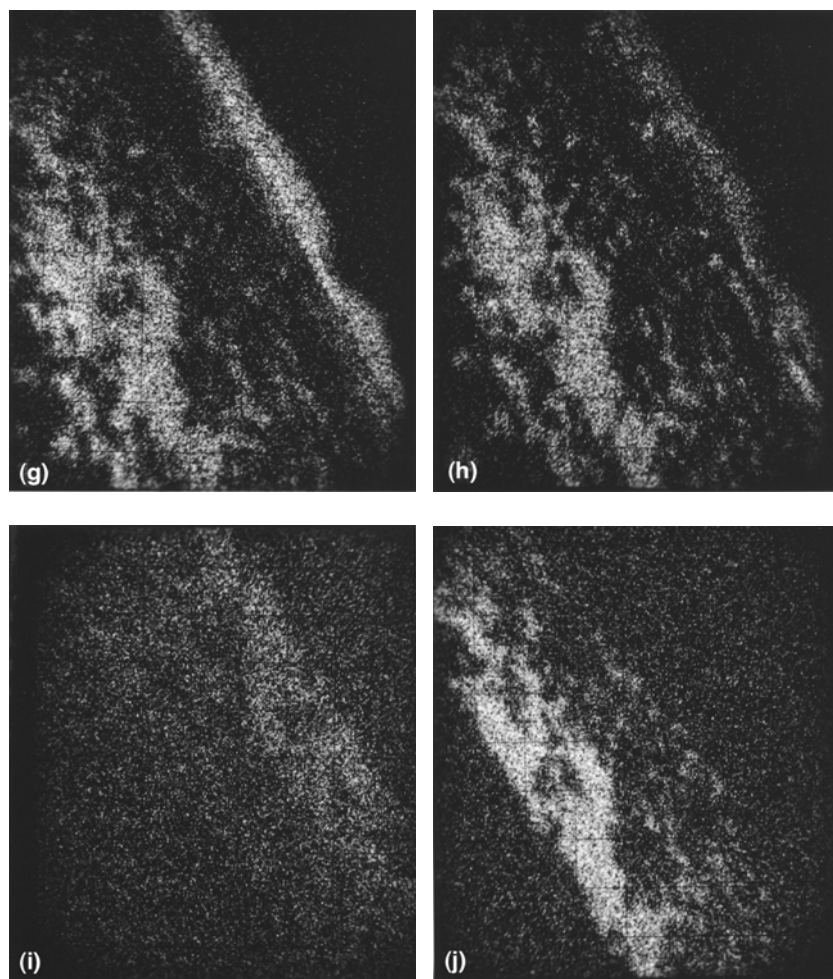


Figure 3. (a) Electron microprobe specimen current image and x-ray maps for (b) U, (c) Pu, (d) Zr, (e) Fe, (f) Cr, (g) Ce, (h) Nd, (i) Ru, and (j) Pd.

Figure 4 shows an EPMA elemental trace taken at the fuel-cladding interface in DP-16. The approximate original cladding boundary corresponds to where the Cr concentration drops to approximately zero. The elemental trace shows the same behavior for Fe, Cr, U, Pu, Zr, Ce, and Nd compared to what is observed with x-ray maps. The lanthanides penetrated the furthest into the cladding (approximately 40 μm) of any components originating in the fuel.

SEM/EDS analysis was completed on a sample from DP-16 from axial elevation $x/L=0.84$. The local burnup was determined to be 9.7 at%, and the peak inner-cladding temperature was 542 C. The sample is shown in Figure 5; to minimize dead time during EDS analysis and exposure to the machine operator, only a small section of cladding (approximately 20% of the size of the original) and adventitious fuel were analyzed. Analyses were completed for the left (A) and right (B) sides of the boxed area of the sample shown in Figure 5. Figure 6 shows higher magnification SEM secondary and backscattered electron micrographs of interaction layers from the area on the left that was analyzed. Individual layers can be observed in this figure, and they have been labeled 1-4.

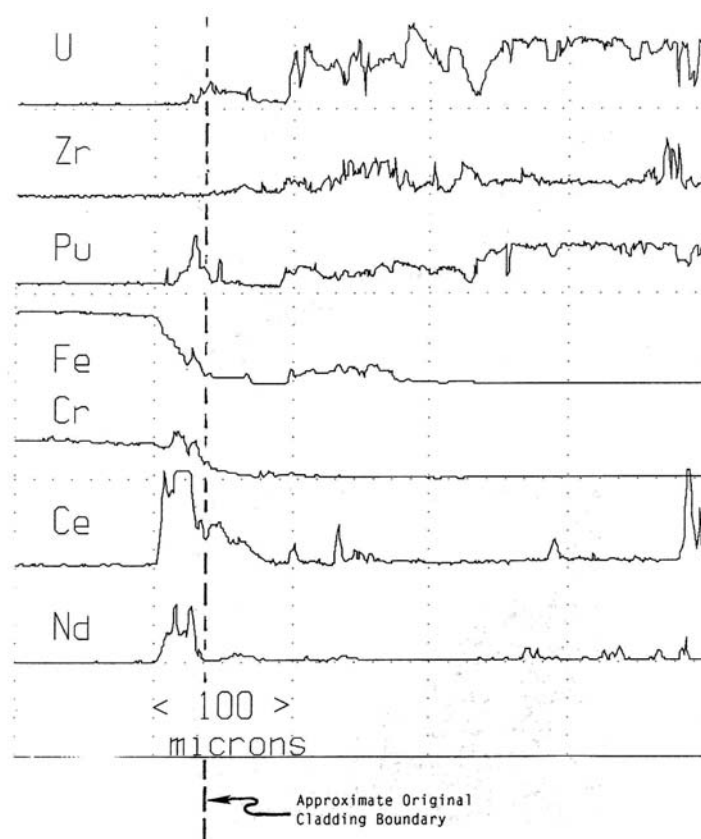


Figure 4. Relative EPMA concentration profiles for U, Zr, Pu, Fe, Cr, Ce, and Nd across the fuel-cladding interface for DP-16.

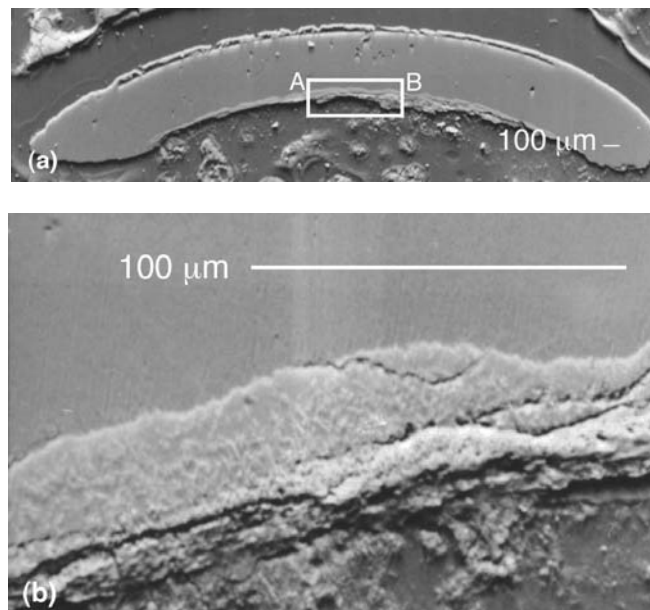


Figure 5. Continued on next page.

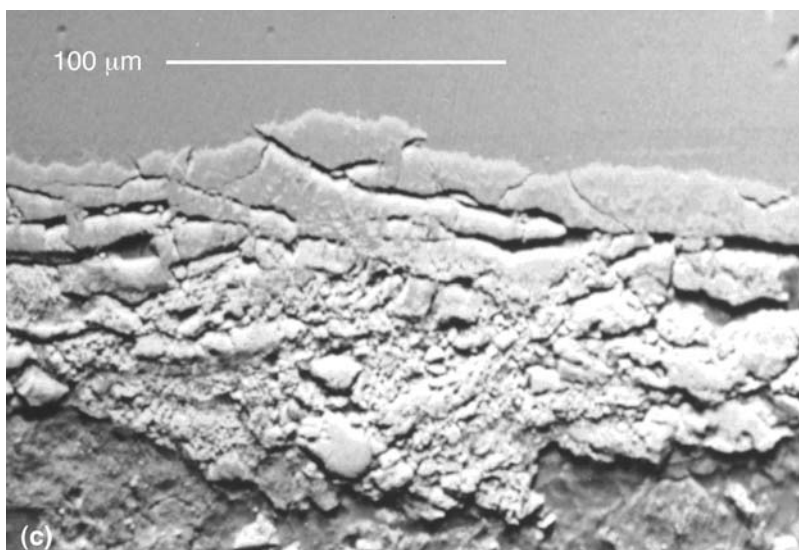


Figure 5. Secondary electron micrographs of a cladding sample taken from DP-16. Approximately 20% of the cladding remains from the original sample. Prior to sample preparation, the fuel had been attached to the bottom of the sample. (a) is a low magnification image of the sample, and the outlined box identifies the region where subsequent analyses were focused, specifically at locations A and B. (b) shows a higher magnification image of region A and (c) shows region B.

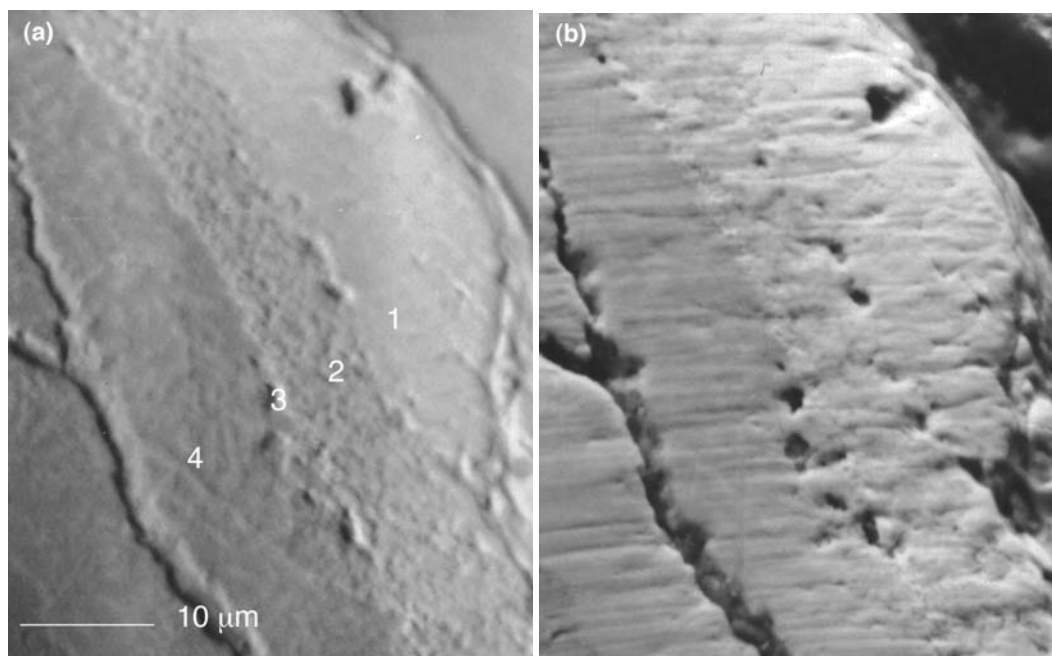


Figure 6. Backscattered electron (a) and secondary electron (b) images of the interaction layers observed in location A for the sample taken from DP-16. The lower left of each image is un-reacted cladding, and the upper right is mount material.

Table 3 enumerates the approximate compositions of the four phase layers, based on point-to-point SEM/EDS measurements taken in the various phase layers at location A.

Table 3. Approximate Compositions of Spot Readings Taken in Different Regions of the Phase Layers at Location A of DP-16 SEM Sample (in at%).^a

Layer	Fe	Cr	Si	Pd	Ce ^b	Nd	Zr	Pu	Other
1	2	2	0	24	16	34	19	1	—
1	1	1	5	35	19	33	5	1	—
1	—	—	5	38	13	26	7	2	9La
1	—	—	—	20	10	16	31	—	6La, 17Pb
2	—	34	6	—	11	23	—	—	3Mo, 6La, 8U, 9Sn
2	2	28	7	—	16	28	—	—	La, 7U, 1Ag, 10Sn
3	28	20	5	15	10	12	—	11	—
4	63	10	4	—	7	13	—	0.3	1La
4	66	15	6	—	5	7	—	0.2	1Ag
4	67	13	6	—	5	7	—	0.2	1Ag
4	69	16	4	—	5	5	—	0.3	1Ag

a. Nominal un-reacted cladding had a composition of 84Fe-12Cr-2Si-1Mn-0.4Ni for the components listed in Table 3 (determined using SEM/EDS).

b. It is difficult to resolve lanthanides using SEM/EDS due to peak overlap. As a result, the numbers given for Ce and Nd should be taken as qualitative numbers, and in some cases La may be present with the Ce and Nd (based on EPMA x-ray maps).

A composition profile through layers 1–4 is shown in Figure 7 where spot readings were matched with a distance through the interaction zone (some constituent concentrations are grouped together). Layer 1 appears to consist of two phases and it contains the only Zr found in the sample and appreciable Pd. Only trace amounts of Fe and Cr were observed in Layer 1, and relatively high levels of lanthanide fission products were observed (approximately 50 at% over the width of the layer).

Layer 2 contained lanthanides and relatively high levels of Cr. A relatively high level of U was observed in this layer as well, which agrees with EPMA data that indicates U penetrates into the cladding. The detection of Sn in layer 2 seems believable and has been recorded in other samples analyzed with EPMA.

Layer 3 was a very thin layer (approximately 1- μ m-wide), and as a result was hard to differentiate from layer 2 (see Figure 6). Composition differences were detected between layers 2 and 3. In addition, 15 at% Pd, 28 at% Fe, and 11 at% Pu concentrations were detected in layer 3. The fact that layer 2 had negligible levels of Fe, Pd, and Pu while notable concentrations of these elements are observed in layer 3 demonstrates that layer 3 is an actual phase layer. The Pu present in layer 3 is evidence of the furthest penetration of any non-fission product fuel constituent.

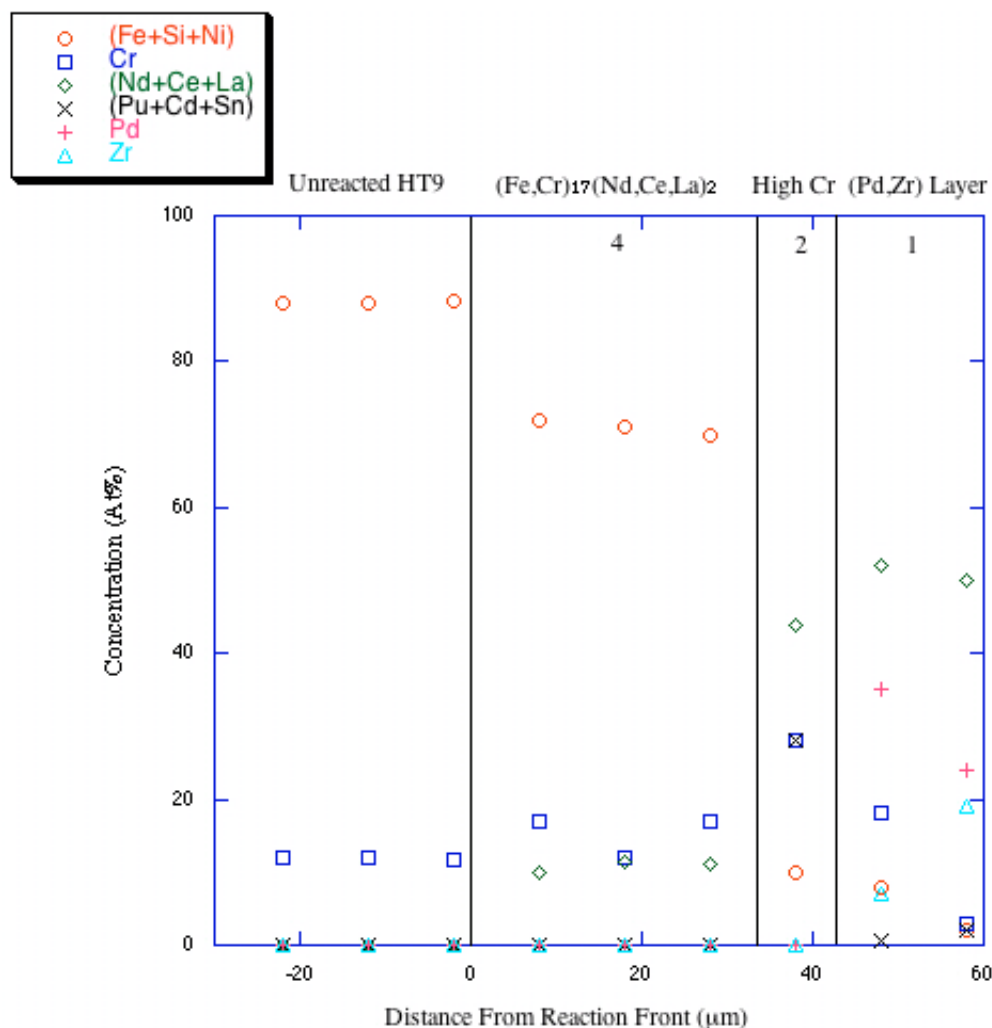


Figure 7. Measured compositions as a function of linear position in location A. The values are compiled in Table 3. The numbers at the top correspond to the layer numbers in Table 3.

Layer 4 was the widest layer (20–28 μm) at location A. A small second phase was observed dispersed throughout this layer. The compositions reported in Table 3 for this layer correspond to a combination of the dispersed and matrix phases. Ten to twelve at% of lanthanides is observed in layer 4. The reason why this layer may be associated with the $(\text{Fe,Cr,Si})_{17}(\text{Nd,Ce,La})_2$ phase will be discussed in Section 3.

The second region of the sample where composition information was determined for the layers was from the right-side of the boxed area of the sample shown in Figure 5 (location B). An SEM micrograph of the analyzed area is presented in Figure 8. The layers were labeled 1–4, and layer 4 had a composition similar to that of the phase labeled layer 4 at location A. The compositions observed for spot readings taken from layers 1–4 are depicted in Table 4.

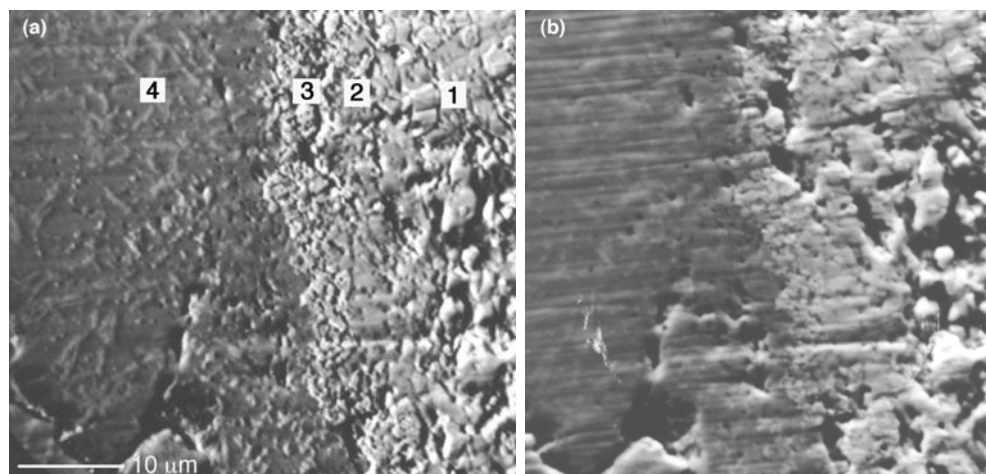


Figure 8. Backscattered electron (a) and secondary electron (b) images of the interaction layers observed in location B for the sample taken from DP-16. The layer shown in the far left of each image is the same as layer 4 in Figure 6.

Table 4. Approximate Compositions of Spot Readings of Phase Layers at Location B of DP-16 SEM Sample (in at%).

Layer	Fe	Cr	Mo	Si	Pu	U	Ce	Nd	La	Other
1	41	—	—	2	15	—	10	21	4	7Pd
1	66	2	—	7	22	—	2	2	2	1Ni, 1Ba
1	55	14	0.4	2	3	1	7	15	2	0.4Ni
2	—	28	1	—	5	4	15	39	6	—
3	37	39	2	2	8	2	5	3	1	0.5Mn
4	69	17	—	1	0.2	—	5	6	—	1Mn, 0.4Ni

Layer 1 appeared to contain different types of fine phases and was not a well-defined layer. The first measurement in Table 3 for layer 1 is for a representative area near layer 2. The phase or phases in this area contain 35 at% lanthanides, 41 at% Fe, 7 at% Pd, and 15 at% Pu. Two other areas where spot readings were taken had different compositions. The second measurement listed in Table 3 was for a small particle (approximately 5 μm in diameter) embedded deeper into the fuel, and the final measurement in layer 1 was taken at the deepest location in the fuel available, and it contained more lanthanides and less Pu than the second measurement.

Layer 2 contained approximately 60 at% lanthanides, 28 at% Cr, 9 at% U and Pu and was similar in composition and size to the layer 2 shown in Figure 6. The major difference between the two layers was that the layer 2 in Figure 6 contained Cd and Sn, while layer 2 in Figure 8 contained U and Pu. If the supposed Cd observed at location B is, in fact, mainly U (possibly due to overlap of the EDS peaks), then the layers at both locations are even more similar.

Layer 3 was approximately 1- μm -wide and contained relatively high amounts of Cr (39 at%) and actinides (10 at% U and Pu). For this layer 3, the Fe content was higher and the lanthanide content was lower if compared to the layer 3 in Figure 6.

The layer nearest the un-reacted cladding, layer 4 in Figure 8, consisted of two-phases, as did the layer 4 in Figure 6. Yet, in Figure 8 the precipitate phase appeared slightly larger in layer 4 than it did in the layer 4 in Figure 6. As a result, attempts were made to determine the concentration of the precipitate phase. The composition listed in Table 3 for layer 4 is the average composition for both the matrix and precipitate phase. SEM/EDS analysis of the precipitate phase suggested that the precipitate phase was lower in Fe (67 at% compared to 72 at%) and Cr (12 at% compared to 17 at%) and had about twice as much lanthanides (15 at% compared to 8 at%) compared to the matrix phase.

2.1.1.2. DP-21

DP-21 is another HT9 clad fuel element with U-Pu-Zr alloy fuel that is of particular interest because of its relatively high burnup of approximately 11.4 at%. DP-21 was run as part of the same fuel assembly as DP-16. It exhibited the same major features as were described for DP-16. The buildup of lanthanides at the fuel-cladding interface was noticeable, and these lanthanides combined with Fe and Ni from the cladding to form phases. The main features of the fuel-cladding interaction zones observed in DP-16 and DP-21 are representative of the smaller amounts of fuel-cladding interaction observed for fuel element T459—a fuel element irradiated to only 3 at%.

2.1.2. HT9-Clad Elements with Binary U-Zr Alloy Fuel

2.1.2.1. DP-11

DP-11 was a fuel element with U-23 at% Zr fuel that was destructively analyzed to investigate the interaction between the fuel and cladding. This fuel element was irradiated to about 10 at% burnup at elevated cladding temperatures (630–660 C beginning-of-life temperature). A specimen was taken from an axial position 14.75 inches from the bottom of the pin and analyzed using EPMA. Figure 9 shows an elemental trace through a region of fuel-cladding interaction zone. The vertical scale in this trace is uncorrected x-ray intensity, and the x scale is positioned (the cladding is to the left) with the cladding inner diameter location being taken as zero. At this particular location, there is a 50- μm -wide zone of the cladding that is high in Cr and low in Fe and Ni. Ni is also depleted from the cladding another 40 μm into the cladding over what is observed for the Fe. In the fuel, Fe has interdiffused around 250 μm , and the Ni interdiffused around 20 μm . In the 50- μm -wide band in the cladding containing high Cr and low Fe and Ni, lanthanide concentrations are elevated. Ce and Nd are the principal elements in this layer, and lower levels of La, Pr, and Sm are also observed. A possible presence of Pd in the fuel-cladding interaction zone is observed, based on x-ray maps. Figure 10 shows typical x-ray maps taken to identify the locations of the various components at the fuel-cladding interface.

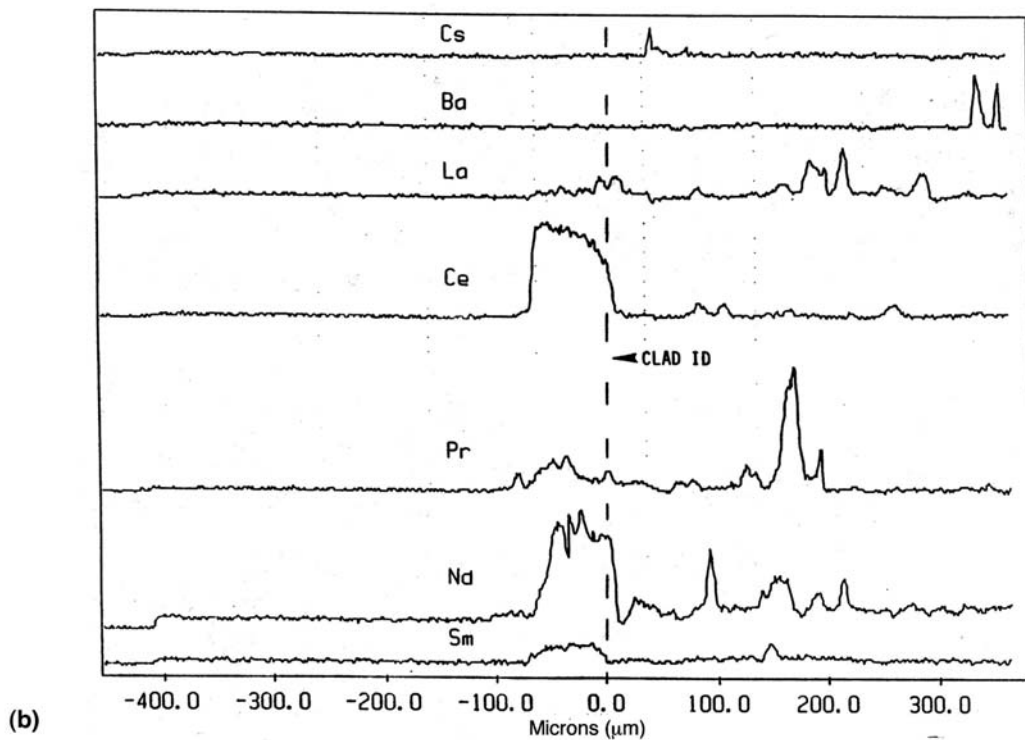
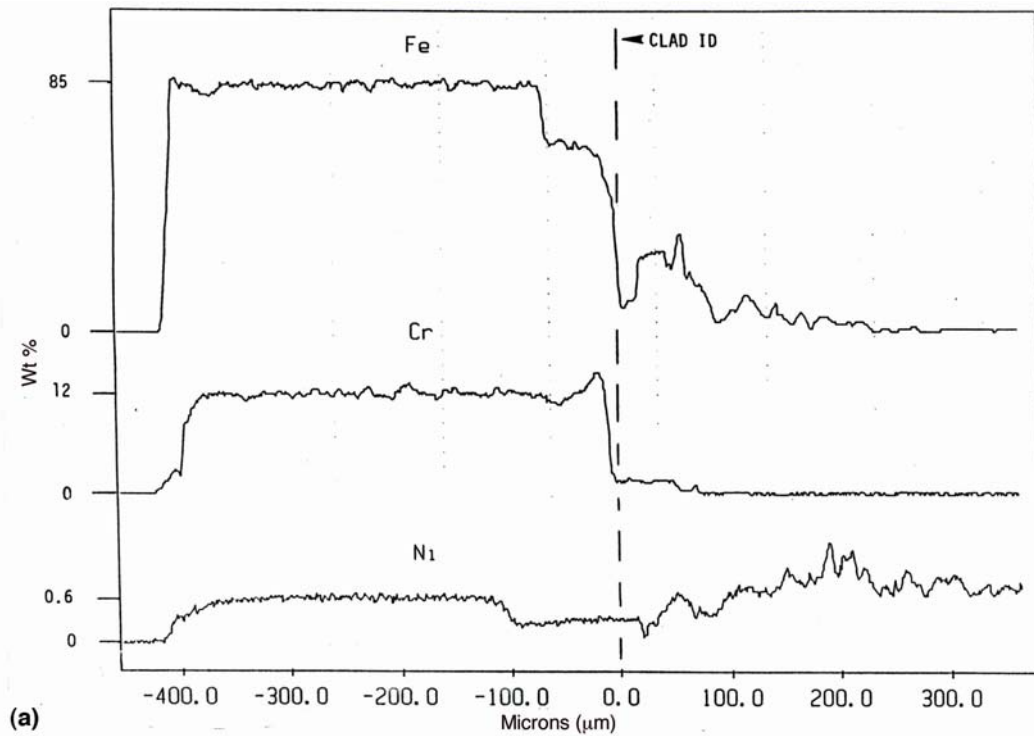
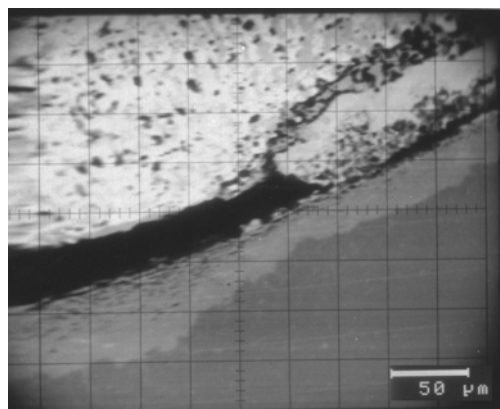
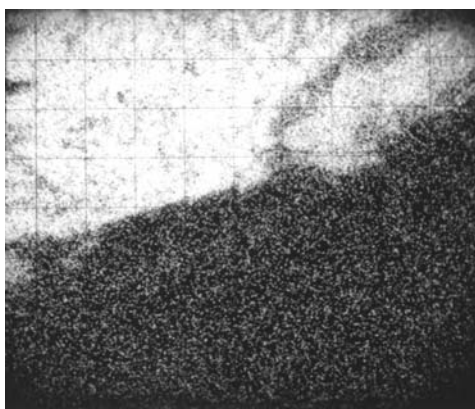


Figure 9. Measured compositions as a function of linear position in location A. (a) shows elemental traces for cladding constituents, and (b) shows traces for the fission products.



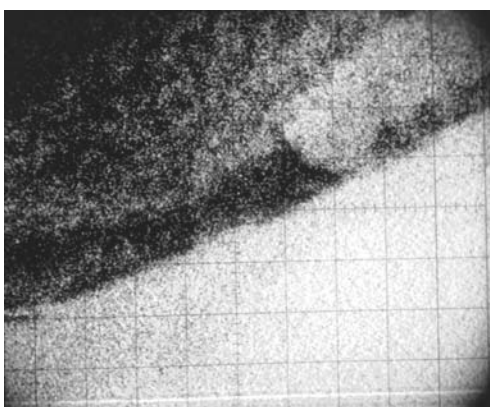
(a)



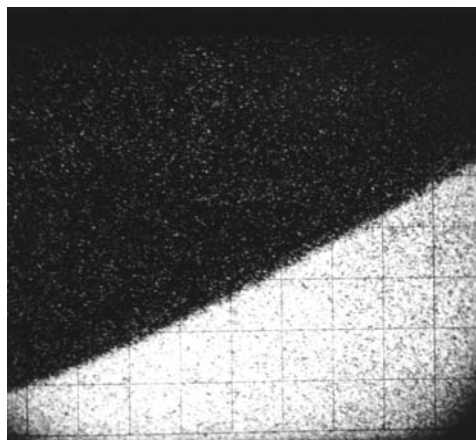
(b)



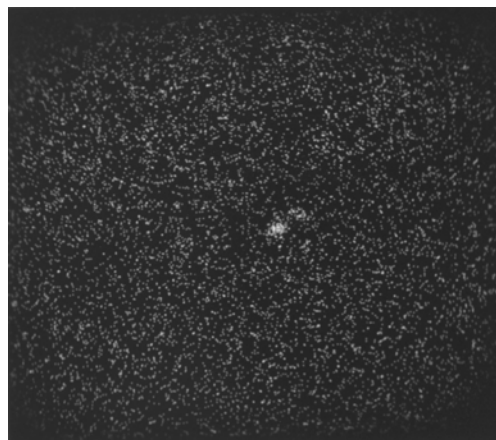
(c)



(d)



(e)



(f)

Figure 10. Continued on next page.

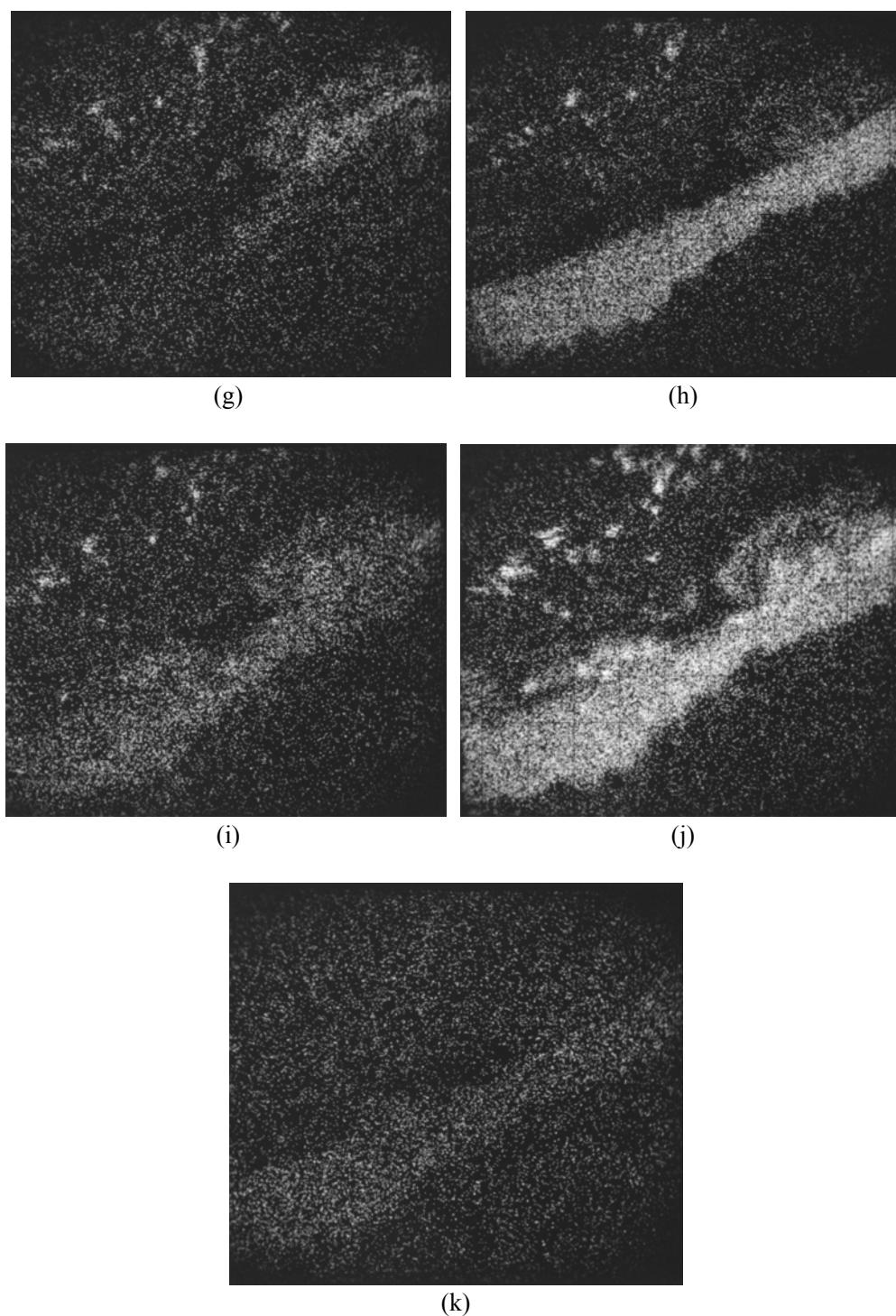


Figure 10. (a) Electron microprobe specimen current image and x-ray maps for (b) U, (c) Zr, (d) Fe, (e) Cr, (f) Ba, (g) La, (h) Ce, (i) Pr, (j) Nd, and (k) Sm.

2.1.2.2. DP-81

Two specimens were examined from fuel element DP-81, an element with U-23Zr fuel and HT9 cladding and irradiated to 5 at% burnup. Samples were cut from positions 0.75 inches and 3.125 inches from the top of the fuel element. Fuel-cladding interaction was observed in the sample taken at 0.75 inches but not in the sample taken at 3.125 inches from the top of the element. Based on the praseodymium penetration into the cladding, the fuel-cladding interaction zone is 70 μm wide. An 80- μm -wide band of the cladding was Ni-depleted. Fission product constituents that penetrated into the cladding included Ce, Pr, Nd, La, and Sm. Besides a high atomic number band, a band was also detected where grain boundary penetration of fission products into the cladding had apparently occurred.

2.1.2.3. Breached Fuel Elements

Results from examinations of fuel elements that were kept in the reactor after failure of the cladding have been reported [2]. Failed elements DP-75 and DP-70 (U-10 wt% Zr fuel and HT9 cladding) were analyzed and compared to un-failed elements DP-04 and DP-11, which had the same fuel and cladding. Brittle, non-uniform bands of lanthanides were observed to penetrate up to 170 μm into the cladding in DP-75, and up to 140 μm into the cladding in DP-70 (in the hottest regions of the fuel elements). A maximum of 90 μm of lanthanide penetration was observed in the un-failed elements DP-04 and DP-11. The breached fuel elements were run to 10 at% burnup, had a peak calculated cladding temperature near 660 C, and had up to 45% of the total cladding thickness of 0.584 mm consumed by interaction layers. The un-breached fuel elements exposed to the same conditions had only 20% of the cladding consumed by layers. Carbon depletion zones (up to approximately 60 μm thick) were observed on the cladding side of the interaction layers.

2.2. Layers in Fuel Elements with D9 or Type 316 Stainless Steel Cladding

D9 and Type 316 stainless steel are austenitic steels that contain appreciable Ni. Those fuel elements clad with these materials imparted slight differences in their observed fuel-cladding interaction compared to the elements clad with the martensitic (low Ni) stainless steel HT9. The fuel element T341 was the only element with Type 316 stainless steel cladding that was analyzed to investigate fuel-cladding interaction. Due to its low burnup of 0.4 at%, negligible fuel-cladding interaction was observed. The fuel elements discussed in the context of fuel-cladding interaction will be the ones clad with D9 stainless steel. The major differences between D9 and Type 316 stainless steel are that D9 contains more Ni, less Cr, and it contains Ti (see Table 1).

2.2.1. D9-Clad Elements with Ternary U-Pu-Zr Alloy Fuel

2.2.1.1. T-106

Fuel element T-106 had U-16Pu-23Zr fuel and D9 cladding and was irradiated to 17 at% burnup. The EPMA analysis of T-106 investigated the development of fuel-cladding

interaction layers, along with the distribution of noble metal fission products in discrete phases in the outer periphery of the fuel at two different locations in a longitudinally-cut sample. Presented in Figure 11 is an optical micrograph of the fission product-containing phases that were observed at one location in T-106. One observed phase was a “blocky” particle observed throughout the fuel. X-ray maps taken of the T-106 specimen showed that Zr, Ru, Pd, and Ni comprised these particles. Another type of observed phase was an irregular-shaped, gray phase that developed in the outer regions of the fuel. Light and dark regions, which indicate differences in composition, were observed in this type of phase, and Table 5 enumerates some measured compositions of light and dark phase regions. Compositions varied widely from point to point in both the light and dark gray phases. Small particles containing Ba and Te were also observed along with the light and dark gray phases.

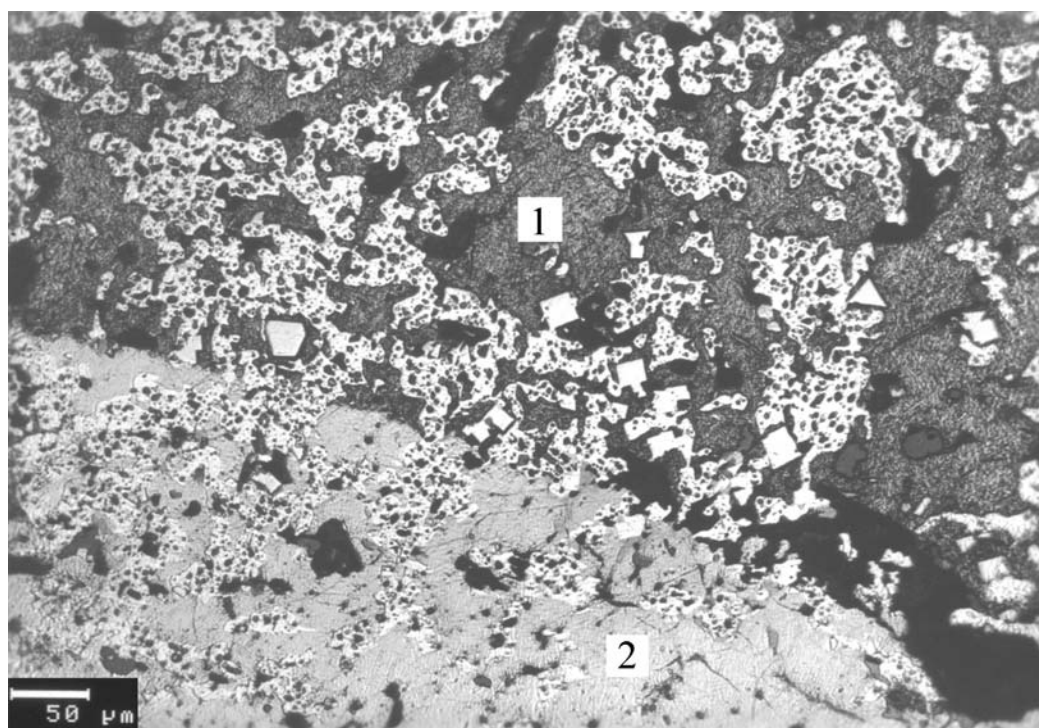


Figure 11. Optical micrograph of an area near the fuel-cladding interface where dark-gray precipitates (1) and light-gray precipitates (2) are observed. The blocky particles are enriched in Zr, Ru, Pd, and Ni.

Table 5. Approximate Compositions of Light and Dark Regions of Noble Metal-Containing Phases in a Sample From Fuel Element T-106 (in at%).

Region	U	Pu	Zr	Pd	La	Ce	Pr	Nd
Light Gray	1.2	1.9	0.3	20.2	23.7	17.6	3.8	31.4
Dark Gray	0	3.8	0	0	27.7	29.2	3.9	34.1

At another location in the fuel (see Figure 12), blocky particles were observed and analyzed. These particles contained Zr, Mo, Tc, Ru, Rh, and Pd, based on x-ray maps. Nickel,

which was observed in similar blocky particles in another area of the fuel, was not identified in these particles.

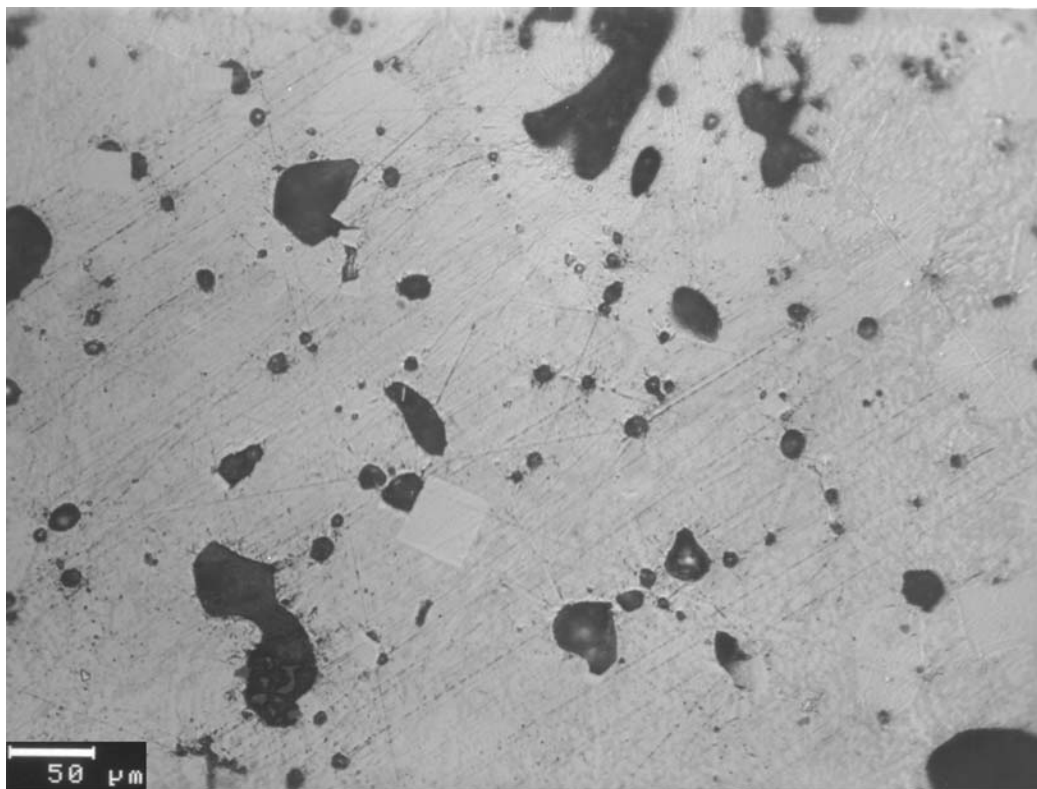


Figure 12. Optical micrograph showing the presence of blocky particles near the fuel-cladding interface.

The fuel-cladding interaction zone in T-106 was a fairly continuous band around the inner diameter of the cladding. About 20 μm of interaction was observed in the analyzed sample, which consisted of two layers. X-ray maps for the interaction zone showed that the Fe and Cr concentration levels are slightly enriched in the interaction zone, and the Ni is depleted. The interaction zone contained Ce, La, Pr, Nd, Pd, and Sn. Evidence of minor amounts of Pu in the interaction zone was observed as well.

2.2.1.2. T-112

Fuel element T-112 had U-16Pu-23Zr fuel and D9 cladding and was irradiated to 11.9 at% burnup. Metallographic specimens were taken from this element at locations 3.5, 7.5, 13.5, and 14.25 inches from the top of the element. The specimen taken from the part of the pin exposed to the highest temperature (14.25 inches) exhibited the most fuel-cladding interaction (approximately 70 μm). Figure 13 shows the observed interaction layers in this region of the pin, along with the indentation marks left behind from hardness testing that was performed on the sample. The interaction zone and un-reacted cladding exhibited hardness values of approximately 1,100 and 350 DPH, respectively. The interaction layers observed in the

cladding of a sample taken from a position of 13.5 inches are presented in Figure 14 (also note the light and dark contrasting fission product phases that can be found in the outer periphery of the fuel). In Figure 15, the fuel-cladding interface at an axial position of 3.5 is shown. An intact layer of Zr-rind, which is a thin layer on the outer surface of the fuel alloy that is produced during the injection casting of fuel using quartz molds, is apparent along with dark-contrast fission product phases that exist at the outer periphery of the fuel. No fuel-cladding interaction is observed where the Zr-rind is intact.

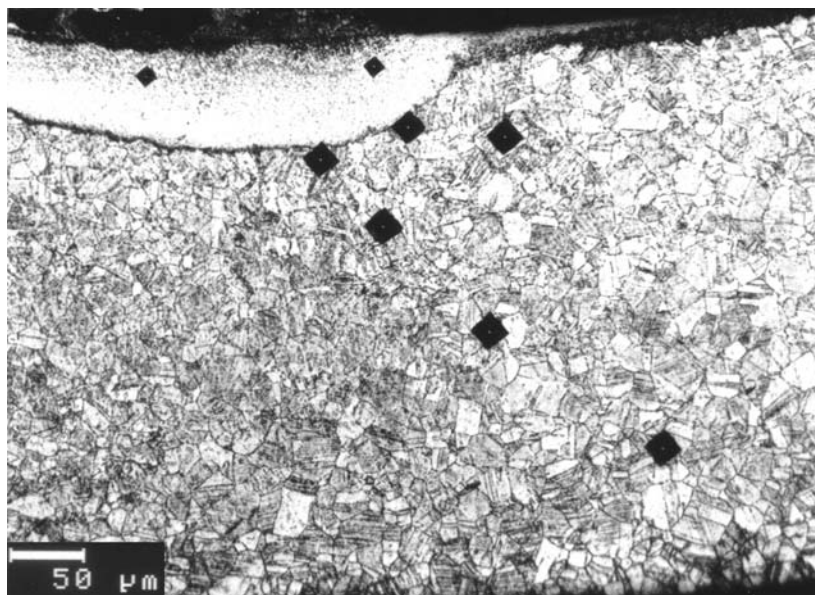


Figure 13. Optical micrograph showing the presence of an interaction zone at the fuel-cladding interface. Hardness indents reveal the increased hardness of the interaction zone vis-à-vis the un-reacted cladding.

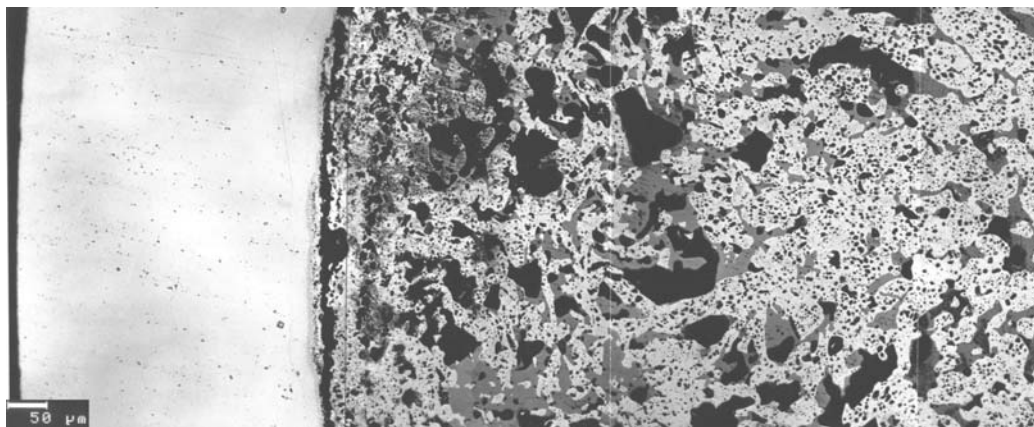


Figure 14. Optical micrograph showing the presence of a fuel-cladding interaction zone and large quantities of fission products filling voids near the cladding.

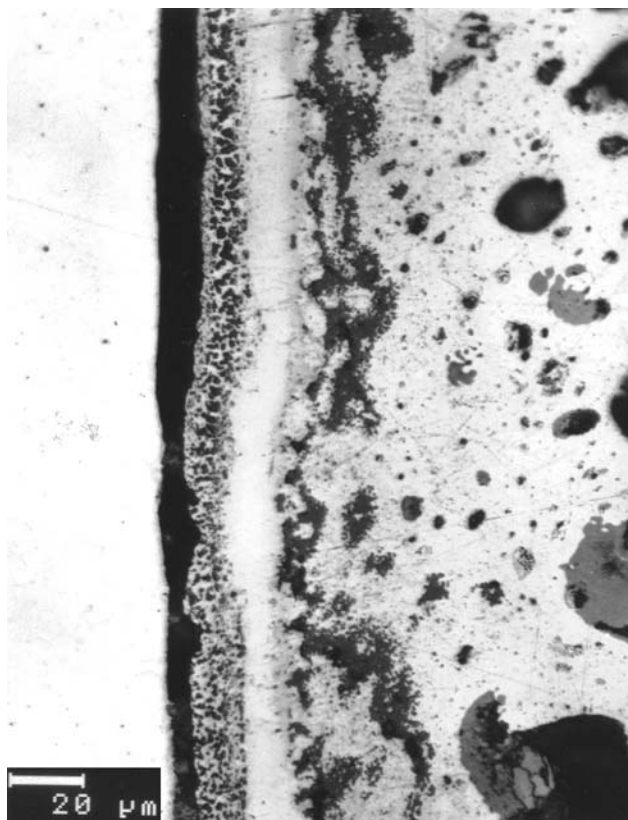


Figure 15. Optical micrograph showing the fuel-cladding interface near an intact Zr-rind layer.

2.2.1.3. T-087

EPMA was performed on the D9-clad fuel element T-087, which had U-16Pu-23Zr fuel and was irradiated to 10 at% burnup. In addition to the original fuel and cladding components, the fission products Tc, Pd, Cs, Ba, La, Ce, Pr, Nd, Sm, Eu, and Gd were detected in the fuel. A narrow reaction band was identified on the cladding side of the fuel-cladding interface. The band proved to have elevated concentration levels of Zr, Ce, Pr, Nd, La, and Sm, and was heavily depleted in Ni and partially depleted in Fe from the levels noted in the original unreacted cladding. On the fuel side of the fuel-cladding interface, discrete, globular, lanthanide-rich phases were detected. Some of these phases had relatively high levels of Pd.

2.2.1.4. T-159

EPMA of this fuel element (U-16Pu-23Zr fuel with D9 clad) showed that, even at a low burnup of 3 at%, redistribution of fuel and fission product constituents results in elevated levels of U, Pu, Zr, and fission products at the fuel-cladding interface. Little evidence of actual fuel-cladding interaction was observed in this fuel element.

2.2.1.5. A-850

Fuel element A-850 consisted of U-16Pu-23Zr fuel and D9 cladding and was irradiated to 11.3 at% burnup. A total of seven samples were analyzed from this as-irradiated pin, and in Table 6 the maximum cladding wastage observed for each specimen is correlated with the axial position (x/L), the local burnup, and the cladding ID temperature. Typically, the largest interaction zones in this element were observed where a wedge-shaped crack had formed in the fuel adjacent to the inner surface of the cladding and a large fission product deposit (primarily Nd, Ce, and La) had formed (see Figure 16).

Table 6. As-irradiated data for samples from fuel element A-850.

Axial Position (x/L)	Local Burnup (at%)	Linear Power (kW/ft)	Cladding ID Temperature ($^{\circ}\text{C}$)	Maximum Interaction Zone Thickness (μm)
0.5	11.4	15.1	518	47
0.6	11.1	14.8	532	75
0.7	10.6	14.2	543	126
0.78	10.1	13.5	552	100
0.9	9.1	12.1	562	90
0.95	8.7	11.5	566	103
1.0	8.2	10.9	570	75

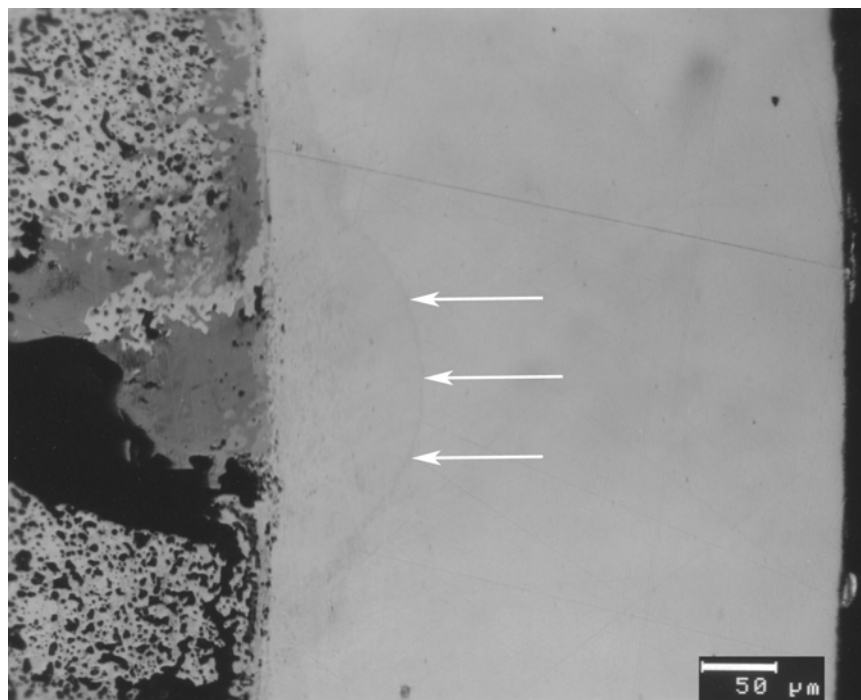


Figure 16. Optical micrograph showing fuel-cladding interaction (white arrows) adjacent to a large deposit of lanthanide fission products.

For a sample taken at $x/L=0.78$, three different areas were analyzed at the fuel-cladding interface. The first area that was analyzed was near a wedge-shaped crack in the fuel that had been filled in with fuel. The second area contained a thin band (about 40 μm thick) of lanthanides compressed between the fuel and the cladding. The last location was near a fuel crack, similar to that shown in Figure 16. The bulk of the Fe diffusion was observed in the first area analyzed. At the wedge-shaped crack location, Fe penetrated into the fuel to a maximum depth of about 175 μm , and the concentration of Fe in local spots in the fuel was as high as 25 wt%. Penetration of Fe into the fuel was not observed in the other two analyzed areas.

Nickel interdiffusion was observed in all three analyzed locations (approximately 180–200 μm into the fuel) leaving a Ni-depleted layer in the cladding; the Ni concentrations varied between 6 and 12 wt% in the fuel. Zirconium and Ni appeared to collocate in the fuel, especially when adjacent to the cladding. With regards to Pu behavior, the high concentrations of Ni corresponded to local depletions of Pu in the fuel, while in the cladding, elevated levels of Pu were observed in the Ni-depleted region. The highest amount of Pu interdiffusion was observed in the first area analyzed. This area had the highest levels of Fe depletion from the cladding and the most elevated concentration levels for Pu in the cladding. The lanthanide interdiffusion behavior appeared to mirror that of the Pu, and the lanthanides were found to interdiffuse to similar depths in the cladding. Cerium and Nd were the most abundant lanthanides at all three locations, and La was the only other fission product observed in a measurable concentration. Uranium and Zr did not penetrate into the cladding at any of the locations analyzed. The maximum lanthanide penetration was seen where lanthanide phases appeared to be in direct contact with the cladding.

As was the case for fuel element DP-16 (an HT9 clad element), two differing contrast phases were observed in the vicinity of the fuel-cladding interface in element A-850. These phases contained differing amounts of lanthanides; the lightest phase also contained Pd and Ru. For the sample sliced at an x/L position of 0.6, the boundary between the light and dark phases was seen around 200 μm from the fuel-cladding interface. For x/L positions of 0.7 and 0.95, the boundary was observed 170 and 150 μm , respectively, from the interface. The 0.95 position was the highest temperature region of the pin, and this is where the light and dark phases seemed to be closest to the cladding. Therefore, the local temperature in a fuel element appears to control how close the light and dark phases will be observed in relation to the fuel-cladding interface.

2.2.1.6. T-042

For the sample from element T-042 (which was irradiated to 6 at% burnup), approximately 20 μm of interaction was observed along with the presence of lanthanide fission products in the interaction zone. T-042 was the only element analyzed that had a fuel alloy with a composition of U-7Pu-23Zr. Another fuel element that was analyzed after 6 at% burnup had U-16Pu-23Zr fuel (no identifying number for this element could be found); the maximum amount of interaction equaled 75 μm . Lanthanide fission products were observed in the interaction zone along with evidence of Ni and Fe depletion. The Ni was depleted to around 1 wt% from the 16 wt% that existed in the original cladding, and the Fe was depleted from 67 wt% to 59 wt%.

2.2.2. D9-Clad Elements with Binary U-Zr Alloy Fuel

2.2.2.1. T-141

In fuel element T-141 (U-23Zr fuel, D9 cladding, 11.9 at% burnup) the original Zr-rind from the injection casting of the fuel provided protection from fuel-cladding interaction. As shown in Figure 17, interaction layers were only observed in the cladding in areas where the Zr-rind had failed. The maximum interaction layer thickness (25 μm) was observed in the sample taken from the hottest region of the pin (an axial position of 14.25). This area of maximum interaction layer formation is presented in Figure 18. Near the cladding, fission product-containing phases were observed, which may be due to the high amounts of fuel porosity that was observed near the cladding. As discussed in Section 2.2.1.2, T-112, a fuel element with U-16Pu-23Zr fuel and D9 cladding, was also irradiated to 11.9 at% burnup (as was T141). However, T-141 had a smaller interaction zone, which may be correlated to its binary U-23Zr fuel.

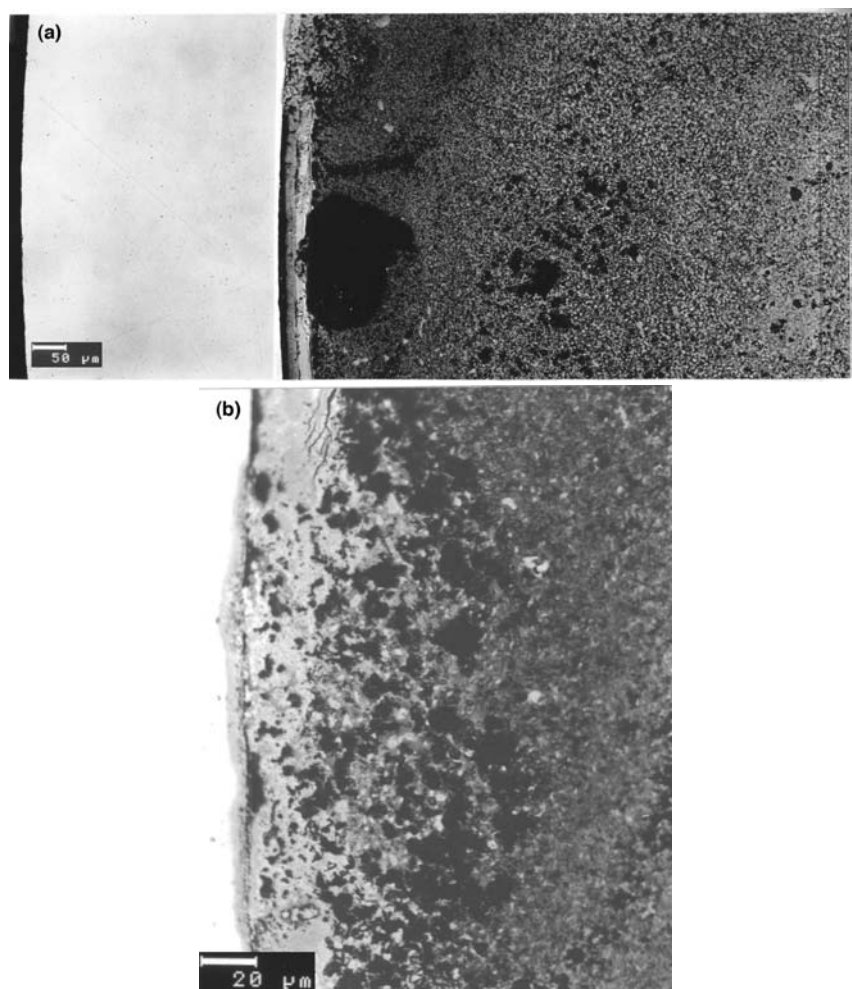


Figure 17. Optical micrographs showing a low magnification image (a) and high magnification image (b) of the fuel-cladding interface where the Zr-rind is not intact and interaction has occurred between the fuel and cladding.

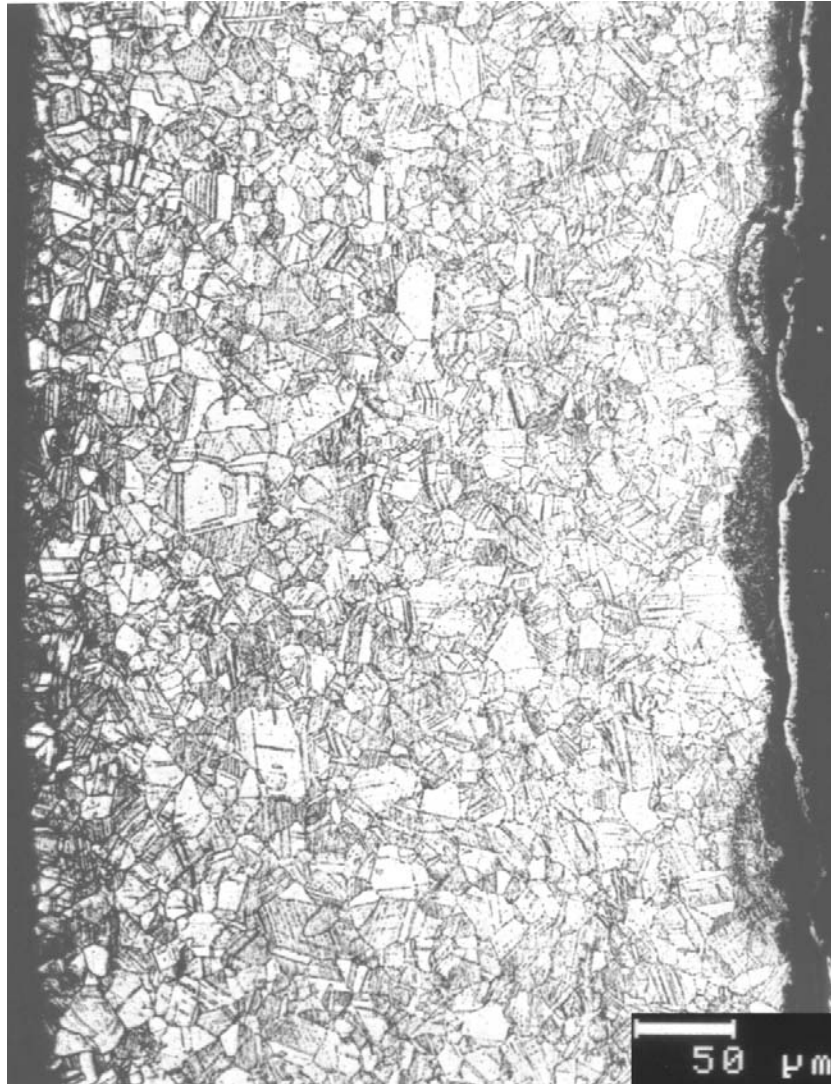


Figure 18. Optical micrograph showing the fuel-cladding interaction that has occurred in an area where the Zr-rind is not intact. The fuel side of the cladding is to the right.

2.2.2.2. T-225

The observed interaction between fuel and cladding in fuel element T-225 (U-23Zr fuel, D9 cladding, 10 at% burnup), as was the case for T-141, is dependent on the integrity of the Zr-rind. Little interaction is observed in areas where the Zr-rind is intact, but in cases where the rind is degraded some interaction is observed (see Figure 19). A maximum interaction zone size of 25 μm was observed in areas near the top of this fuel element.

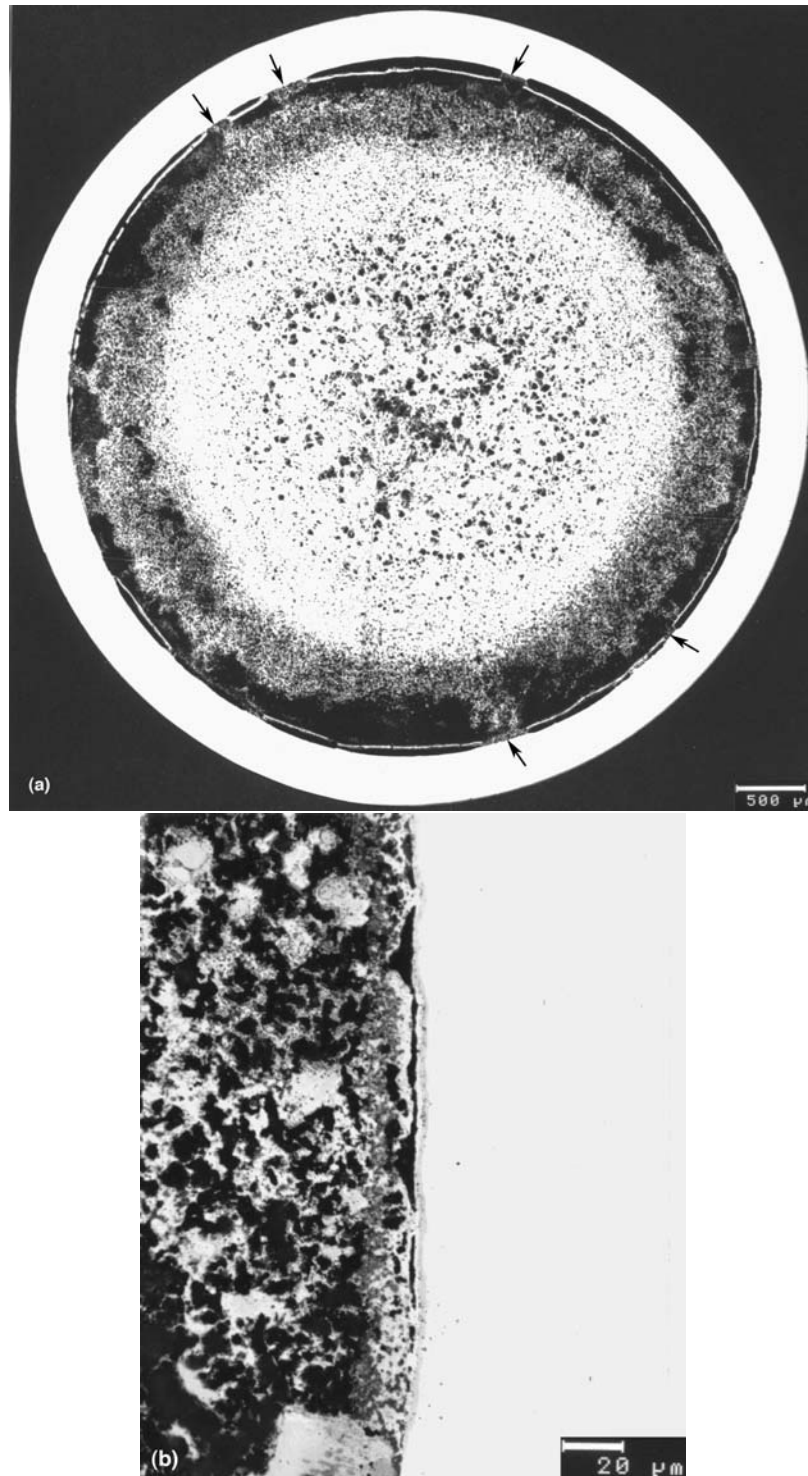


Figure 19. Optical micrographs of a transverse cross section taken from the bottom of element T-225. (a) is a low magnification image where the black arrows identify some of the areas where the Zr-rind is not intact and fuel-cladding interaction has occurred. (b) is a higher magnification image of one of these areas of interaction.

2.2.2.3. C-709

In a transverse cross section taken from element C-709 (U-23Zr fuel, D9 cladding, 9.3 at% burnup), two regions near the fuel-cladding interface were analyzed using EPMA. In both regions, complex multiphase diffusion structures were noted. By moving through the interaction zone, starting with the un-reacted cladding and moving towards the fuel, the first phase layer that was observed contained Fe, Cr, and some lanthanides (no Ni). The next layer consisted of Fe, Cr, lanthanides, and some Ni. This layer was followed by two lanthanide layers with and without Pd and Sn (the layer without Pd and Sn was towards the cladding) and a layer containing Zr, Ni, and Fe (and some Si). Closest to the un-reacted fuel was a multiphase layer, where the matrix phase was enriched in U. A precipitate phase containing Zr, Ni, and Fe was present on the clad-side of the layer, and a precipitate phase containing only Zr and Ni was present closer to the center of the fuel element. Lanthanides were not present in high amounts on the clad-side of this zone but were observed towards the fuel-side.

The second region of the sample that was analyzed had similar interdiffusion zones compared to the first. Again going from the un-reacted cladding towards the fuel center, the layers observed included (1) a layer containing Fe, Cr, and negligible Ni; (2) a layer with Fe, Nd, Ce, La, and negligible amounts of Ni; (3) a Zr-rich layer with Ni and smaller amounts of Fe; (4) a phase with Nd, Ce, La, and Pd; (5) a U layer with Fe and Zr precipitates (the layer was devoid in lanthanides); and (6) a layer with a U-matrix and lanthanide phases, Zr and Ni phases, and Fe and Zr phases.

3. Discussion

3.1. Phases Found at the Fuel-Cladding Interface in Irradiated U-Zr and U-Pu-Zr Fuel Elements Clad with D9, Type 316, or HT9 Stainless Steel

Based on the results from analyses of fuel elements with U-Zr and U-Pu-Zr alloy fuels and D9, Type 316, and HT9 stainless steel cladding that were irradiated in EBR-II, a variety of phases have been observed to form locally where swelled fuel has contacted the cladding. At the interface between the fuel and the cladding, the observed phases on both sides of the interface combine the cladding elements Fe, Ni, and Cr with the fuel components U, Zr, and Pu and the lanthanide fission products. Some of these phases near the fuel-cladding interface contain Pd and Ru.

Looking at the cladding-side of the original fuel-cladding interface, the developed phases are enriched in cladding components and they contain differing amounts of the primary fuel components (U, Pu, and Zr) and fission product components. The lanthanide fission products are observed to penetrate furthest into the cladding (Ce and Nd appear to penetrate the furthest) in both HT9 and D9-clad fuel elements. This penetration results in the development of phases containing Ce, Nd, and La nearest the un-reacted cladding.

Fuel element DP-16 (U-16Pu-23Zr fuel and HT9 cladding) was the fuel element analyzed in the greatest detail (using OM, SEM, and EPMA), and it provided the most information for trying to determine specific phases that may form on the cladding-side of the fuel-cladding interface. In particular, a lanthanide-Fe type of intermetallic phase, linked to the binary Ce-Fe

phase diagram, may be the most likely phase to develop nearest the unreacted cladding. Tortorici and Dayananda [11] determined, via a diffusion couple technique, that when a diffusion couple between HT9 and Ce is assembled and annealed at 425 C for 7 days, two single-phase layers form. Of the two layers, the phase layer adjacent to the un-reacted cladding is similar in composition and morphology to the one observed for fuel element DP-16 (layer 4 in Table 1). By creating a ratio of the Ce concentration (13 at%) with the sum of the concentrations for the cladding components [Fe (71 at%) + Cr (16 at%) + Ni (<1 at%)], Tortorici and Dayananda determined this phase layer to be a $\text{Ce}_2(\text{Fe}+\text{Cr}+\text{Ni})_{17}$ phase.² This phase is similar to the $\text{Ce}_2\text{Fe}_{17}$ described in the Ce-Fe binary phase diagram [12]. Using the same approach for labeling the phase layer that forms adjacent to the un-reacted cladding in the DP-16 fuel element, layer 4 in Figures 6 and 7 appears to also consist of a $(\text{Ce}+\text{Nd})_2(\text{Fe}+\text{Cr}+\text{Ni})_{17}$ phase.

Besides the $(\text{Ce}+\text{Nd})_2(\text{Fe}+\text{Cr}+\text{Ni})_{17}$ phase, other types of phases appear on the cladding-side of the fuel-cladding interaction zone in DP-16 that have different ratios of cladding components to lanthanides. These phases also contain Pu, Pd, and minor amounts of other components. In DP-16 and the other analyzed fuel elements, deep penetration of Pu into the cladding is observed, along with penetration of U. Zr does not penetrate far, but instead stays near the original fuel-cladding interface and combines with primarily cladding components to form Zr-rich phases.

On the fuel-side of the fuel-cladding interaction zone, some phases are observed that are enriched in the fuel constituents U, Pu, or Zr and contain Fe or Ni as the major non-fuel or components (with some lanthanides). Other phases are observed that contain primarily lanthanides. When the low-Ni stainless steel HT9 is employed as cladding in a fuel element, Fe is observed to penetrate the deepest into the fuel of any cladding constituent. As a result, the phases found in on the fuel-side of the interface in these fuel elements contain more Fe than any other cladding component. The fuel component that appears to combine with the Fe is Zr. For fuel elements clad with the austenitic stainless steel D9, Ni diffuses much further into the fuel, and it is found to combine with Zr. Iron also diffuses into the fuel and forms phases. An Fe and Ni depletion zone is observed in all the fuel elements, with the depletion of the Ni extending further into the cladding than the depletion of the Fe.

Pd is the non-lanthanide fission product that is found to interdiffuse farthest into the cladding. Looking at the results from DP-16, the phase layer with the most Pd also has levels of Ce, Nd, and Pu that are also reasonably high. In a case where Pd did not penetrate far into the cladding, the Pd is found in a phase layer where the Ce and Nd levels are still high; however, the Pu concentration has dropped off, and the Zr concentration was high. Using binary phase diagrams that combine the lanthanides Ce or Nd with Pd, it is observed that many intermetallic phases can form between any lanthanide and Pd. In layers 1 and 3 of the DP-16 SEM sample shown in Figure 6, notable amounts of Ce, Nd, and Pd are detected. These concentration levels could be indicating the formation of one or more (Ce+Nd)-Pd phases that have some solid solubility for other fuel and cladding constituents.

² A $(\text{lanthanide})_2\text{Fe}_{17}$ is the only possible intermetallic phase that can form at lower lanthanide concentrations based on binary phase diagrams between Fe and the lanthanides Ce, Nd, Sm, and Pr. La_2 when combined with Fe, does not form a $\text{La}_2\text{Fe}_{17}$ phase, and lanthanum is not observed in noticeable levels in what has been labeled the $(\text{lanthanide})_2(\text{Fe}+\text{Cr}+\text{Ni})_{17}$ phase in DP16. Based on the same phase diagrams, the likely intermetallic to form at higher lanthanide concentrations is a $(\text{lanthanide})\text{Fe}_2$ phase.

3.2. Factors Affecting Interaction Zone Thickness in Irradiated Fuel Elements

In general, interaction zone formation is localized. In many of the analyzed fuel elements, fuel-cladding interaction can be linked to the integrity of the Zr-rind. In cases, where the Zr-rind is failed, some of the thickest interaction zones can be observed. The Zr-rind is most likely to remain intact in fuel elements exposed to lower temperatures and lower power.

Both temperature and power variations along the length of a fuel element appear to impact the amount of fuel-cladding interaction that will occur. The temperature increases along the length of a fuel element and reaches a maximum at the top. The power variation is a dropped cosine. The fuel element region exposed to the maximum power and temperature combination is located near an x/L position of 0.80. The maximum fuel-cladding interaction is observed in this combined high-temperature and high-power region of a fuel element. In this region of a fuel element, the interdiffusion kinetics are increased and the supply of fission products at the fuel-cladding interface is relatively large. The result can be some of the largest observed interaction zones.

The burnup of a fuel pin is another parameter that impacts the size of the fuel-cladding interaction zone. Larger interaction zones are observed for higher-burnup fuel elements. One reason for the larger zone size is the larger inventories of fission products that are available at the fuel-cladding interface in high-burnup fuel elements. More lanthanides diffuse down the temperature gradient that exists from the center towards the outer edge of a fuel element, thereby increasing the inventory of fission products at the interface. Also, a high-burnup fuel element has been at relatively high temperatures for long periods of time, resulting in more available time for interdiffusion between fuel and cladding components to occur. Another fuel element attribute that impacts fuel-cladding interaction is the position of a fuel element in the core of the reactor because this helps determine the burnup of the fuel element. In addition, the peak inner cladding temperature will vary based on core position, and this will impact the interdiffusion kinetics. Fuel elements at the hotter core positions can exhibit increased interdiffusion at the fuel-cladding interface over those at colder positions.

4. Conclusion

Results from the destructive analyses of nineteen irradiated fuel elements from EBR-II have been assessed in the context of fuel-cladding interaction. Based on these results, the following conclusions can be made:

- 1) Of the fuel and fission product constituents, the lanthanide fission products penetrate the deepest into HT9 and D9 cladding; these components form phases containing Fe, Ni, and Cr.
- 2) For fuel elements with U-Pu-Zr alloy fuel, Pu is the primary fuel constituent that penetrates furthest into the cladding; for U-Zr alloy fuel, U penetrates the deepest into the cladding.
- 3) Of the cladding constituents, Fe and Ni are observed to diffuse into the fuel to form phases containing U, Pu, and Zr. A significant Ni-depletion band is observed to form

in the austenitic cladding, D9. Cr becomes enriched at the fuel-cladding interface and does not penetrate far into the fuel.

- 4) Near the fuel-cladding interface, light and dark contrast phases can be observed on the fuel-side of the interface that contains lanthanide fission products and Pd. Blocky particles containing Zr, Mo, Tc, Ru, Rh, and Pd are observed in the fuel near the cladding.
- 5) The Zr-rind in the fuel is a barrier to fuel-cladding interaction, and it is most likely to remain intact in fuel elements exposed to relatively lower temperatures and lower power.
- 6) Maximum fuel-cladding interaction occurs at the combined high power and high temperature region of a fuel element. The largest penetrations into the cladding are observed for fuel elements with ternary U-Pu-Zr fuel and D9 cladding.

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Chapter 5

MICROALLOYING DESIGN FOR NUCLEAR REACTOR PRESSURE VESSEL (RPV) STEELS

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Abstract

Microalloying technology is widely used in the steel industry to improve the mechanical properties of structural steels, in which the strength and toughness are improved by the refinement of ferrite grain size. The present chapter explains the importance of microalloying elements in controlling the microstructures and properties of quenched and tempered (QT) RPV steels. First, we give a brief description of the physical metallurgy of prior austenite grain refinement in microalloyed steels. The critical microalloying element for prior austenite grain size control is shown to be titanium, which forms carbonitrides, able to pin prior austenite grain boundaries during heat treatment. Secondly, attention is drawn to experimental results from the literature that demonstrate prior austenite grain coarsening in simulated coarse-grained heat-affected-zones (CGHAZs) in certain grades of non-microalloyed RPV steel. Finally, we discuss the microstructures and mechanical properties of simulated heat-affected-zones (HAZs) in A508 and A533 steels.

1. Introduction

Most nuclear electricity is generated nowadays using two types of light water reactor (LWR): the pressurised water reactor (the Western PWR or former Eastern Bloc VVER) and the boiling water reactor (BWR). In these designs, the reactor core is enclosed in a pressure vessel (RPV). Other reactor types having RPVs are the gas-cooled reactor (AGR and Magnox) and fast neutron reactor (FBR). In contrast, the pressurised heavy water reactor (PHWR), the ‘CANDU’ reactor and the light water graphite reactor (RBMK) have pressure tubes instead of a RPV.

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The main core region (the “barrel” region) of a modern PWR RPV (as exemplified by the UK’s Sizewell ‘B’ reactor) is a thick steel cylinder as shown schematically in Fig. 1. Here, the vessel is about 12 m high, with a wall thickness in the barrel region of ~215 mm and an internal diameter of ~4.4 m. It has a hemi-spherical bottom dome with penetrations for instrumentation, and a ‘nozzle ring’ to which is welded the inlet and outlet pipe-work carrying cooler water to the reactor and hotter water from the reactor. There is a detachable hemi-spherical head, with penetrations for the control rods, which can be raised and lowered to regulate the operation of, or shut down, the reactor. The head is bolted to the top of the nozzle ring, but all other joints are welds. The maximum operating pressure is 17 MPa, so that the hoop stress generated in the cylindrical section under normal operation is moderate (~170 MPa). Following conventional pressure vessel codes (with a safety factor of 2), this requires steel with a minimum yield strength of 340 MPa at the operating temperature of 290°C. The steel must be readily weldable.

Early vessels were fabricated from A533B plates (MnMoNi steel). These were welded into rings, which were joined by circumferential welds. The rings contained axial welds and the welds in each ring were therefore staggered to avoid a continuous axial weld running the length of the vessel (normal to the hoop stress). Later vessels were fabricated from forged A508 rings (MnMoNiCr steel), which avoided any axial welds. The UK Sizewell ‘B’ RPV has a particularly large ring forging in the barrel section, such that no weld is subjected to the very high neutron exposure associated with regions around the reactor core (referred to as the ‘beltline’ region). Conventional 2.25Cr1Mo steel was used by the Japan Atomic Energy Research Institute to manufacture a nuclear RPV for its so-called high temperature engineering test reactor (HTTR). The operation temperature of the HTTR is ~400°C and the internal pressure is ~4 MPa [2].

A main concern, particularly for steel in the ‘beltline’ region, relates to degradation in the material’s fracture toughness throughout life and the consequences of this if the plant were to suffer a ‘loss-of-coolant accident’ (LOCA) or other major accident late in life. The RPV is bombarded by neutrons, which produce ‘damage’: the displacement of atoms from their normal lattice sites to produce clusters of vacancies and (self-) interstitials, or ‘Frenkel pairs’ of vacancies and interstitials. Damage is now usually defined in terms of *displacements per atom*, dpa, although earlier measures may use dose (fluence) of ‘fast’ neutrons having energies above, typically, 0.1 MeV. The main effect is to harden the matrix, because the clusters, in particular, provide barriers to dislocation movement [3]. The increase in hardness is associated with an increase in the notched impact brittle-ductile transition temperature and a decrease in fracture toughness.

Neutron-induced hardening in the submerged arc welds (SAW) used to fabricate the RPV is due not only to the clusters of point defects, but also to precipitation hardening by copper: present to aid electrical contact in the SAW process. This finding was surprising. It was known that copper produces precipitate hardening in steels but the ageing temperatures are in the range 450–550°C, whereas the temperatures in the reactors were less than 350°C. The critical point is that the point defects generated by irradiation increase diffusion rates such that copper can become mobile at these lower temperatures. When the copper component is removed, it is found that the hardening depends on the square root of ‘damage’, expressed as dpa.

The advantage of the large ring forging in the Sizewell reactor is that the absence of a weld means that less hardening, and consequently less degradation of toughness, is produced

in the 'belt-line' region. A major issue for future nuclear 'new build' is that the worldwide supply of large forgings is extremely limited, so that timescales for introduction into a build programme are likely to be protracted. As demand for electrical power increases, attention may be paid to the possibility of justifying welded vessels, but these will have to meet a design life of 60 years or more: hence being subjected to a larger neutron dose than current generation PWRs. The issues of the SAW process and copper contents will arise.

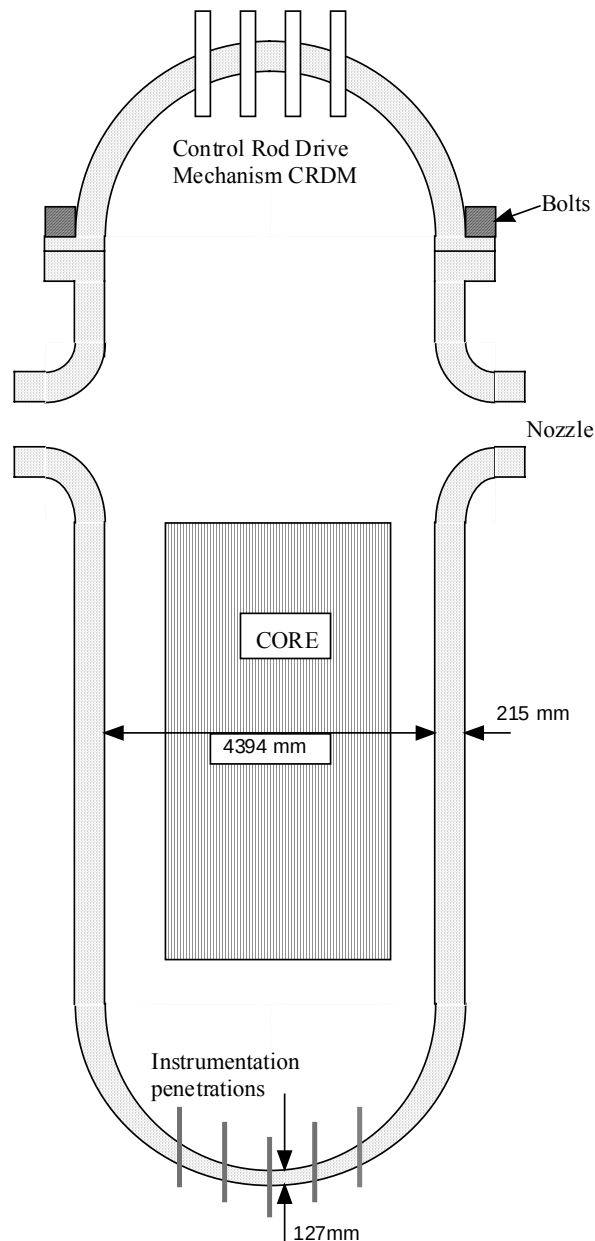


Figure 1. PWR Pressure vessel – schematic [1].

A second issue that causes concern was the possibility of neutron irradiation-enhanced intergranular segregation of trace impurity elements during reactor operation. In the same way that the point defects enable copper to diffuse at temperatures lower than normal, they can also assist the diffusion of phosphorus or similar impurities to grain boundaries at reactor operating temperatures, inducing embrittlement. Evidence of this is provided by results from notched impact tests made on surveillance specimens extracted from Russian VVER reactors, which are generally similar to PWRs, but, as a result of a smaller water gap, transmit a higher neutron dose (per unit power output) into a higher strength RPV steel. The increase in DBTT correlates with Cu and P contents [4]. The response *re* Sizewell was to specify a steel with tight controls on trace impurity elements [5]. The Japanese HTTR steel had both very low levels of trace impurity elements which could contribute to temper embrittlement (in wt. %) 0.002 P, <0.003 As, 0.002 S, <0.003 Sn and 0.0014 Sb and very low levels of trace impurity elements which could contribute to neutron irradiation embrittlement (in wt. %) 0.002 P and 0.02 Cu [2]. Information on performance testing of weldments was not reported, despite the fact that the steel was PWHT but without previous austenitisation. Tight control of impurities has obvious cost implications.

It is generally accepted that the presence of nickel in RPV steels increases its sensitivity to neutron irradiation embrittlement even at low phosphorus and copper concentrations. A significantly higher irradiation induced embrittlement has been reported for a weld with 1.7 wt. % Ni compared with that for a weld with 1.2 wt. % Ni [6]. Additionally, for a given high Ni content and all other factors being equal, a high manganese content is associated with a higher amount of embrittlement than that for a low manganese content. It is, however, noted in Ref. [6] that a steel with high nickel (3.4 wt. %), but with only 0.02 wt. % manganese and 0.03 wt. % copper, had low radiation sensitivity.

The steels SA 508, 2.25Cr1Mo0.25V (vanadium-modified 2.25Cr1Mo, grade F22V ASTM A 336/A 336M 2004) and modified 9Cr1Mo (grade F 91 ASTM A 336/A 336M 2004) have been considered as candidate materials for the two designs of very high temperature reactor system of the next generation nuclear plant reactor pressure vessel (NGNP) [7]: the pebble bed modular reactor (PBMR) and the gas turbine modular helium reactor (GT-MHR). The assessment of the three candidate materials concluded that SA508 steel is a potential candidate for the PBMR, but not for the GT-MHR because its operating temperature exceeds 371°C. SA 508 steel is ASME code approved for class 1 nuclear components for use up to 371°C under normal operation. Note that 2.25Cr1Mo0.25V is not currently approved under ASME Code Section III for use in nuclear systems although it is approved under Section VIII for non-nuclear applications. Modified 9Cr-1Mo is approved under ASME Code Section III for use in nuclear applications. It is a primary candidate for both PBMR and GT-MHR. It would require field welding of either ring forgings or plates.

2. Prior Austenite Grain Refinement in Microalloyed Steels

Prior austenite grain size control may be achieved by a combination of controlled rolling / forging and additions of microalloying elements such as V, Nb and, in particular, Ti. These additions can form carbide, nitride or carbonitride particles, stable at high temperatures and effective in pinning austenite grain boundaries. It is the solubility temperature of the nitride or carbide that is important: these are VN 1050°C, NbC 1100°C, AlN 1150°C, TiN >1450°C [8]. It is clear that, to restrain grain growth at a temperature of 1200°C or higher, it is necessary to

have a minimum level of titanium, although, for the short times involved in an actual weld cycle, other microalloying elements may also be effective up to 1200°C (because there is insufficient time for the nitrides or carbides fully to dissolve). This role of microalloying elements does not appear previously to have been explicitly recognised in the RPV field. It is of obvious importance to weldable RPV steel specifications.

The microchemistry and solubility of Ti, Nb and V carbide, nitride or carbonitride particles depend on the matrix, (austenite or ferrite), temperature, time and the major alloying elements present in the steel [8-11]. The solubility of VC in austenite is complete at 1200°C and independent of the carbon content, whereas the solubility of TiC decreases with increasing carbon content. The titanium precipitates TiC and, particularly, TiN are relatively insoluble in austenite at typical rolling/forging temperatures (1100-1250°C) [12]. These precipitates are associated with dislocation arrays within the grains or on grain boundaries, pinning them. This retards the process of recovery and recrystallization and grain growth due to solute drag and the presence of the precipitates.

However, the effectiveness of pinning grains (grain refinement) [12], depends on a critical particle radius, r_{crit} (Eq. 1) and the extent of Ostwald ripening i.e. coarse particles being formed with time and temperature at the expense of smaller ones which dissolve [13,14].

$$r_{crit} = \left(\frac{6Rf}{\pi} \right) * \left(\frac{3}{2} - \frac{2}{Z} \right)^{-1} \quad (1)$$

r_{crit} = critical particle radius (μm)

R = austenite grain size radius (μm)

f = volume fraction of particles

Z = grain size heterogeneity: $Z = R/R_0$, where R_0 = radius of the neighbouring grains (μm)

Therefore, r_{crit} depends on R , R_0 and f . When the prior austenite grains (R) are small, r_{crit} to control prior austenite grain size is small and mixed grains can form.

The Ostwald ripening effect consists of coarse particles growing at the expense of smaller particles which dissolve. Eq. 2 defines the diffusion controlled kinetics of particle coarsening developed by Greenwood [15], Wagner [16], and Lifshitz and Slyozov [17]:

$$r^3 - r_0^3 = \frac{8\sigma DC_0 V_m^2}{9RT} t \quad (2)$$

C_0 = concentration of particles species in matrix

D = diffusivity of particles species

r = average particle radius after time t

r_0 = initial average particle radius

R = gas constant

t = time

T = absolute temperature

V_m = particle molar volume

σ = particle / matrix surface energy

For a given temperature (T), the average particle radius (r) increases with time (t), concentration of particles species in matrix (C_0) and particle molar volume (V_m).

Table 1. Chemical composition (wt. %) of the ASTM A 302/A 302 M product analysis [20]

C [#]	Si	P	S	Cr	Mn	Ni	Cu	Mo	Sn	Al	As	B	Sb	Ti	Nb	V	N	O
<i>Grade A</i>																		
0.20-0.25	0.13-0.45	0.035*	0.035*		0.87-1.41			0.41-0.64										
<i>Grade B</i>																		
0.20-0.25	0.13-0.45	0.035*	0.035*		1.07-1.62			0.41-0.64										
<i>Grade C</i>																		
0.20-0.25	0.13-0.45	0.035*	0.035*		1.07-1.62	0.37-0.73		0.41-0.64										
<i>Grade D</i>																		
0.20*-0.25*	0.13-0.45	0.035*	0.035*		1.07-1.62	0.67-1.03		0.41-0.64										

*max; [#]0.20 C up to 25 mm incl, in thickness, 0.23 C over 25 mm to 50 mm incl, in thickness, 0.25 C over 50 mm, in thickness

The data in this table is adapted from ASTM A 302/A 302 M Standard specification for pressure vessel plates, alloy steel, manganese-molybdenum and manganese-molybdenum-nickel. American Society for Testing and Materials, copyright ASTM International, 100 Barr Harbor Drive, West Conshohocken, PA 19428

Table 2. Chemical composition (wt. %) of the ASTM A533 – 93(2004) product analysis [21]

C	Si	P	S	Cr	Mn [♠]	Ni	Cu	Mo	Sn	Al	As	B	Sb	Ti	Nb	V	N	O
<i>Type A</i>																		
0.25*	0.13-0.45	0.035*	0.035*		1.07-1.62			0.41-0.64										
<i>Type B</i>																		
0.25*	0.13-0.45	0.035*	0.035*		1.07-1.62	0.37-0.73		0.41-0.64										
<i>Type C</i>																		
0.25*	0.13-0.45	0.035*	0.035*		1.07-1.62	0.67-1.03		0.41-0.64										
<i>Type D</i>																		
0.25*	0.13-0.45	0.035*	0.035*		1.07-1.62	0.17-0.43		0.41-0.64										

*max; [♠]The maximum Mn content may be increased to 1.65 % on product analysis when Class 2 or Class 3 properties are specified and when Supplementary Requirement S3 (see Specification A 20/A 20M) is specified with a total holding time of more than 2.4 min/mm of thickness

The data in this table is adapted from ASTM A533 – 93(2004) Standard specification for pressure vessel plates, alloy steel, quenched and tempered, manganese-molybdenum and manganese-molybdenum-nickel, copyright ASTM International, 100 Barr Harbor Drive, West Conshohocken, PA 19428

Table 3. Chemical composition (wt. %) of the ASTM A 508/A 508M – 05b [22]

C	Si ^β	P	S	Cr	Mn	Ni	Cu	Mo	Sn	Al ^χ	As	B	Sb	Ti	Nb	V	N	O	Ca
<i>Grade 2</i>																			
0.27*	0.4*	0.025*	0.025*	0.25- 0.45	0.50- 1.00	0.50- 1.00	0.20*	0.55- 0.70		0.025*		0.003*		0.015*	0.01*	0.05*			0.015*
<i>Grade 3V</i>																			
0.10- 0.15	0.10*	0.020*	0.020*	2.8- 3.3	0.30- 0.60		0.25*	0.90- 1.10		0.015*		0.001- 0.003		0.015- 0.035	0.01*	0.20- 0.30			0.015*

*max; ^βWhen required by the purchased a minimum Si content of 0.15% shall apply for grades 2 and 3; ^χAl content reported shall be the combined total soluble and insoluble Al

The data in this table is adapted from ASTM A 508/A 508M – 05b Standard specification for quenched and tempered vacuum-treated carbon and alloy steel forgings for pressure vessels. American Society for Testing and Materials, copyright ASTM International, 100 Barr Harbor Drive, West Conshohocken, PA 19428

Table 4. Chemical composition (wt. %) of the ASTM A542/A542M-99(2004) product analysis [23]

C	Si	P	S	Cr	Mn	Ni	Cu	Mo	Sn	Al ^χ	As	B	Sb	Ti	Nb	V	N	O	Ca ^ξ
<i>Type B (2.25Cr1Mo steel)</i>																			
0.09- 0.18	0.50	0.015*	0.015*	1.88- 2.62	0.25- 0.66	0.28*	0.28*	0.85- 1.15								0.03*			
<i>Type D (CrMoV steel)</i>																			
0.09- 0.18	0.13*	0.020	0.015	1.88- 2.62	0.25- 0.66	0.28*	0.23*	0.85- 1.15				NA		0.035*	0.08*	0.23- 0.37			0.020*
<i>Type E (CrMoV steel)</i>																			
0.08- 0.18	0.15*	0.025*	0.010*	2.63- 3.37	0.25- 0.66	0.28*	0.28*	0.85- 1.15							0.10- 0.075	0.18- 0.33			NA

*max; ^ξRare earth metals (REM) may be added in place of calcium, subject to agreement between the producer and the purchaser.

In that case, the total amount of REM shall be determined and reported; NA—Product analysis is not applicable

The data in this table is adapted from ASTM A542/A542M-99(2004) Standard specification for pressure vessel plates, alloy steel, quenched-and-tempered, chromium-molybdenum, and chromium-molybdenum-vanadium, copyright ASTM International, 100 Barr Harbor Drive, West Conshohocken, PA 19428

Table 5. Chemical composition (wt. %) of the ASTM A 336/A 336M – 07 product analysis [24]

<i>C</i>	<i>Si</i>	<i>P</i>	<i>S</i>	<i>Cr</i>	<i>Mn</i>	<i>Ni</i>	<i>Cu</i>	<i>Mo</i>	<i>Sn</i>	<i>Al</i>	<i>As</i>	<i>B</i>	<i>Sb</i>	<i>Ti</i>	<i>Nb</i>	<i>V</i>	<i>N</i>	<i>O</i>	<i>Ca</i> [‡]	<i>Zr</i>
Grade F22V																				
0.11- 0.15	0.10*	0.015*	0.010*	2.00- 2.50	0.30- 0.60	0.25*	0.20*	0.90- 1.10				0.0020*		0.03*	0.07*	0.25- 0.35			0.015*	
Grade F91																				
0.08- 0.12	0.20- 0.50	0.025*	0.025*	8.0- 9.5	0.30- 0.60	0.40*		0.85- 1.05		0.0 2*				0.01*	0.06- 0.10	0.18- 0.25	0.03- 0.07			0.01*

*max; ‡Rare earth metals (REM) may be added in place of calcium, subject to agreement between the producer and the purchaser.

In that case, the total amount of REM shall be determined and reported

The data in this table is adapted from ASTM A 336/A 336M – 07 Standard specification for alloy steel forgings for pressure and high-temperature parts, copyright ASTM International, 100 Barr Harbor Drive, West Conshohocken, PA 19428

The interaction between particle dissolution and growth also depends on the nitrogen content in the steel [18]. In order to maximise the pinning effect, the maximum volume fraction of *fine* precipitates is required. Using the Hillert-Staffanson model, *Prikryl et al.* [11] carried out a kinetic and thermodynamic study of the evolution of carbonitride precipitates in medium carbon, Ti-V-N microalloyed steels for forging applications and concluded that N in excess of the stoichiometric level is required for grain size control. A nitrogen level higher than that required precisely to match the Ti content results in a low Ti concentration in the matrix and retards Ostwald particle ripening. *Lee et al.* [19] reported that the N / Ti (at. %) level in micro alloyed steels needs to be kept above 1.5 to be effective in controlling prior austenite grain size. As the solubility of vanadium carbonitride in ferrite is lower than that of vanadium carbide [11], the kinetics of particle growth of vanadium carbonitrides are lower than those for vanadium carbides.

Tables 1-5 list the chemical compositions of non-microalloyed RPV steels A302 and A533 and the chemical compositions of Ti, Nb and V microalloying additions for some A508, 2.25Cr1Mo and current versions for 2.25Cr1Mo0.25V and modified 9Cr1Mo steel grades.

3. Simulated HAZ in ASTM A508 and A533 Steels

The RPV steel is welded (usually SAW) and the HAZ of the weldment experiences temperatures up to the steel's melting temperature. The microstructures generated can be simulated either by weld thermal cycle simulation techniques or by furnace heat treatments, using lower temperatures for longer times. In Ref. [25] trials were carried out for 1h at austenitising temperatures of 1100°C, 1125°C, 1150°C, 1175°C and 1200°C in a DIN 22NiMoCr37 forging steel equivalent to ASTM A508 Class2. Subsequently, a quenching, tempering, quenching and embrittling treatment (QTQE) was carried out. The cooling rates experienced in the HAZ are rapid as a result of metallic conduction and this was simulated by water quenching after the austenitisation treatment. A service weld is then subjected to a stress-relief heat treatment at rather slow heating and cooling rates. This was simulated by tempering at 650°C for 2h, followed by water-quenching and an isothermal hold at 520°C for 180h to maximise any susceptibility to grain boundary embrittlement (as a result of segregation of trace impurity elements such as phosphorus to the boundaries). The lowest temperature generated a prior austenite grain size of ~100 µm; temperatures of 1125°C and 1150°C produced a mixed grain size: some of 100 µm diameter, but some of ~1 mm diameter; temperatures of 1175°C and 1200°C exhibited large, uniform, equiaxed grains of ~1 mm diameter. This DIN 22NiMoCr37 steel investigated was much more susceptible to grain growth than other 'equivalent' variants of RPV steels. Previous experience [26], using the (plate grade) equivalent steel A533B indicated that a heat treatment of 1h at 1200°C simulated the coarse grain (110 µm diameter) HAZ microstructure in service welds. The effect of such very coarse grains on the material's resistance to fracture is discussed below.

A literature survey of prior austenite grain sizes of A533, A508 and 2.25Cr1Mo RPV steels was made. These are summarised in Table 6, together with details of the steel grade and its heat treatment. At an austenitisation temperature of 1300°C for 10 s, a range of prior austenite grain size values, from 400 µm to 480 µm is quoted in Table 6, compared to that of

Table 6. Prior austenite grain size for some grades of A533, A508 and 2.25Cr1Mo RPV steels reported in the literature

Materials	Chemical composition (wt. %)	Heat treatment	Prior γ grain size; Microstructure	References
A533B	Fe-0.25C-0.26Si-0.0080P-0.0140S-0.11Cr-1.41Mn-0.46Ni-0.12Cu-0.49Mo-0.0080Sn-0.016Al-0.0120As-0.0013Sb-0.003V-0.008N	1300°C 10 s; AC; PWHT 615°C 6 h	480 μm bainite	[27]
A533B	Fe-0.21C-0.23Si-0.0060P-0.0090S-0.05Cr-1.45Mn-0.55Ni-0.13Cu-0.64Mo-0.0150Sn-0.029Al-0.0350As-0.0015Sb-0.003V-0.014N	1300°C 10 s; AC; PWHT 615°C 6 h	400 μm bainite	[27]
SA508-2	Fe-0.213C-0.26Si-0.0050P-0.0110S-0.36Cr-0.64Mn-0.56Ni-0.04Cu-0.57Mo-0.0230Sn-0.001Al-0.0020As-0.0040Sb-0.004V-0.002N	1300°C 10 s; AC; PWHT 615°C 6 h	450 μm bainite	[27]
SA508-2	Fe-0.223C-0.28Si-0.0070P-0.0120S-0.34Cr-0.64Mn-0.63Ni-0.02Cu-0.58Mo-0.0040Sn-0.003Al-0.0030As-0.0040Sb-0.022V-0.005N	1300°C 10 s; AC; PWHT 615°C 6 h	480 μm bainite	[27]
A508 CL3	Fe-0.18C-1.46Mn-0.51Mo-0.86Ni-0.10Si-0.15Cr-0.006P-0.003S-0.03Cu-0.004V-0.018Al	Simulated HAZ (a) $T_p = 900^\circ\text{C}$ 10 s double thermal cycling (b) $T_p = 1350^\circ\text{C}$ 10 s double thermal cycling	(a) 10 μm 71% ferrite, 22 % martensite, 5% austenite 2% carbide (b) 410 μm 4% ferrite, 93 % martensite, 2% austenite 1% carbide	[28]
2.25Cr1Mo	Fe-0.15C-0.51Mn-0.91Mo-0.11Ni-0.22Si-2.27Cr-0.23S-0.013P-0.16Cu-0.03Al-0.004Ti	1100°C 2 h; OQ; 650°C 2 h; OQ; embrittling treatment: 520°C 0 h, 24 h, 96 h and 210 h; AC	170 μm	[26]

WQ—water quenching; OQ—oil quenching; AC—air cooling; PWHT—Post weld heat treatment

A533B and SA508-2 reprinted from Materials Science and Engineering, Vol 37, C. J. McMahon, Jr. R. J. Dobbs, D. H. Gentner, Stress relief cracking in MnMoNi and MnMoNiCr pressure vessel steels, pp. 179-186, Copyright (1979), with permission from Elsevier

A508 CL3 reprinted from Metallurgical and Materials Transactions A, Vol 31(4), S. Kim, S. Y. Kang, S. J. Oh, S. J. Kwon, S. Lee, J. H. Kim, J. H. Hong, Correlation of the microstructure and fracture toughness of the heat-affected zones of an SA 508 steel, pp. 1107-1119, Copyright (2000), with kind permission of Springer Science and Business Media

410 μm in the ASME SA 508 Cl.3 steel (b) of Ref. [28] which was measured after double thermal cycling at a peak temperature of 1350°C corresponding to the S1 location in Table 7 and Fig. 2 (see below) but without PWHT. The former ASTM A 508/A 508M–03 steel class 3 was not microalloyed with Ti [30], whereas the current version ASTM A 508/A 508M–05b grade 3 is Ti microalloyed [22].

4. Mechanical Properties of Simulated HAZs in A508 and A533 Steels

Typical HAZs, thermal cycle simulation conditions and mechanical properties for an ASTM A508 steel (class 3) [28] are given in Table 7. The HAZ positions in multi-pass welding are shown schematically in Fig. 2. The HAZ width is typically about 4.5 mm. Weld thermal cycle simulation techniques are used to determine the mechanical properties of the HAZ. From Table 7, the S1-S5 regions (the coarse-grained and fine-grained regions) showed higher combinations of hardness, strength and toughness levels than S6-S7 regions and the base metal. A complex broad range of microstructures can be generated in the weld HAZ. The microstructure of the S1-S5 regions was tempered martensite and that of the S6-S7 regions was tempered bainite. The latter had a large volume fraction of coarse carbides, which reduced the yield strength and was responsible for the toughness deterioration. Charpy V-notch impact tests at -50°C with the specimen notch positioned at different distances from the fusion line (>1 mm) in the HAZ of the actual weld conformed to the toughness ranking found in the simulated HAZ specimens, but the impact toughness values in the HAZ of the actual weld showed large scatter [29].

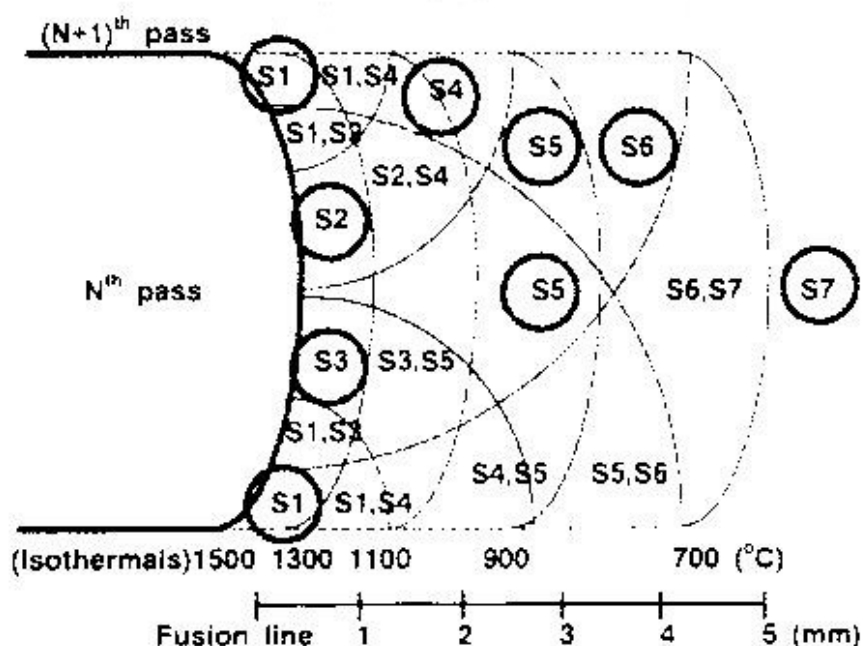
Table 7. Typical HAZ regions, thermal cycle simulation conditions and mechanical properties for an ASTM A508 Cl.3 steel [28]

<i>Position</i>	<i>Typical HAZ region</i>	<i>Nth pass</i> <i>T_p^{Nth} (°C);</i> <i>time (s)</i>	<i>(N+1)th pass</i> <i>T_p^{(N+1)th} (°C)</i> <i>time (s)</i>	<i>Vickers</i> <i>hardness</i> <i>(HVN)</i>	<i>Tensile</i> <i>strength</i> <i>(MPa)</i>	<i>Charpy Absorbed</i> <i>Energy at -50°C</i> <i>(J)</i>
Base metal	---	---	---	198	610	97
S1	U CG + PWHT	1350; 10	1350; 10	288	767	85
S2	SCR CG + PWHT	1350; 10	900; 10	258	780	150
S3	IR CG + PWHT	1350; 10	700; 10	294	749	120
S4	SCR FG + PWHT	900; 10	900; 10	227	670	175
S5	IR FG + PWHT	900; 10	700; 10	216	637	160
S6	IR + PWHT	700; 10	700; 10	186	537	22
S7	SR + PWHT	650; 10	650; 10	189	559	20

U CG—unaltered coarse-grained; SCR CG—supercritically reheated coarse-grained; IR CG—intercritically reheated coarse-grained; SCR FG—supercritically reheated fine-grained; IR FG—intercritically reheated fine-grained; IR— intercritically reheated; SR— supercritically reheated; PWHT—post weld heat treatment (PWHT) at 893 K for 4.5 h

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Uniaxial tensile testing and 0.8 scaled 'Griffith and Owen' blunt notch four-point bend testing were carried out on the (N.B. DIN 22NiMoCr37) as-received (AR) 'parent' RPV material and on the HAZ simulated conditions: the 1100°C QTQE condition, to represent a 'normal' (~100 μm) CGHAZ and the 1200°C QTQE condition, to represent the 'extraordinary' (~1 mm) CGHAZ. The tensile properties are given in Table 8. It can be seen that at a given testing temperature the two heat treated conditions had similar strength levels, but the 1200°C QTQE condition exhibited lower ductility levels (as measured by the percentage of reduction of area) than those for the 1100°C QTQE condition. Fracture toughness values were determined for the AR condition and for the simulated HAZ 1100°C QTQE condition [31].



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Figure 2. Positions of the HAZ regions from Table 1 in the actual multipass welding [29].

The sharp crack fracture toughness values at -150°C for the AR tempered bainite condition, and the 'normal' CGHAZ simulation (1100°C QTQE) tempered martensite condition are compared in Fig. 3. The CGHAZ condition has uniformly higher fracture toughness, $94.3 \pm 14.3 \text{ MPam}^{1/2}$, than the AR condition, $58.4 \pm 6.9 \text{ MPam}^{1/2}$. Its yield strength at -150°C is also significantly higher: 1012 MPa vs. 779 MPa, respectively. The local fracture stress values in 0.2 mm blunt notched bars showed similar effects: $2624 \pm 17 \text{ MPa}$ for 1100°C QTQE and $1982 \pm 40 \text{ MPa}$ for AR. The 1200°C QTQE condition had as well higher yield strength at -150°C (998 MPa) and higher local fracture stress values in 0.2 mm blunt notched bars at -150°C ($2500 \pm 147 \text{ MPa}$) than the AR condition. These results can be explained in terms of the finer carbide sizes and distributions obtained in tempered martensite

Table 8. Tensile properties

Specimen	T (°C)	YS (MPa)	UTS (MPa)	El (%)	RA (%)	n
AR						
SX25.3-c.2+1.1	-196	917 (978)	978	18.0	42	0.121
SX25.3-c.2+1.2	-196	917 (988)	988	18.7	45	0.134
Mean	-196	917 (983)	983	18.4	44	0.128
SX25.3-c.2+1.3	-150	779 (830)	866	18.4	54	0.136
Ref. [32]	20	470	612	nr	70	nr
Ref. [33]	20	455	609	20*	73	nr
1100-QTQE						
SX25.2-a.2'+1'.7	-196	1182 (1192)	1227	17.1	36	0.064
SX25.2-c.2+1.1	-150	978	1070	16.6	49	0.071
SX25.2-a.2'+1'.8	-150	1013	1100	15.0	45	0.072
SX25.2-c.2+1.2	-150	1044	1108	16.0	46	0.053
Mean	-150	1012	1093	15.9	47	0.065
SX25.2-c.2+1.3	20	752	860	23	56	0.085
SX25.2-c.2+1.4	20	750	851	21.4	56	0.081
Mean	20	751	856	22.2	56	0.083
1200-QTQE						
SX25.2-c.2'+1'.3	-196	1192 (1207)	1233	14.5	15	0.074
SX25.2-c.2'+1'.6	-150	988	1080	11.6	29	0.066
SX25.2-c.2'+1'.5	-150	993	1070	13.8	23	0.063
SX25.2-c.2'+1'.4	-150	1014	1080	12.9	23	0.066
Mean	-150	998	1077	12.8	25	0.065
SX25.2-c.2'+1'.8	20	737	834	16.8	47	0.081
SX25.2-c.2'+1'.7	20	756	852	20.6	51	0.077
Mean	20	747	843	18.7	49	0.079

(upper) and lower yield points values;

T—Testing temperature; YS—0.2% yield strength; UTS—ultimate tensile strength; El—elongation to failure; RA—reduction of area; n—strain hardening exponent (calculated from the true stress (σ)—true strain (ϵ) relationship $\sigma = K\epsilon^n$ in the region of uniform plastic deformation, where K is a constant, assuming constant volume deformation); nr—not reported; *—the specimen gauge length and diameter were not given and therefore may not be directly comparable.

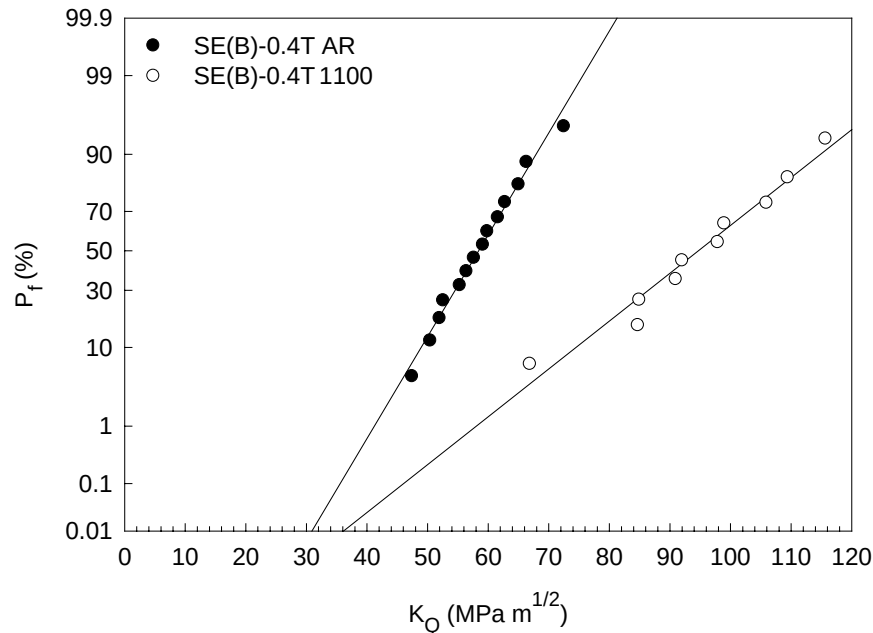
Tensile properties at -196°C and -150°C reprinted from Engineering Fracture Mechanics, Vol 75(8), M. J. Balart, J. F. Knott, Low temperature fracture properties of DIN 22NiMoCr37 steel in fine-grained bainite and coarse-grained tempered embrittled martensite microstructures, pp. 2480-2513, Copyright (2008), with permission from Elsevier

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Tensile properties at 20°C [33], reprinted from International Journal of Fracture, Vol 31(4), E. Lucon, M. Scibetta, E. van Walle, Assessment of the master curve approach on three reactor pressure vessel steels, pp. 161-178, Copyright (2003), with kind permission of Springer Science and Business Media

compared with tempered bainite, giving both more potent barriers to dislocation movement (higher yield strength) and smaller microcrack nuclei (higher values of local fracture stress and hence fracture toughness). Although more data would be needed to differentiate the mean values in 0.2 mm and 1 mm blunt notched bars at -150°C and -196°C for the 1100°C and

1200°C QTQE conditions (Tables 4, 5 and 6 in Ref. [31]), the *minimum* values are *lower* for the coarse-grained 1200°C QTQE condition (2106 MPa at -150°C, 2265 MPa at -196°C in 0.2 mm blunt notched bars; 2473 MPa at -196°C in 1 mm blunt notched bars) than for the fine-grained 1100°C QTQE condition (2591 MPa at -150°C, 2364 MPa at -196°C in 0.2 mm blunt notched bars; 2660 MPa at -196°C in 1 mm blunt notched bars).

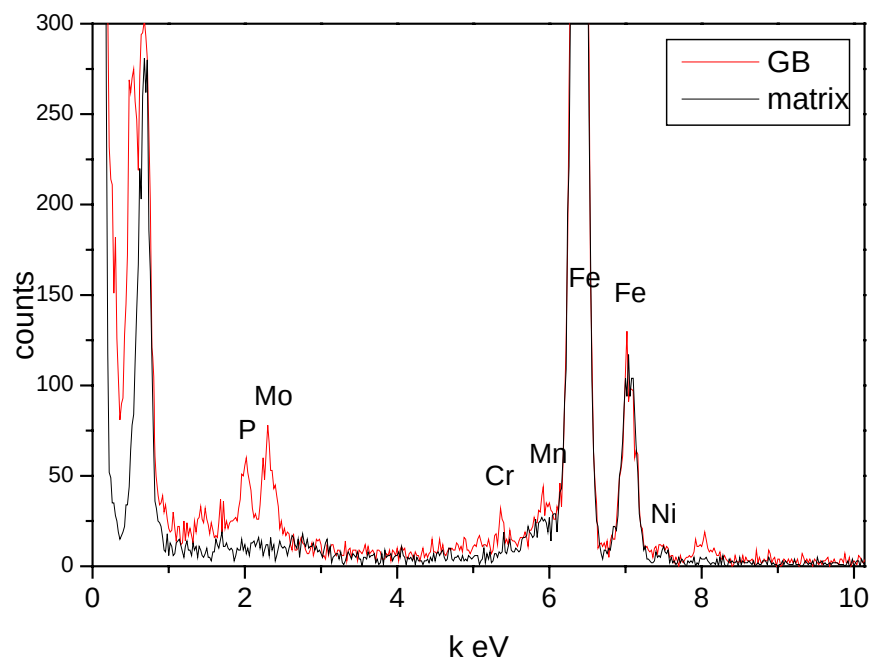


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Figure 3. Sharp-crack fracture toughness values at -150°C for a tempered bainite condition, and the ‘normal’ CGHAZ simulation 1100°C QTQE for a tempered martensite condition [31].

Note that the fracture surfaces in the sharp crack tests for the 1100°C QTQE treatment were ~100 % transgranular, but in the blunt notch tests were ~15 - 30 % intergranular in the region just below the notch. This amount of intergranular fracture has been shown elsewhere not significantly to reduce the local stress below that for unembrittled material [26]. Figs. 4 and 5 show the grain boundary P, Cr and Mo segregation for the 1100°C condition as determined by FEG TEM [34].

It could therefore be argued that the simulated CGHAZ and FGHAZ samples would exhibit properties superior to those of the base metal. However, it has been demonstrated [35] that in A533 Cl.1 a tempered coarse grain martensite is more susceptible to intergranular embrittlement than is a tempered bainite of the same grain size. The detrimental effects on fracture resistance of phosphorus segregation to prior austenite grain boundaries have been demonstrated in A508 Cl.3 steel [36], and in A533B, 2.25Cr1Mo steels [26]. Furthermore, prior austenite grain size is a major factor in determining the ductile-to-brittle transition temperature of MnMoNi alloys, having a tempered martensitic microstructure and similar strength levels [37], and of A302B, A533B, A508 Cl.2 RPV steels [38] and 2.25Cr1Mo RPV steels [39].



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Figure 4. EDS spectra from grain boundary and matrix, showing segregation of P to grain boundary [34].

In recent work on the effect of phosphorus segregation on fracture properties, a direct relationship between grain boundary phosphorus concentration and microscopic fracture stress has been demonstrated: in a steel of chemical composition representative of segregated zones of A508 class 3 steel [36]; and in two RPV steels 2.25Cr1Mo and A533B [26]. Additional findings are a critical grain boundary phosphorus concentration threshold for the transition from entirely transgranular cleavage to partly intergranular fracture and a decrease in microscopic fracture stress with increase in the percentage of intergranular fracture. In parallel, a fracture toughness threshold for entirely transgranular cleavage fracture was established and a decrease in fracture toughness with increase in grain boundary phosphorus concentration and percentage of intergranular fracture was found [26]. It was concluded that $\sim 20\%$ area fraction of intergranular fracture was needed to produce a significant reduction in the microscopic fracture stress and fracture toughness. When the grain boundary P concentration was less than the threshold value needed to cause intergranular fracture, the fracture stress was taken as the cleavage fracture stress, 2940 MPa for 2.25Cr1Mo and 2392 MPa for A533B at -196°C ; and the fracture toughness was considered to be equal to the cleavage fracture toughness, $45.5 \text{ MPam}^{1/2}$ for 2.25Cr1Mo at -196°C . The degree of P segregation to grain boundaries depends on the both the misorientation angle and the prior austenite grain size. Segregation occurs most readily at high angle grain boundaries [40] and refinement of grain size implies a higher grain boundary area to accommodate P, giving a lower grain boundary P concentration, for a given P level and embrittling heat treatment.

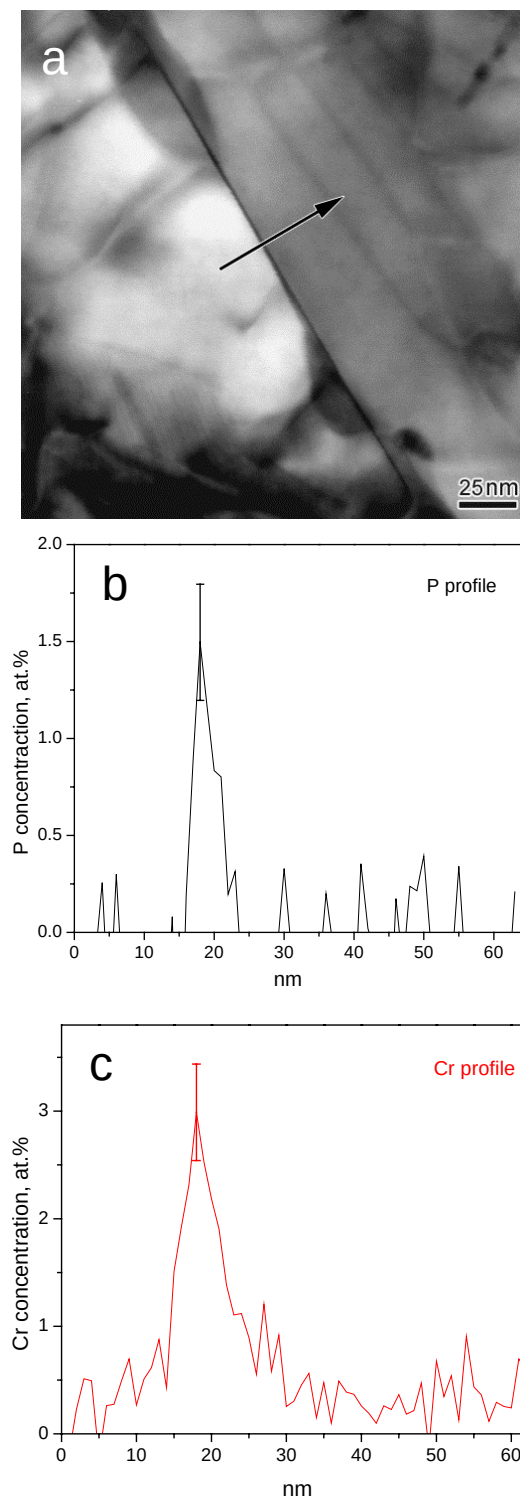
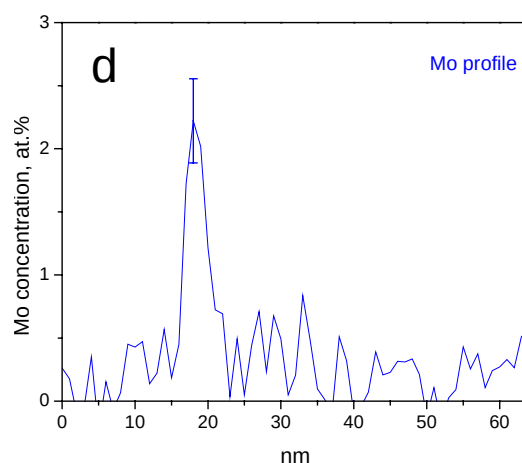


Figure 5. Continued on next page.



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Figure 5. (a) EDS linescan across the grain boundary (gb); and variation in (b) P, (c) Cr and (d) Mo concentration across the gb, which showed segregation of P, Cr and Mo to the gb [34].

In the *sharp crack fracture toughness* test, the fatigue crack tip, after prefatiguing, tends to be located primarily somewhere within the interior of a grain and only a very small fraction of its length (along the thickness direction of the testpiece) intercepts a grain boundary. At low temperatures, the measured value of fracture toughness then represents the difficulty of initiating and propagating what is initially a transgranular cleavage crack. In blunt notched testpieces (such as the conventional Charpy specimen with a notch root radius of 0.25 mm), however, the region of more or less uniformly high stress samples a region of approx. one root radius parallel to the length of the testpiece, so that the test volume sampled at the notch root surface is likely to include at least one grain boundary (for a grain size of 100 μm or less) at every point along the length of the notch (thickness of the testpiece). If the material has embrittled grain boundaries (as a result of trace impurity segregation induced by the QE treatment), it is possible for the brittle fracture mode to become *intergranular*. This is associated with a lower work-of-fracture (as measured by the local fracture stress) than that for transgranular fracture. The fracture appearance changes from a very small area fraction of intergranular fracture in precracked specimens to a large area fraction in blunt notched specimens. The area fraction of intergranular fracture observed during the subsequent propagation of a crack, initiated either in a transgranular manner in a sharp crack test, or in an intergranular manner in a blunt notch test, throughout the bulk of the specimen, is an issue on which there is no clear agreement in the literature.

Druce [37] has investigated the metallurgical factors determining the brittle and ductile properties of MnMoNi alloys, having a tempered martensitic microstructure, for different austenitising temperatures in the range 850°C – 1300°C, followed by quenching and tempering. Some additional samples were held isothermally at 500°C after tempering. The brittle fracture resistance was measured in terms of the Charpy impact ductile-to-brittle transition temperature (DBTT) and the ductile fracture resistance was related to the Charpy impact upper shelf fracture energy (USE). It was shown that the steel possessed similar strength levels for the different heat treated conditions, but that the prior austenitisation

treatment and cooling rate during quenching had important influences on the DBTT and USE. The coarser prior austenite grain microstructures fractured partly in a brittle intergranular mode in the lower shelf region. This affected the DBTT adversely. The large prior austenite grain size resulted in a relatively low grain boundary area per unit volume, so that, for a given impurity content, a higher coverage of trace impurity elements and a greater number density of fine MnS particles could be present at the prior austenite grain boundaries. The former had a detrimental effect on the lower shelf and DBTT, and the latter on the USE.

Nb and Ti microalloying additions have been shown to improve the DBTT in steels [41,42]. A CrMoB steel with Nb microalloying addition showed an improvement in the 27 J transverse impact energy transition temperature (-140°C) with respect to the same grade with no Nb (-87°C) [41]. However, when added in excess Ti causes the formation of coarse (Ti,V)(C,N) particles which can act as a cleavage initiation sites [43]. Hence, control of the Ti level is required to avoid the formation of coarse (Ti,V)(C,N) in the liquid phase or during steel solidification.

Conclusion

Prior austenite grain size is a major factor in determining the ductile-to-brittle transition temperature of MnMoNi alloys, having a tempered martensitic microstructure and similar strength levels [37], and of A302B, A533B, A508 Cl.2 RPV steels [38] and 2.25Cr1Mo RPV steels [39]. Detrimental effects on fracture resistance of phosphorus segregation to prior austenite grain boundaries have been demonstrated in A508 Cl.3 steel [36], and in A533B, 2.25Cr1Mo steels [26]. It is found that there is a critical grain boundary phosphorus concentration threshold for the transition from 100 % transgranular cleavage to partly intergranular fracture and, from then on, a decrease in microscopic fracture stress with increase in the percentage of intergranular fracture. In parallel, it is found that there is a fracture toughness threshold for 100 % transgranular cleavage fracture and, beyond that threshold, a decrease in fracture toughness with increase in grain boundary phosphorus concentration and percentage of intergranular fracture [26]. The degree of P segregation to grain boundaries depends on the both the misorientation angle and the prior austenite grain size. Segregation occurs most readily at high angle grain boundaries [36,40] and refinement of grain size implies a higher grain boundary area to accommodate P, giving a lower grain boundary P concentration, for a given P level and embrittling heat treatment. Our own results, for DIN 22NiMoCr37 RPV steel (having low levels of trace impurity elements), heat treated to produce both coarse and fine grain sizes, and to maximise any intergranular embrittlement, show that, for martensitic microstructures, the mean local fracture stress values for coarse and fine austenite grain size are indistinguishable, within scatter, although the lower bound results perhaps suggest that the coarse grain condition is more susceptible to brittle fracture. Note that all the martensitic conditions give better properties than does the original, as-received, bainitic condition.

Some current weldable RPV steels have explicit Ti microalloying additions: for example the current versions of the three candidate materials, SA 508, 2.25Cr1Mo0.25V and modified 9Cr1Mo, for the two designs of very high temperature reactor system of the NGNP reactor pressure vessel [7]: the PBMR and the GT-MHR. However, some other current weldable RPV steels are non-microalloyed, e.g. A302 and A533, and we have shown that unrestricted

prior austenite grain growth can occur if microalloying elements are not present. The critical microalloying element for prior austenite grain size control is titanium, which forms carbonitrides, able to pin prior austenite grain boundaries during heat treatment. The Ti microalloying additions made to weldable RPV steels for prior austenite grain size control need to be optimised to provide a high volume fraction of fine particles, and there is evidence that the effect of Ti may be enhanced by careful control of the N content. Although our own results on the effect of grain size on brittle fracture are not completely unambiguous, the DIN 22NiMoCr37 was 'clean', in terms of levels of trace impurity elements, and less unambiguous effects could well be obtained for 'dirtier' steels.

In summary, if it is required to restrict the growth of austenitic grains in the welding of RPV steels, the addition of *optimum amounts* of the microalloying element Ti is advised. For shorter thermal cycles, it is possible that V or Nb could be employed as alternatives.

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Chapter 6

HISTORY AND EVOLUTION OF FUSION POWER PLANT STUDIES: PAST, PRESENT, AND FUTURE PROSPECTS

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Abstract

This chapter provides a brief history of magnetic confinement fusion power plant conceptual designs, beginning with the early development in 1970, focusing on tokamaks. In addition, the evolution of six more magnetic concepts (stellarator, spherical tori, field-reversed configurations, reversed-field pinches, spheromaks, and tandem mirrors) is highlighted. The key issues encountered are discussed, including the technological obstacles and the elements necessary for economic competitiveness. Extensive R&D programs and international collaboration in all areas of fusion research led to a wealth of information generated and analyzed. As a result, fusion promises to be a major part of the energy mix in the 21st century and beyond.

1. Introduction

Since the 1958 2nd Conference on the Peaceful Uses of Atomic Energy held by the United Nations in Geneva, Switzerland, the secrecy surrounding controlled thermonuclear fusion by magnetic confinement had been lifted allowing researchers in the US, Russia, and UK to freely share results and discuss challenges. In the 1960s, fusion researchers were engaging in a variety of theoretical analyses and experiments to more fully understand and advance fusion physics and engineering. The energy crisis of the early 1970s encouraged all nations to seriously investigate other nuclear energy sources (like fusion and renewable) to supplement fission. Building on the early progress made in the 1950s and 1960s, the world's fusion

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researchers realized the need for better understanding of the physics and technology of fusion energy. Numerous fusion studies, extensive research and development (R&D) programs, more than 100 operating experiments worldwide, impressive international collaboration in all areas of research, and a large body of accumulated knowledge have led to the current wealth of fusion information and understanding. For decades, the international energy agencies have provided a framework for collaborative programs that covers a broad range of fusion topics (such as plasma physics, materials, safety, environmental, and economic aspects, and social acceptance). These strong national programs along with international cooperation in fusion research programs allowed interested organizations to nurture their R&D activities in particular areas of interest. Even though declining fusion budgets have impeded the progress of fusion research for decades, many scientists around the world strongly believe that fusion could be an option in the 21st century energy mix. The growing concern over environmental, safety, health, and sustainability effects with other current energy sources (fission, coal, solar, and wind) could erode public confidence in these concepts and provide an opportunity for fusion to become a significant energy source.

In the early 1950s, there were four magnetic confinement fusion concepts pursued internationally: tokamak (donut configuration with toroidal plasma current), stellarator (steady-state toroid without plasma current), mirror (steady-state linear system with magnetic wells), and pinch (simple toroidal device). The tokamak, stellarator, and pinch concepts have experienced substantial modifications over the past 60 years. The mirror concept was actively pursued in the US, but suspended in 1986 due to budgetary constraints, while continuing at a very low level in Japan and Russia. After the first 1969 International Fusion Reactor Conference in Culham, England, more than 50 conceptual power plant design studies have been conducted in the US, EU, Japan, Russia, and China. During the 1970-2008 period, numerous D-T fueled fusion power plant designs were developed for both magnetic and inertial confinement concepts, covering a wide range of new and old design approaches: tokamaks, stellarators, spherical tori (ST), field-reversed configurations (FRC), reversed-field pinches (RFP), spheromaks, tandem mirrors (TM), and laser/heavy-ion/Z-pinch driven inertial fusion. Most of the studies and experiments are currently devoted to the D-T fuel cycle, since it is the least demanding to reach ignition. The stress on fusion safety has stimulated worldwide research on fuel cycles other than D-T, based on 'advanced' reactions, such as deuterium-deuterium (D-D), deuterium-helium-3 ($D-^3\text{He}$), proton-boron-11 ($P-^{11}\text{B}$), and $^3\text{He}-^3\text{He}$. In addition, a few smaller-scale projects investigated non-electric applications of fusion along with the technological means to lessen the likelihood of proliferation.

Figures 1 and 2 display the timeline of large-scale magnetic fusion power plants designed since the early 1970s by research teams in the US and worldwide. Numerous conceptual commercial plant designs were developed for all seven confinement approaches, especially for the tokamak. The decade of the 1980s witnessed a transition period aimed at temporarily impeding the US large-scale tokamak studies in order to investigate alternate concepts: stellarator, ST, FRC, RFP, spheromaks, and TM. In the late 1980s, the US has decided to pursue all concepts, except tandem mirrors.

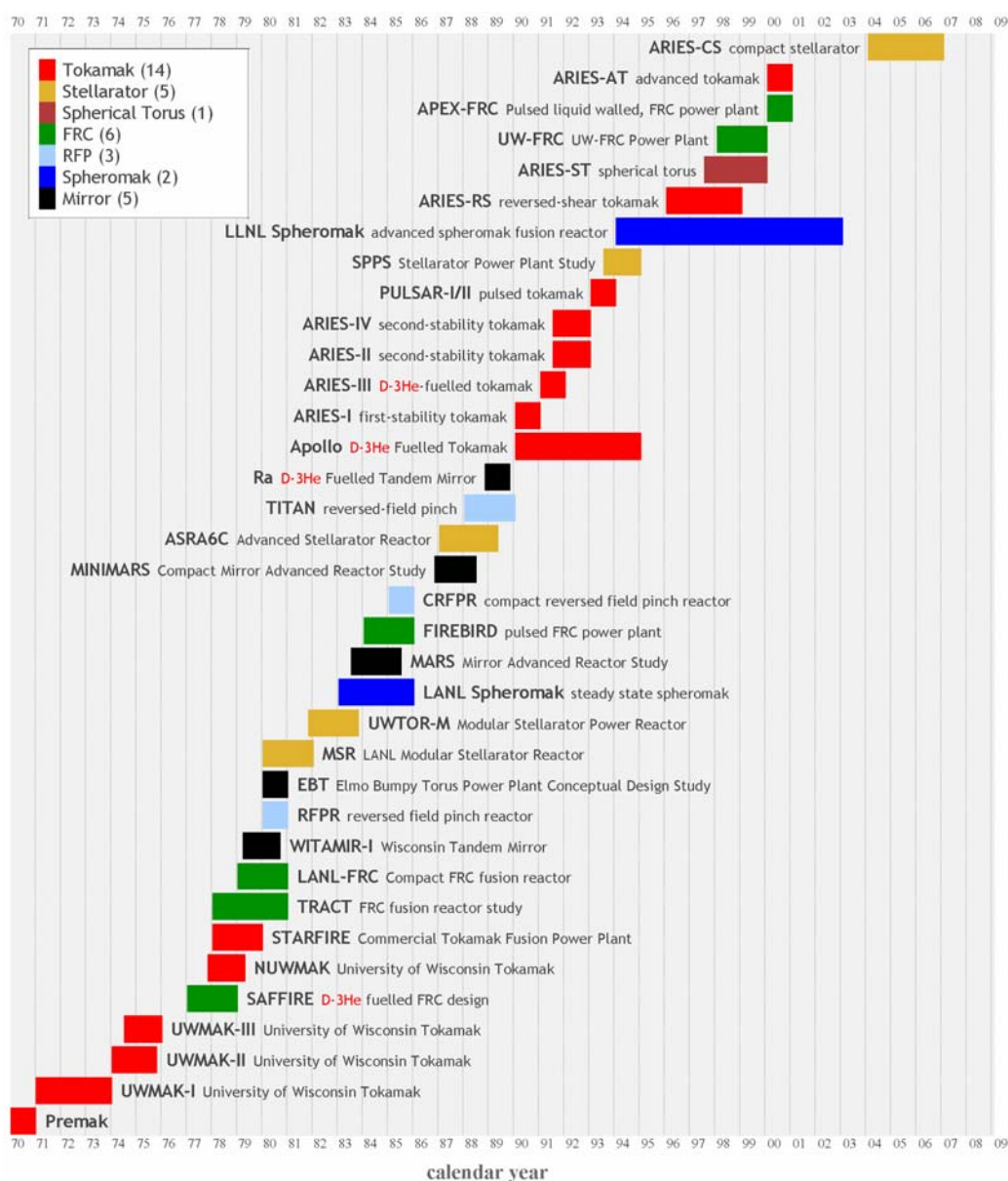


Figure 1. Timeline of large-scale US conceptual power plant designs.

While there are numerous reviews of fusion physics and technology research [1-11], comprehensive reviews of the conceptual power plant designs are scarce. The prime goal of this chapter is to fill this gap and address the engineering aspects of the magnetically confined fusion concepts developed to date. Furthermore, it provides a guide to design criteria for advanced designs, highlights the emerging design issues, identifies technological challenges, and presents a perspective on tokamaks as well as the major alternate toroidal and linear configurations. Throughout the chapter, there are additional materials on the evolution of the various concepts that address the basic issues and concerns for fusion science and technology.

Besides focusing on the US designs, the most recent international power plant designs are covered briefly. Furthermore, the early generations of fusion power plants were acknowledged for undertaking a noteworthy work. Section 2 presents the motivation for developing advanced fusion concepts as a viable energy source. Section 3 covers the history and development of tokamaks (Section 3.1), stellarators (Section 3.2), spherical tori (Section 3.3), field-reversed configurations (Section 3.4), reversed-field pinches (Section 3.5), spheromaks (Section 3.6), and tandem mirrors (Section 3.7). Lastly, a brief coverage of the roadmap to fusion power is given in Section 4.

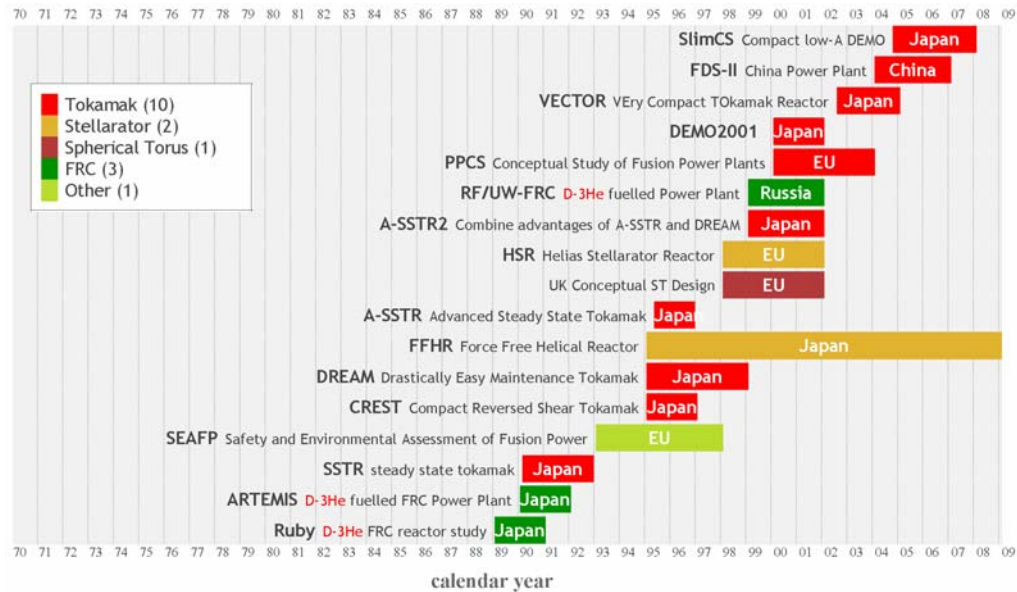


Figure 2. Timeline of selected power plants designed recently in EU, Japan, China, and Russia.

2. Mission and Main Features of Fusion Power Plants

Power plant studies help the fusion community and the funding agencies understand the major design problems and provide guidance for the R&D program to deliver an attractive and viable end product. Any fusion power plant must be safe, reliable, economically competitive, maintainable, environmentally attractive, and meet public acceptance. The philosophy adopted in international fusion power plant designs varies widely in the degree of physics extrapolation, technology readiness, and economic competitiveness. Some designs are viewed as a roll-forward step with a modest extrapolation beyond the International Thermonuclear Experimental Reactor (ITER) [12], yielding large devices with low power density and unfavorable economics, environmental impact, and perhaps safety concerns. This approach assumes that fusion is needed to fill an energy deficiency and the cost of electricity is not a dominant factor. Other designs suggest using the cost of electricity with its underlying factors as a figure of merit measured against competing energy sources. The latter approach would mandate advanced physics, high performance hardware with related future technologies to be competitive. Clearly, a balance must be achieved regarding the market and

technology forces that will spell success or failure for a future energy source. Self-consistency is an essential element of any credible fusion conceptual design assessment. As such, most fusion design study teams are multi-institutional, involving experts in plasma physics, neutronics, magnets, materials, heat transfer, power conversion, maintenance, safety, and economics.

High power density machines are compact and could provide many of the positive attributes outlined above, but require higher magnetic fields, more complex coils, and advanced physics, such as high beta (plasma pressure/magnetic field pressure), coupled with advanced technology, such as an innovative first wall design that can operate with $> 5 \text{ MW/m}^2$ neutron wall loading, an advanced divertor system that can withstand $> 10 \text{ MW/m}^2$ heat flux, radiation-resistant low-activation structural materials that can handle $> 200 \text{ dpa}$, and high field magnets that can provide $\geq 16 \text{ T}$ [13]. Fusion magnets will employ Nb_3Sn and/or superconductors as opposed to resistive Cu magnets that require enormous amounts of recirculating power (30-40%) and decrease the net electricity production. High-temperature superconductors are being developed that offer higher fields and lower cryogenic demands [14,15]. In most designs, the coils are protected from the energetic neutrons by the in-vessel components: blanket, shield, and vacuum vessel [16,17,18,19]. The blanket contains lithium compounds to breed tritium for fuel self-sufficiency. The blanket and other power core materials convert the nuclear energy into thermal energy that is transported to the thermal/electrical conversion system [20-22]. The external heat transfer system must be able to efficiently handle high temperature heat transfer media ($700\text{-}1100^\circ\text{C}$) in concert with advanced energy conversion concepts. Advanced low-cost fabrication techniques can be developed for all components to enhance the initial capital and recurring costs [23,24]. Reliability and maintainability are immensely important as the plants will be very expensive and they must run continuously for long periods of time. High reliability is especially important to minimize the number of unscheduled outages and extend the operational periods. Replaceable power core components must be durable and radiation resistant to achieve long service periods between scheduled outages. The plants should be designed around how to maintain the power core in as short a time as possible to minimize the downtime and maximize the productive operational time [25-29]. References 25-29 highlight the importance of designing a spectrum of tokamaks and other magnetically confined power plants to be highly maintainable in order to achieve high availability and low operational cost.

The challenging fusion environment (14 MeV neutrons, high heat flux, thermo-mechanical stresses, and chemical compatibility issues) mandates employing advanced low-activation structural materials (ferritic steels, vanadium alloys, and SiC/SiC composites) to assure the successful development of economical fusion energy. An advanced materials R&D program has been progressing worldwide for decades to achieve the desired materials characteristics [30]. All fusion materials should contain benign alloying elements and extremely low level of impurities to achieve very low radioactivity, allowing clearance and/or recycling of all fusion components [31]. Methods of obtaining highly pure materials need to be improved at a very competitive cost.

Highly elongated (vertical) plasmas are naturally unstable to vertical motions, but a conducting shell can slow down the instability and active feedback coils can stabilize the vertical plasma motions. Integrating these stabilizing elements with the in-vessel components and the plant control system represents a challenging engineering task. It is highly likely that commercial fusion machines will operate in a steady-state mode to eliminate the cyclic

fatigue induced in all systems by pulsed operation [32-34]. The cyclic fatigue could limit component lifetimes. Steady-state fusion plants can operate successfully for 50 years or more with regular power core component replacements. Inner power core components, such as the blanket, divertor and other plasma facing components, will have a radiation damage limit and must be replaced every few years (now expected to be in the 3-5 y range). The outer power core components (shield, vacuum vessel, and magnets) are shielded to large degree by the inner components and should be designed to operate reliably for the entire plant lifetime [16-19]. Plant availability must exceed 90% in order for fusion to compete economically with other energy sources in the time frame for fusion introduction. Rapid and reliable maintenance schemes with radiation-resistant remote handling equipment, highly reliable tools, and system health monitoring technologies will support the high availability goal [25-29].

Numerous studies have predicted that fusion potentially has favorable safety and environmental features [35-38]. Top-level safety objectives have been defined to assure public and worker safety (to protect individuals and ensure the likelihood of accidents is small and consequences are bounded within the premises), no need for an evacuation plan during accidents, and an attractive low-level waste reduction scheme through innovative designs and recycling/clearance. The means to minimize the radioactivity level has been developed through choice of low-activation materials, such as ferritic steels (FS), vanadium alloys, and SiC/SiC composites.

Besides safety and environmental attractiveness, economics remains an important consideration [39-45]. According to researchers in various countries, fusion is cost-effective compared to other energy sources, particularly when external costs are added to the cost of electricity [46,47]. There is a need to develop low-cost techniques [23,24] to fabricate power core components as existing techniques are too expensive and may not be able to handle the complex shapes that are necessary (ala stellarators). Recent studies [13,48] continue to indicate an economical power plant should deliver at least 1 GW_e of net output power. Larger sizes (> 1 GW_e) are more economical due to economy of scale, but would present higher financial risk for utilities and more complexities for integrating and handling multi-GW sources [49].

In summary, a few general remarks can be made regarding the mission of fusion power plant studies:

- Perform self-consistent integrated designs, stressing constructability, fabricability, operability, and maintainability of fusion power plants
- Focus on practicality, safety, and economic competitiveness of fusion power
- Involve experts in plasma physics, neutronics, magnets, materials, heat transfer, power conversion, maintenance, safety, and economics, requiring multi-institutional design teams
- Uncover physics and technology problems
- Help fusion community and funding agencies understand major design issues
- Guide R&D program to deliver attractive and viable end product.

Studies developed to date defined the key features of an attractive tokamak power plant:

- Must be safe, reliable, maintainable, economically competitive, environmentally attractive, and meet public acceptance.

-
- Fusion has favorable safety and environmental features:
 - Low-activation materials to minimize radioactivity
 - Assurance of public and worker safety
 - No need for evacuation plan during accidents
 - Low-level wastes that are recyclable and clearable
 - Radwaste reduction scheme through compactness, blanket segmentation, permanent components, and recycling/clearance.
 - High power density machines are compact and economical, but require advanced physics and technology:
 - Innovative first wall design operating $> 5 \text{ MW/m}^2$ neutron wall loading
 - Advanced divertor system withstanding $> 10 \text{ MW/m}^2$ heat flux
 - Radiation-resistant structural materials handling $> 200 \text{ dpa}$
 - High field magnets providing $> 16 \text{ T}$:
 - Superconductors as opposed to resistive Cu magnets (that require enormous amounts of recirculating power and decrease net electricity production)
 - High-temperature superconductors are being developed offering higher fields and lower cryogenic demands.
 - Advanced power plants could compete economically with other energy sources, but mandate:
 - Steady-state operation to eliminate cyclic fatigue induced in all systems by pulsed operation.
 - Breeding blanket converting nuclear energy at high temperature $> 700^\circ\text{C}$.
 - External heat transfer system handling high temperatures ($700\text{-}1100^\circ\text{C}$) with high energy conversion efficiency (40-60%).
 - Successful operation for > 50 years with regular blanket and divertor replacements (every 3-5 years).
 - Outer components (shield, vacuum vessel, and magnets) operating reliably for entire plant lifetime.
 - Advanced low-cost fabrication techniques for all components to enhance economics.
 - High availability $> 90\%$:
 - Highly reliable components to minimize unscheduled outages
 - Power core maintenance in short time to minimize downtime.
 - Other features include:
 - Power plant should deliver at least 1 GW_e of net output power
 - Larger sizes ($> 1 \text{ GW}_e$) are more economical, but present higher financial risk for utilities and more complexities for integrating and handling multi-GW sources
 - Fusion is cost-effective, particularly when adding external costs to cost of electricity.

3. Magnetic Fusion Concepts

Over 40 years, since the inception of the large-scale fusion designs in the early 1970s, the worldwide fusion community has gone from an almost exclusively tokamak concept to a variety of alternate magnetic configurations such as stellarators, ST, FRC, RFP, spheromaks, and tandem mirrors, aiming at developing safe, environmentally attractive, and economically competitive fusion power plants for the 21st century.

3.1. Tokamaks

Internationally, the tokamak concept is regarded as the most viable candidate to demonstrate fusion energy generation. Since the 1960s, the fusion program around the world has been dominated by the tokamak – a donut configuration invented in 1951 by Russian physicists Sakharov and Tamm while named a few years later by Golovin. Tokamak is an acronym from the Russian phrase “Toroidal Chamber with Magnetic Coil.” In 1956, the first tokamak experiment started in Kurchatov Institute, Moscow. Ever since, the confinement concept has been successfully demonstrated with more than 100 worldwide experimental facilities, of which ~35 experiments are currently operational in Russia, US, EU, Japan, South Korea, China, India, and other countries [50]. Over the years, strong domestic and international experimental programs addressed the tokamak physics and technology issues. The collaborative worldwide effort materialized in the design and construction of ITER [12] – a large burning plasma experiment that will produce ~500 MW of fusion power. ITER is being designed, constructed, and operated by a consortium of seven parties: EU, Japan, US, Russia, China, South Korea, and India. France has been chosen as the site for ITER with construction starting around 2011 and first plasma in 2018.

The tokamak plasma is confined by a large set of equally spaced coils: typically 16 toroidal field coils and approximately 10 poloidal field coils. A second set of divertor, equilibrium field, and central solenoid coils are necessary to shape and position the plasma within a toroidal vessel of elliptical cross section. A tokamak feature is a flowing current through the plasma that generates a helical component of the magnetic field for plasma stability. Tokamaks are capable of reaching steady-state operating conditions using current drive systems. Once the plasma ignites, the alpha particles from the fusion reaction provide nearly all the plasma heating. All tokamaks employ divertors in single or double null configurations to collect the lost alpha particles and plasma ions and electrons. The components surrounding the plasma must breed tritium, convert the neutron energy into thermal energy, protect the magnets and other external components, and accommodate the plasma stabilizing shells.

3.1.1. US Tokamak Power Plant Studies

Numerous tokamak conceptual design power plant studies were developed over the last four decades to assess the viability of different approaches and recommend productive R&D directions. In the US, the conceptual studies progressed steadily from the early 1970s pulsed UWMAK series [51-54], to STARFIRE [55] (1980) that first promoted steady-state current drive, to the more advanced 1990s steady-state ARIES series [56-58,48,13]. The earlier

designs of the 1970s demonstrated how fusion plants could be designed and operated, but also uncovered undesirable aspects of pulsed operation with an energy storage system, a low power density machine, plasma impurity control problems, and maintainability issues. Nevertheless, these pioneer studies contributed significantly to the basic understanding of the field of fusion power plant design and technology. In fact, many of the proposed 1970s technologies are still considered in recent fusion designs: 316-SS structure, Li and LiPb liquid breeders, LiAlO₂ solid breeders, Be multiplier, helium and water coolants, NbTi and Nb₃Sn superconductors, low-Z liner for first wall and solid divertor, liquid Li divertor, and remote maintenance.

Table I. Key parameters of STARFIRE and ARIES steady-state power plants

Power Plant	STARFIRE	ARIES-I	ARIES-II	ARIES-IV	ARIES-RS	ARIES-AT
	[55]	[56,57]	[58]	[58]	[48]	[13]
Fusion Power (MW)	3510	1926	1910	2024	2167	1760
Net Electric Power (MW _e)	1200	1000	1000	1000	1000	1000
Major Radius (m)	7	6.75	5.6	6.04	5.52	5.5
Minor Radius (m)	1.94	1.5	1.4	1.51	1.38	1.3
Aspect Ratio	3.6	4.5	4	4	4	4
Ave. Toroidal Beta	6.7%	1.9%	3.4%	3.4%	5%	9.2%
Max. Field at Coil (T)	11.1	21	15.9	15.9	15.8	11.1
Number of TF Coils	12	16	16	16	16	16
Structural Material	PCA*	SiC/SiC	V	SiC/SiC	V	SiC/SiC
Blanket Type	LiAlO ₂ /H ₂ O/Be	Li ₂ ZrO ₃ /He/Be	Li	Li ₂ O/He/Be	Li	LiPb
Average NWL [#] (MW/m ²)	3.6	2.5	2.9	2.7	3.96	3.3
Thermal Conversion Efficiency	36%	49%	46%	49%	46%	59%
System Availability	75%	76%	76%	76%	76%	85%
COE ⁺ (mills/kWh)	110	87	76	68	76	48

* Primary Candidate Alloy – an advanced austenitic stainless steel.

[#] Neutron wall loading (NWL) evaluated for first wall at separatrix passing through the X point.

⁺ in 1992 US dollars.

Beginning in the 1980s with the STARFIRE design [55] and continuing to the present, more in-depth power plant studies were initiated in the US and abroad to identify, understand, and resolve the physics and technology challenges of tokamaks. While these studies proposed solutions for many problems, they uncovered other areas that needed further assessment and development, such as disruption control, current drive technology, high heat flux divertor design, and high temperature blankets. The key engineering parameters of the STARFIRE design, led by Argonne National Laboratory, are listed in Table I while Fig. 3 displays the principal features of the design. STARFIRE represented the first tokamak power plant that operated in a steady-state, current drive mode without an energy storage system. Other STARFIRE design features include advanced physics, lower-hybrid current drive, beryllium-coated first wall (FW), modular water-cooled solid breeder blanket, attractive safety

program. Improvements were apparent, progressing from ARIES-I to ARIES-AT and all studies stressed the practicality, safety, and economic competitiveness of fusion power. At the outset, the ARIES design process took into consideration the fabricability, constructability, operability, and maintainability of the machine. The physics, engineering, and economics proceeded interactively while the ARIES systems code determined the reference parameters by varying the physics and engineering parameters, subject to pre-assigned physics and technology limits, to produce an economic optimum plant design.

Each ARIES design was conceived to consider various options that may provide an improvement; basically, a technical sounding board that helps judge the viability of different physics and engineering concepts. The first design of the ARIES series (ARIES-I) operates in the first-stability plasma regime – the closest to the present database – with ceramic breeder blanket and SiC/SiC composites as the main structure. The ARIES-I physics and engineering models were updated repeatedly to obtain meaningful comparisons with later designs [57]. Even though the high-field toroidal field coils and high thermal conversion efficiency improved the attractiveness of this first-stability regime of operation, ARIES-I did not satisfy the economic requirement. The second-stability regime of ARIES-II/IV [58] had better performance, but the experimental database for this physics regime is very limited. Two blanket options were examined in ARIES-II/IV: liquid lithium with vanadium structure and lithium oxide ceramic breeder with SiC/SiC composites. Next, the ARIES-RS study [48] with reversed-shear (RS) plasma and Li/V blanket offered similar economic performance. Nevertheless, the physics database for this RS regime, while small, is growing rapidly. The advanced tokamak (AT) plasma confinement regime was incorporated in the last ARIES-AT design [13] to assess the physics and technology areas with the highest leverage for achieving attractive and competitive fusion power. Indeed, ARIES-AT demonstrated superior performance and benefited greatly from several developments: new SiC/LiPb blanket operating at high temperature ($\sim 1000^{\circ}\text{C}$) with high thermal conversion efficiency (59%), high system availability (85%) with an efficient horizontal maintenance scheme, and 9% toroidal beta (compared to 5% beta of ARIES-RS). Noteworthy is that moving the stabilizing shells inward between the outboard blanket segments helped increase the beta significantly. In all ARIES designs, the cost of electricity (COE) involves capital costs, operating costs, financial costs, power core energy production (energy capture, energy multiplication, high temperature operation), thermal to electrical energy conversion efficiency, plant recirculating power, and plant availability (reliability, maintainability, and inspectability). Even the safety and environmental impact as well as the radwaste management scheme have indirect influences on the COE. Improvements in any of these areas enhance the economic performance of the plant. The safety and environmental impact and the smart choice of a favorable radwaste management approach (such as recycling and/or clearance, avoiding disposal) also have other social impacts that will contribute to the attractiveness of any fusion power plant.

The STARFIRE and five ARIES tokamaks operated under the assumption that current drive is essential for the steady-state operation and economic feasibility of fusion power plants. The design intent is based on the strong recommendations from the STARFIRE and ARIES industrial advisory committees for steady-state operation. Also the desire to achieve high levels of reliability suggests that steady state operation would minimize fatigue related failures. Economics of the on-site energy storage also proved to be prohibitive. In the mid 1990s, the ARIES team re-evaluated the pulsed operation of an inductively driven tokamak (PULSAR [32,33]) to determine the advantages and drawbacks of operating in a pulsed mode

using current plasma physics and updated engineering designs. The main advantage of pulsed operation would be the elimination of the expensive and inefficient current drive system. Besides the need for an efficient energy storage system, many of the plant systems suffer from cyclic thermal and mechanical fatigue, particularly the plasma facing components, toroidal and poloidal field magnets, and pumps and valves. The magnet fatigue was identified as the most critical issue calling for hefty structure to support the magnets (refer to Fig. 3). PULSAR-I and -II employed the ARIES-IV and ARIES-II blanket systems, respectively, and suggested utilizing the outboard shield for thermal energy storage to provide the 1000 MW_e output between pulses (downtime of ~3 minutes). The smart choice of the largest fusion power core component for energy storage helps buffer the thermal oscillations, assuring steady conditions at the steam generator and grids. PULSAR large major radius approaches 8 m and its economic performance is poor (COE > 100 mills/kWh) primarily due to cost penalties associated with pulsed operation. Clearly, pulsed tokamaks cannot compete with steady-state systems due to the added complexity of energy storage, fatigue of critical power systems, and poor economics.

3.1.2. Recent European, Japanese, and Chinese Tokamak Power Plant Studies

International institutions, particularly in Europe, Japan, and China, carried out a number of tokamak fusion power plant studies. There are technical similarities and differences between these studies. Some of the underlying differences are related to strategic objectives and technology readiness. For instance, the emphasis given to the economic competitiveness of power plants varies significantly between countries. The US is highly motivated to obtain a fusion power plant that is at least as economically competitive as other available electric power sources. On the other hand, Europe and Japan take the view that the first generation of fusion power plants will enter the energy market because of the major safety and environmental advantages and large fuel reserve, even if they produce electricity at a somewhat higher cost.

During the 1990s, a series of EU studies delivered two reports [60,61] on the safety and environmental assessment of fusion power (SEAFP). As the 2000s began, Cook [44] and Ward [45] summarized the prospects of fusion energy that offers acceptable internal cost and very low external costs and makes an economically acceptable contribution to the future energy mix. The lessons learned from these studies were applied to the successor EU study of commercial power plants: European Power Plant Conceptual Study (PPCS) [62]. This 4-year study focussed on five models that spanned a wide range of near-term and advanced physics/technology tokamaks operating in steady-state mode. As Table II indicates, the models deliver ~1500 MW_e output power, but differ substantially in plasma parameters, size, fusion power, materials, blanket and divertor technologies, breeding capacity, economic performance, and safety and environmental impacts. Models A, AB, and B are considered near-term concepts while Models C and D are more advanced concepts. Figure 4 displays an isometric view of the Model C power plant. The tenth-of-a-kind cost of electricity for all models is thought to be competitive with other sources of energy obtained from the literature. In addition to this valuable comparison, the PPCS study highlighted the need for specific R&D activities as well as the need to establish the basic features of the Demo [63] – a device that bridges the gap between ITER and the first-of-a-kind fusion power plant.

Table II. Key parameters of recently developed power plants in Europe [62]

PPCS Model	A	AB	B	C	D
Fusion Power (MW)	5000	4290	3600	3410	2530
Net Electric Power (MW _e)	1546	1500	1332	1449	1527
Major Radius (m)	9.55	9.56	8.6	7.5	6.1
Minor Radius (m)	3.18	3.18	2.87	2.5	2.03
Aspect Ratio	3	3	3	3	3
Max. Field at Coil (T)	13.1	13.4	13.2	13.6	13.4
Structural Material	LAFS*	LAFS	LAFS	LAFS	SiC/SiC
Blanket Type	LiPb [#] /H ₂ O	LiPb [#] /He	Li ₄ SiO ₄ /He/Be	LiPb ⁺ /He	LiPb ⁺
Overall Tritium Breeding Ratio	1.06	1.12	1.13	1.15	1.12
Average NWL (MW/m ²)	2.2	1.8	2	2.2	2.4
Peak Divertor Heat Load (MW/m ²)	15	10	10	10	5
Pumping Power (MW)	110	400	375	87	12
Plant Net Efficiency ^{&}	32%	35%	37%	42%	60%
Cost ⁺⁺ (Euro Cents/kWh):					
Internal [@]	5-9	5-9	4-8	4-7	3-5
External ^{**}	0.09	0.08	0.07	0.06	0.06

* Low activation ferritic steel: Eurofer.

Stagnant LiPb.

+ Circulated LiPb.

& Ratio of net electric power output to fusion power.

++ in 2004 Euros.

@ Tenth-of-a-kind and First-of-a-kind.

** Accounts for cost associated with environmental damage or adverse impacts on health.

In Japan, several studies have been made of tokamak power plants, including the SSTR series [64-68], DREAM [69], CREST [70], and VECTOR [71]. Some of these designs can be broadly classified into two categories [72] according to the peak magnetic field ($B_{\max}$) at the magnet and normalized beta (β_N):

- High field tokamaks ($B_{\max} > 16$ T): SSTR [64,65], A-SSTR [66], and A-SSTR2 [67,68]
- High beta tokamaks ($\beta_N > 5$): CREST [70]

SSTR [64,65] is a pioneer Japanese study developed in the early 1990s and aimed at achieving high power density through high magnetic field at the magnet (16.5 T). The blanket employs the F82H ferritic steel structure and water-cooled Li₂O breeder with a beryllium multiplier. Two more advanced studies followed in 1995 and 1999, recommending higher magnetic fields of 20.5 T at the 20 K magnets of A-SSTR [66] and 23 T for A-SSTR2 [67,68]. SiC/SiC composites, Li₂TiO₃ breeder, and Be₁₂Ti multiplier are the materials of choice for A-SSTR2. A major design challenge for such a high magnetic field approach is the

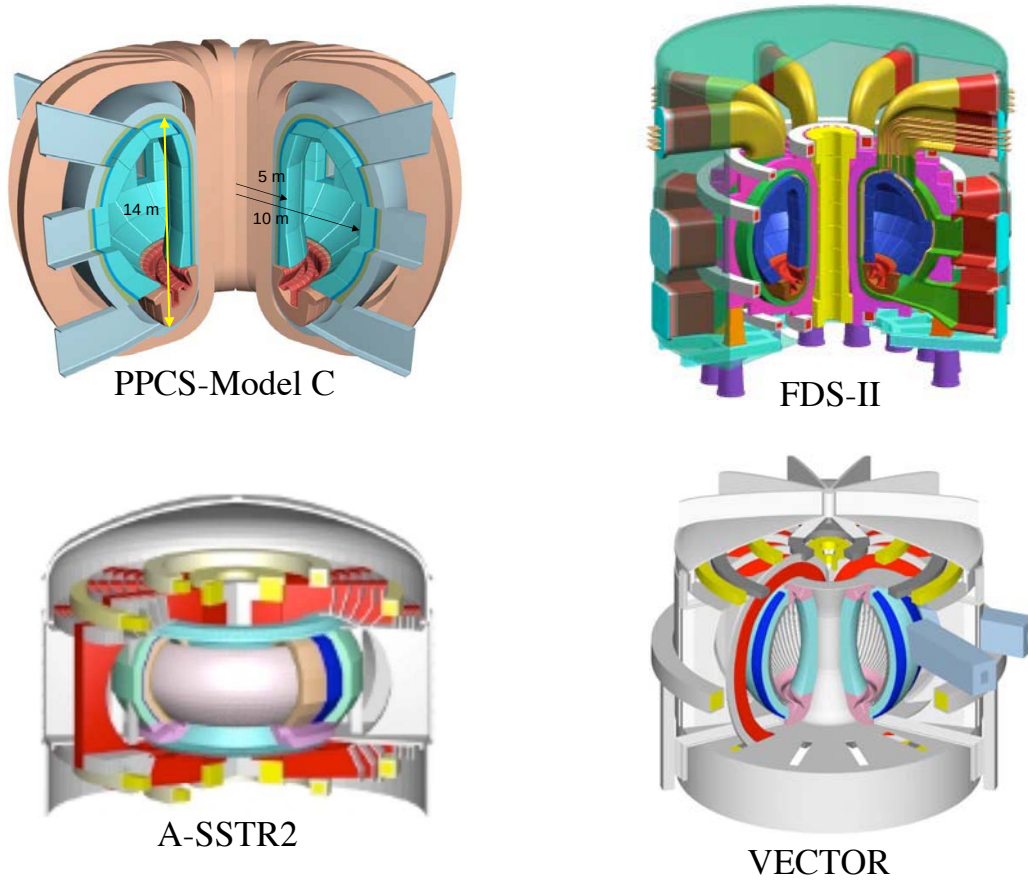


Figure 4. Isometric views of selected EU, Japanese, and Chinese power plants (not to scale).

sizable magnet structure needed to support the enormous electromagnetic force on the TF coils. In A-SSTR2, a space is provided for the supporting structure by eliminating the central solenoid. In the mid-1990s, the DREAM study [69] promoted the approach of easy maintenance to achieve high overall availability and thus reduce the cost of electricity. With 12 toroidal sectors and a high aspect ratio (A) of 6, an entire sector can be pulled out radially between the outer legs of toroidal field (TF) coils – a similar approach to the ARIES-RS and ARIES-AT maintenance designs. Because of the high A , there is ample space (~ 1 m) between the inboard legs of the 12 TF coils to easily accommodate all pipes and supply lines. The successor CREST study [70] adopted DREAM's maintenance philosophy, but with 14 TF magnets and 14 sectors. CREST is compact with moderate A of 3.4 and operates in the reversed shear mode.

As Table III indicates, the most recent VERY Compact TOkamak Reactor (VECTOR) [71], shown in Fig. 4, is even more compact with A of 2, β_N of ~ 4 , major radius of 3.75 m, no central solenoid coils, no inboard blanket, ~ 0.7 m thick inboard shield, Li/Be/SiC outboard only blanket, and high field Bi2212/Ag/AgMgSb superconducting TF magnets operating at 20 K with low stored energy. In this regard, VECTOR combines two distinct features: the high $B_{\max}$ of A-SSTR and high β_N of CREST. These salient features were recently

incorporated in the design of a compact DEMO (SlimCS) with low A of 2.6, β_N of 4.3, 16.4 T at magnet, and slim central solenoid, offering physics and engineering advantages [72]. In addition, Demo-CREST [73] has also been proposed as an alternate Demo based on the CREST approach.

Table III. Key parameters of recently developed power plants in Japan and China

Power Plant	CREST	A-SSTR2	VECTOR	FDS-II
	[70]	[67,68]	[71]	[75]
Fusion Power (MW)	2930	4000	1800	2500
Net Electric Power (MW _e)	1000	2550	1000	1200
Major Radius (m)	5.4	6.2	3.75	6
Minor Radius (m)	1.6	1.5	1.4	2
Aspect Ratio	3.4	4.1	2	3
β_N	5.5	4	3.75	5
Max. Field at Coil (T)	12.5	23	19.6	10
Number of TF Coils	14	12	12	16
Structural Material	Ferritic Steel	SiC/SiC	SiC/SiC	CLAM steel [#]
Blanket Type	Water Cooled Blanket	Li ₂ TiO ₃ /Be ₁₂ Ti/He	Li/Be	LiPb/He/steel [#]
Average NWL (MW/m ²)	5.05	6	3.5	2.6
Thermal Conversion Efficiency	35%			47%
System Availability	75%			75%
COE (mills/kWh)				63-105 ⁺

[#] China Low Activation Martensitic steel.

⁺ Range for 60-85% capacity factor and 0.5-2 uncertainty factor in price of blanket materials.

In China, fusion is highly regarded as a prominent energy source due to the rapid economic growth. As such, a series of FDS fusion power plants [74] has been developed over the past 10 years covering hybrid tokamak to transmute fission products and breed fissile fuels (FDS-I), electricity generator (FDS-II), hydrogen producer (FDS-III), and hybrid spherical tokamak (FDS-ST). The FDS-II single-null tokamak design [75] (refer to Fig. 4 and Table III) is based on advanced plasma parameters and employs LiPb breeder and reduced-activation ferritic steel structure. The dual-cooled LiPb/He blanket is the reference with 700°C LiPb outlet temperature while the He-cooled quasi-static LiPb blanket is the back-up with 450°C He outlet temperature. The FDS-II configuration is designed with modularized blankets to alleviate the thermal stress and impact of electromagnetic force caused by plasma disruption. The 240 blanket modules and divertor cassettes are maintained through equatorial and lower ports, respectively. A set of preliminary assessments indicated the conceptual design satisfies the FDS-II requirements in terms of tritium breeding, mechanical performance, fabricability, maintainability, safety, and economics.

3.1.3. D-³He Fueled Tokamak Power Plant Studies

The D-³He fueled fusion concepts [76] offer advantages, such as no tritium breeding blanket, permanent components, and possibility to obtain electrical power by direct energy conversion of charged particles [77,78]. However, the D-³He plasma has its own set of issues and concerns, such as the availability of ³He [79,80] and the attainment of the higher plasma parameters that are required for D-³He burning. Moreover, the D-³He cycle is not completely aneutronic, requiring shielding components to protect the magnets and externals [81,82]. It has a very low presence of energetic fusion neutrons, due to side D-D reactions generating 2.45 MeV neutrons and T and the side D-T reactions generating 14.1 MeV neutrons.

Several studies have addressed the physics and engineering issues of D-³He fueled power plants. As advanced fuel cycles are more suitable for devices with high beta and high magnetic field, most D-³He efforts of the 1970s and 1980s focused on innovative confinement concepts, such as the field-reversed configuration, tandem mirrors, and Ring Trap concept. In the early 1990s, the University of Wisconsin developed a series of tokamak-based D-³He Apollo designs [83-86] while the D-³He ARIES-III tokamak [87] was developed within the ARIES project. The Apollo series and ARIES-III, shown in Fig. 5, postulated the reduction in radioactivity and the advantageous effect of the D-³He safety characteristics [88-92] that include low activity and decay heat levels, low-level waste, and low releasable radioactive inventory from credible accidents. References 93-95 address the pertinent issues of utilizing today's technology and a strategy for D-³He fusion development.

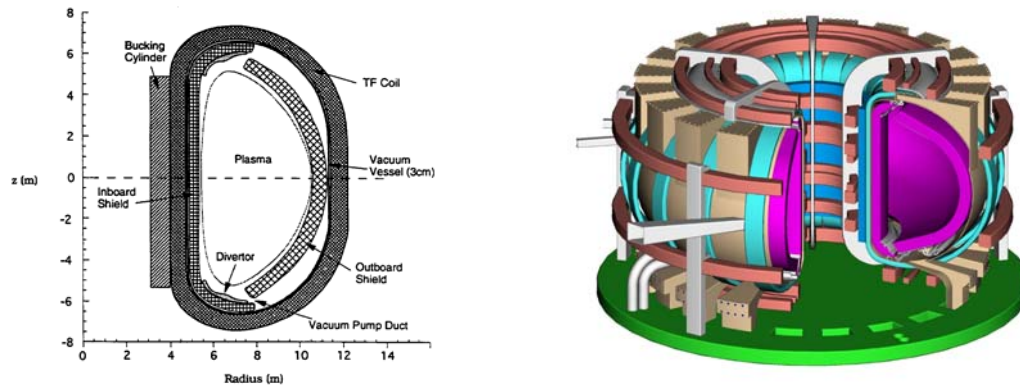


Figure 5. Vertical cross section of Apollo and isometric view of ARIES-III.

3.1.4. Summary

Power plant studies performed worldwide over the past 50 years indicated the tokamak is a highly promising concept for producing large-scale fusion power. These studies have also helped shape the R&D directions for improved physics and engineering. This view of tokamaks is currently shared by many nations and is reflected in the support for the design, construction, and operation of ITER [12]. ITER will help the transition from the present fusion experimental basis toward the goal of electricity-producing power plants. At present, the tokamak concept is well funded and more advanced than any other fusion concept. Undoubtedly, additional theoretical and experimental research into the tokamak confinement

concept will continue at the national and the international levels to resolve emerging physics and technology issues necessary to produce a viable fusion power plant.

3.2. Stellarators

The stellarator concept was one of the first approaches proposed by Spitzer in 1950 for obtaining a controlled fusion device. The first device was built in the US in 1951 at Princeton. Reference 96 covers the historical development of worldwide stellarator research from 1950 to 1980. The prime interest in stellarators stems from their potential physics advantages over tokamaks. Stellarators are inherently steady-state devices with no need for large plasma current, no external current drive, no risk of plasma disruptions, low recirculating power due

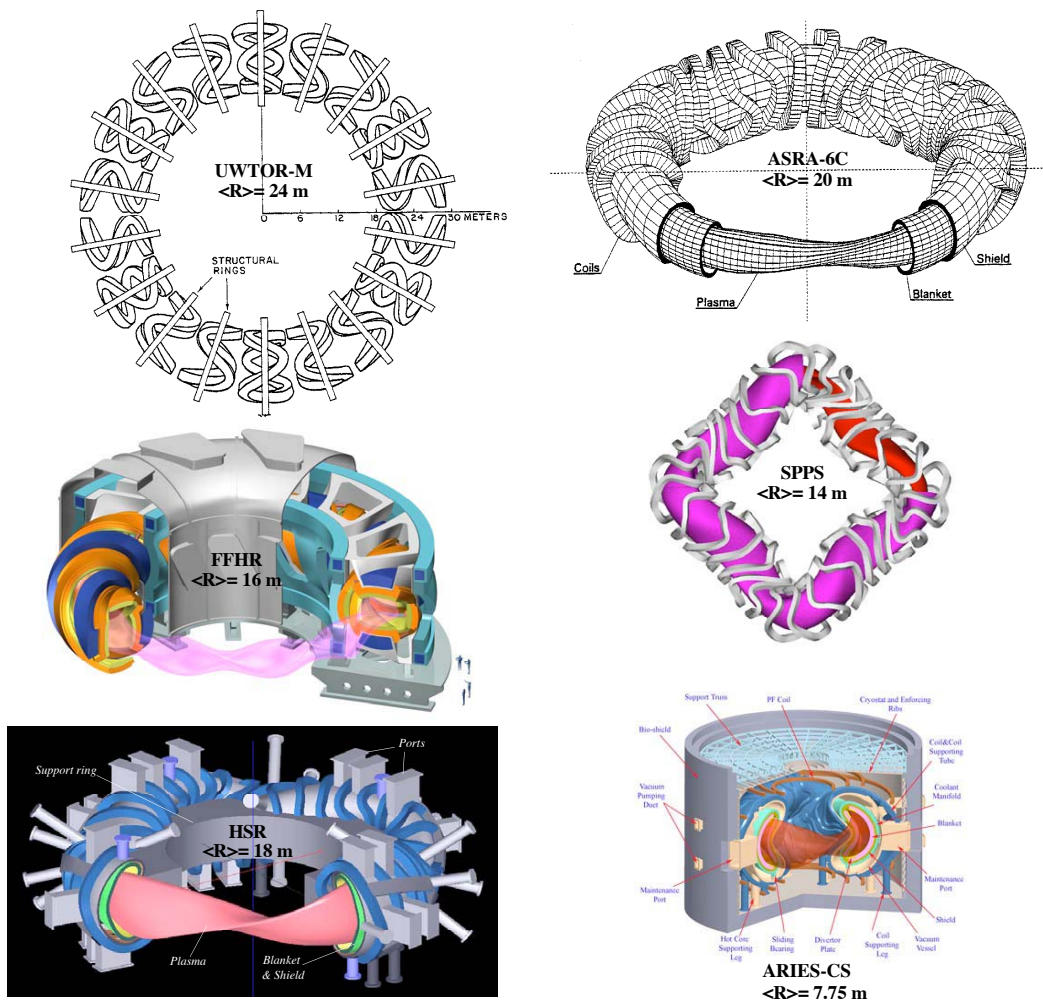


Figure 6. Isometric view of the six recent stellarator power plants developed in the US, Europe, and Japan (not to scale).

to the absence of current-drive requirements, and no instability and positional control systems. For these attractive features, stellarator power plants have been studied for decades in the US, Europe, and Japan to optimize the design parameters and enhance the physics and engineering aspects. Recently, compactness was promoted as an economic advantage for future stellarators, allowing direct comparisons with tokamaks.

Numerous stellarator experiments have been constructed worldwide in the US, Germany, Japan, Russia, Spain, Korea, and Australia. In addition to seven worldwide smaller experimental devices, the operational HSX experiment in the US [97], the performance-class LHD experiment in Japan [98], and the Wendelstein 7-X under-construction experiment in Germany [99] are capable of approaching fusion-relevant conditions comparable to those attained in today's large tokamaks. In 2008, the US decided to terminate the NCSX compact stellarator experiment [100] that is being assembled at the Princeton Plasma Physics Laboratory.

Although the stellarator concept has been around for several decades, very little in the way of conceptual design studies has been performed compared to tokamaks, of which many studies have taken place in the US and abroad. During the decade of the 1980s and continuing to the present, six large-scale stellarator power plants have been developed: UWTOR-M [101,102], ASRA-6C [103], SPPS [104], and ARIES-CS [105] in the US, and the most recent HSR [106,107] in Germany and FFHR [108] in Japan. The six studies vary in scope and depth and encompass a broad range of configuration options as shown in Fig. 6. The timeline of these studies is given in Fig. 7.

The stellarator confines the plasma in a toroidal magnetic configuration in which controlled currents flowing in external coils produce vacuum flux surfaces with rotational transform. Thus, the plasma confining magnetic field is generated by numerous external coils rotating as they move around the torus. Unlike tokamaks, the coils are not equally spaced on the inboard and outboard for better plasma containment. In this regard, stellarators are clearly distinguished from tokamaks (and other toroidal concepts) that rely entirely on current flowing through the plasma for confinement. Moreover, stellarators cover a variety of configurations (helias, torsatron, heliotron, modular torsatron, etc.) and a wider range parameter space than tokamaks (e.g., 8-24 m major radius, 3-12 aspect ratio, 2-5 field periods, and 0.2-1.6 rotational transform), but such parameters are subject to numerous constraints. While the magnetic geometry of tokamaks is intended to be entirely symmetric in the toroidal coordinate, the magnetic field components of the stellarator vary in all three coordinates, deviating from toroidal symmetry. As such, the stellarator physics advantages could be offset by the more complex configurations, harder divertor designs, and challenging maintenance schemes. In most stellarator designs developed so far, the first wall (FW) and surrounding in-vessel components conform to the plasma and deviate from the uniform toroidal shape to reduce the machine size. The FW shape varies toroidally and poloidally, representing a challenging 3-D engineering problem and making the design of in-vessel components, overall integration process, and maintenance scheme more complex than for tokamaks. Nevertheless, interest in the stellarator concept increased over the years because of the remarkable advances in theory, experimental results, and construction techniques [109].

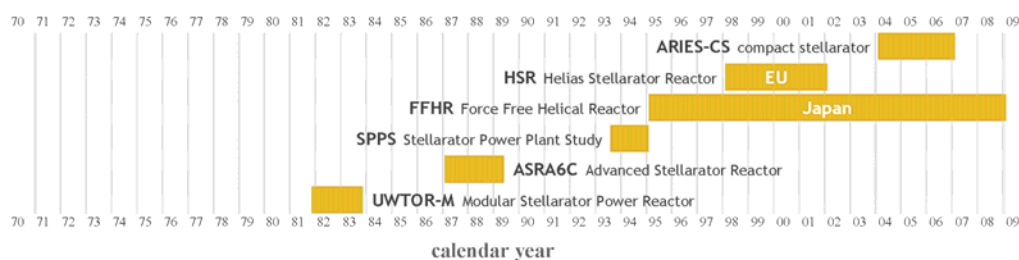


Figure 7. History timeline for large-scale studies of stellarator power plants.

In the 1970s, researchers were encouraged by the positive physics experimental results and the development of modular twisted coils that can replace the continuous helical coils (for torsatron and heliotron designs) – an early drawback of stellarator power plants that makes maintainability of blanket and coils extremely difficult [101]. In the early 1980s, the Los Alamos study of the Modular Stellarator Reactor (MSR) characterized parametrically the critical relationship between plasma, blanket/shield, coils, and overall power plant performance [110]. The first large-scale stellarator design [101,102] was developed by the University of Wisconsin (UW). UWTOR-M had 18 modular twisted coils with only two different coil geometries arranged in a toroidal configuration. Here, the non-planar modular coils were first used in 2 or 3 field period (FP) applications [111]. The blanket employs FS as the main structure and LiPb for cooling and tritium breeding. In the mid 1980s, the non-planar axis became more pronounced and non-planar modular coils became more complicated [111]. UWTOR-M was followed by the ASRA-6C study [103] that was performed in collaboration between UW and two German laboratories: FZK at Karlsruhe and IPP at Garching. All 30 coils of ASRA-6C and the internal components (FW, FS/LiPb blanket, and shield) have identical elliptical bores as shown in Fig. 6. Next came the Stellarator Power Plant Study (SPPS) [104] initiated in 1995 by the multi-institutional ARIES team to address key issues for stellarators based on the modular Helias-like Heliac approach. As Fig. 6 indicates, the baseline configuration has four field periods produced by 32 modular coils of four distinct types. Vanadium structure and liquid lithium breeder are the reference materials for SPPS. On the international level, a Helias Stellarator Reactor (HSR) study was initiated in Germany in the late 1990s based on the W7-X experiment. The most recent HSR4/18 design [106,107] has four FPs with 40 coils and LiPb/FS blanket. Alternatively, the stellarator configuration can be produced using continuous helical coils. An example of this approach is the ongoing Force Free Helical Reactor (FFHR) study in Japan [108]. Ferritic steel structure, Flibe breeder, and beryllium multiplier are the materials of choice for FFHR. Note that all designs developed to date employed liquid breeders (Flibe, LiPb, or Li) for breeding and cooling to cope with the complex geometry of stellarators.

The 1980s and 1990s stellarator studies led to large power plants mainly due to the relatively large aspect ratio and the design constraint imposed by the minimum distance between the plasma and coils. For instance, the UWTOR-M design [101,102] had an average major radius (R_{av}) of 24 m in a six FP configuration. Moving toward smaller sizes, the ASRA-6C study [103] suggested 20 m R_{av} with five FPs. The most recent German HSR4/18 study [106,107] proposed 18 m R_{av} with four FPs. The ARIES SPPS study [104] was the first step toward a smaller size stellarator, proposing 14 m R_{av} with four FPs. Japan developed a

series of FFHR designs [108], recently calling for 16 m R_{av} with ten FPs. After two decades of stellarator power plant studies, it was evident that a new design that reflects the advancements in physics and improvements in technology was needed. To realize this vision, the ARIES team launched the ARIES-CS study [105] to provide perspective on the benefits of optimizing the physics and engineering characteristics of the so-called compact stellarator power plant. The primary goal of the study is to develop a more compact machine that retains the cost savings associated with the low recirculating power of stellarators, and benefits from the smaller size and higher power density, and hence lower cost of electricity, than was possible in earlier studies. The benefit of the compact feature can be fully recognized when comparing ARIES-CS to all five large-scale stellarator power plants developed to date (see Table IV and Fig. 8). The most recent advanced physics and technology and innovative means of radial build design helped reduce the major radius by more than 3-fold, approaching that of advanced tokamaks.

In ARIES-CS, the principle of compactness drove the physics, engineering, and economics. The study aimed at reducing the stellarator size by developing a compact configuration with low aspect ratio (~ 4.5) along with a combination of advanced physics and technology and by optimizing the minimum plasma-coil distance (Δ_{min}) through rigorous nuclear assessment as Δ_{min} significantly impacts the overall size and cost of stellarator power plants.

Table IV. Key parameters of stellarator power plants

Power Plant	UWTOR-M [101,102]	ASRA-6C [103]	SPPS [104]	HSR [107]	FFHR2m2 [108]	ARIES-CS [105]
Fusion Power (MW)	4300	3900	1730	3000	3000	2436
Net Electric Power (MW _e)	1840	1620	1000		1300	1000
Number of Field Periods	6	5	4	4	10	3
Average Major Radius (m)	24	20	14	18	16	7.75
Average Minor Radius (m)	4.77	1.6	1.6	2.1	2.8	1.7
Aspect Ratio	14	12.5	8.5	8.6	5.7	4.5
Toroidal Beta	6%	5%	5%	5%	4.1%	6.4%
Max. Field at Coil (T)	11.6	10.4	14.5	10.3	13	15
Number of Coils	18	30	32	40	2 continuous	18
Structural Material	FS	FS	V	FS	FS	FS
Blanket Type	LiPb	LiPb	Li	LiPb	Flibe/Be	LiPb/He
Average NWL (MW/m ²)	1.4	1.4	1.3	1	1.3	2.6
Thermal Conversion Efficiency	40%	40%	46%	35%	37%	42%
System Availability			76%		85%	85%
COE (mills/kWh)			75 ⁺		87 [*]	78 [#]

⁺ in 1992 US dollars.

^{*} in 2003 US dollars.

[#] in 2004 US dollars.

The FW and surrounding in-vessel components conform to the plasma, as shown in Figs. 9 and 10, and deviate from the uniform toroidal shape in order to achieve compactness. The reference design calls for a 3-FP configuration. Within each FP that covers 120 degrees toroidally, the FW changes from a bean-shape at 0° to a D-shape at 60° , then back to a bean-shape at 120° , continually switching the surfaces from convex to concave over a toroidal length of ~ 17 m.

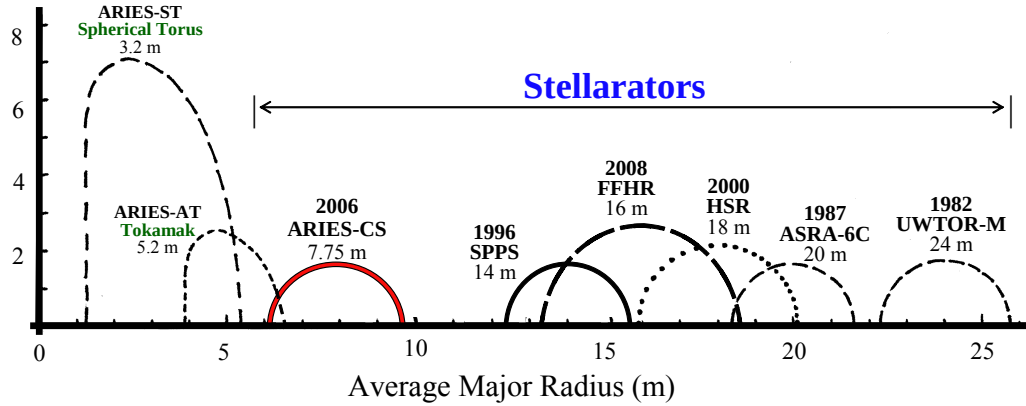


Figure 8. Evolution of stellarator size. Advanced tokamak and spherical torus included for comparison.

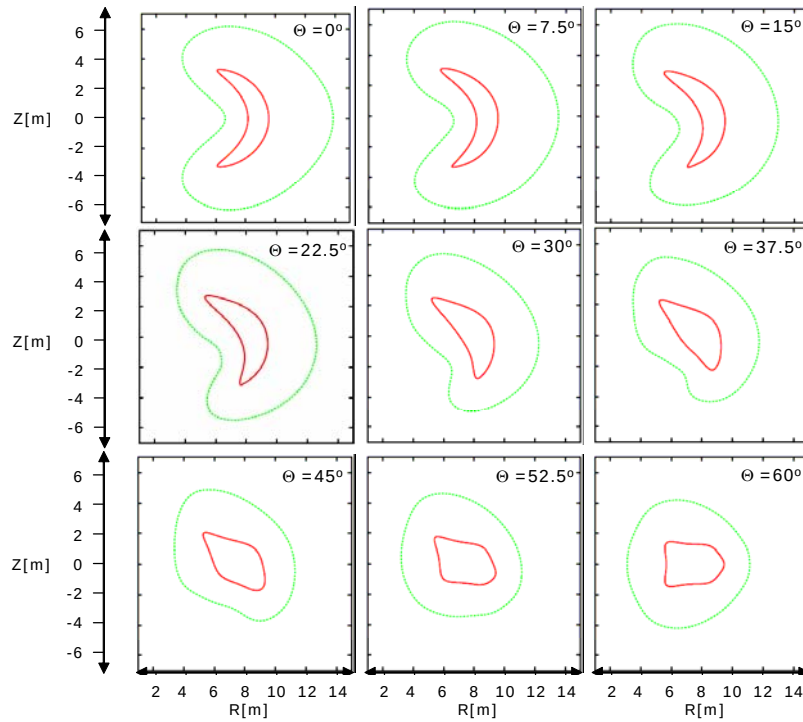


Figure 9. Nine plasma and mid-coil cross sections covering one half field period. Dimensions are in meters. Toroidal angle (Θ) measured from beginning of field period.

Figure 9 displays nine cross sections over a half FP showing the plasma boundary and the mid-coil filament. In each field period, there are four critical regions of $\Delta_{\min}$ where the magnets move closer to the plasma, constraining the space between the plasma edge and mid-coil. $\Delta_{\min}$ should accommodate the scrapeoff layer (SOL), FW, blanket, shield, vacuum vessel, assembly gaps, coil case, and half of the winding pack. An innovative approach was developed specifically for ARIES-CS to downsize the blanket at $\Delta_{\min}$ and utilize a highly efficient WC-based shield [19]. This approach placed a premium on the full blanket to supply the majority of the tritium needed for plasma operation. A novel approach based on coupling the CAD model with the 3-D neutronics code was developed to model, for the first time ever, the complex stellarator geometry for nuclear assessments to address the breeding issue and assure tritium self-sufficiency for compact stellarators with $R_{av} > 7.5$ m [19].

ARIES-CS has 7.75 m average major radius, 6.4% beta, 2.6 MW/m² average neutron wall loading, and 1000 MW_e net electric power. A number of blanket concepts and maintenance schemes were evaluated. The preferred option is a dual-cooled (LiPb and He) FS-based modular blanket concept coupled with a Brayton cycle with a thermal conversion efficiency of 43%, and a port-based maintenance scheme utilizing articulated booms [22]. Analogous to advanced tokamaks, the prospect of using LiPb with SiC/SiC composites as the main structural material offers high operating temperature with high thermal conversion efficiency approaching 56% and lower cost of electricity. The ARIES-CS vacuum vessel is internal to the coils. The overall coil system, consisting of the inter-coil structure, coil cases, and winding packs, is enclosed in a common cryostat. The coils are wound into grooves at the inside of a strong supporting toroidal tube that provides a ring structure to accommodate the electromagnetic forces. A cross-section at the beginning of a FP is shown in Fig. 10, depicting the details of the power core components.

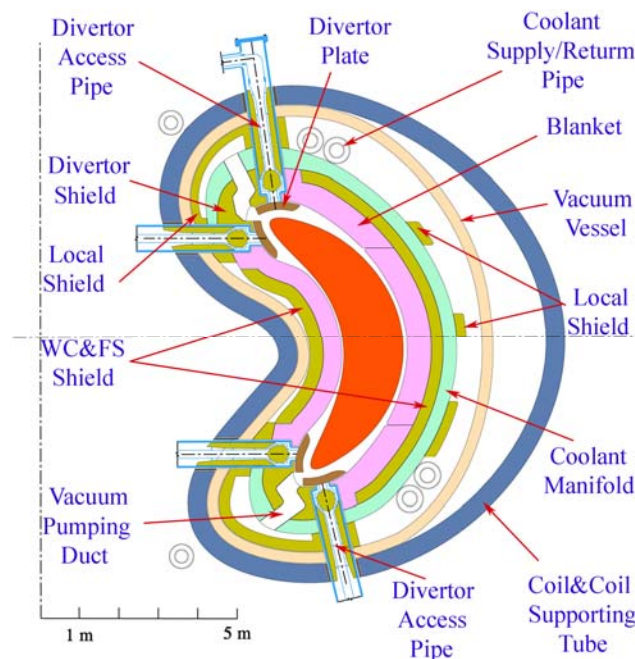


Figure 10. ARIES-CS cross-section at beginning of field period.

During the 3-y course of the ARIES-CS study, the design point was constantly pushed to the limit in order to understand the constraints imposed by compactness and the impact of the various tradeoffs on the peak neutron wall loading, tritium breeding, peak heat flux at divertor, materials temperatures and stresses, coil complexity, maintenance scheme, and other design constraints [105]. The most notable impact of compactness is the 18 MW/m^2 peak heat flux at the divertor that exceeds the 10 MW/m^2 engineering limit by a wide margin. The study concluded that increasing the machine size beyond the 7.75 m major radius could be beneficial as it provides more margins for space and engineering constraints with a small cost penalty. Furthermore, alternate aspect ratio (e.g., 6 instead of 4.5), divertor plate orientation, and engineering tradeoffs could lead to more attractive configurations with less complex coils and geometry that help stellarator constructability. Future physics activities should aim at understanding the limiting mechanism for the plasma beta and developing means to reduce the alpha particle losses below the design value of 5% as these energetic particles damage the plasma surrounding wall through blistering [105].

Overall, ARIES-CS has benefited substantially from its compactness, showing economic advantages (with comparable cost to advanced LiPb/SiC tokamaks [42]), and predicting a much brighter future for stellarator power plants than had been anticipated a few decades ago. At this writing, it is premature to state with certainty how the US stellarator community will adapt its research program to the recent cancellation of the compact stellarator NCSX experiment [100]. This cancellation will definitely place the US stellarator development on a longer time scale compared to tokamaks. Nevertheless, a strong collaboration program with the LHD experimental device in Japan [98] and W7-X in Germany [99] will help fill the gap and enhance the physics database. Meanwhile, the national stellarator program should continue the engineering studies to resolve the divertor issues and simplify the coil design.

3.3. Spherical Tori

Initiated in the late 1990s, two power plant studies have been made of the spherical torus (ST) concept: the US 3-year ARIES-ST study [112,113] and the UK conceptual ST design [114,115]. The limited number of studies reflects the much smaller ST database compared to tokamaks. Worldwide interest in the ST concept began in the 1980s when Peng et al. [116,117] identified unique physics features of ST as a low aspect ratio (A) device. Key ST features include good plasma confinement, high toroidal beta, and naturally large elongation that allows operation with high bootstrap current ($> 90\%$). Geometrically, the ST device is tall and elongated, having a plasma shape like a football with a central hole to accommodate the inner legs of the TF coils and necessary shielding. This highly elongated shape is quite different from the donut plasma with a D-shape for tokamaks. The strong magnetic field line curvature in STs naturally leads to plasma stability. Consequently, the ST has the ability to operate with high toroidal beta (30-60%) – a major attribute of STs. This particular advantage becomes apparent only at low aspect ratios ($A \leq 2$). Thus, ST plasmas have been produced with A ranging between 1.1 and 2 – low compared to conventional tokamaks with A of 3 or more. Another advantageous feature of STs is the resiliency to major disruptions. Moreover, the toroidal magnetic field is 2-3-fold less in STs compared to tokamaks, calling for normal, non-superconducting magnets with present-day technology and much less shielding requirements. However, the resistive losses in these magnets could be significant, requiring

large recirculating power (~ 300 MW), higher fusion power, and a larger machine to deliver 1000 MW_e or more. For ST power plants, a challenging task would be to design a divertor system that can handle tens of MW/m^2 heat flux with high sputtering rates. On the positive side, the high bootstrap current will reduce the external current drive (CD) power requirements and the high beta will enhance the fusion power and lower the cost of electricity.

START was the first ST experiment, built at Culham, UK in the early 1990s as a small-size with $A=1.3$, low-cost device to test the theoretical predictions for low A STs [118]. The combination of small-size, low-cost, and achievable high plasma temperatures and 40% beta in START strengthened the worldwide interest in the ST concept. At present, there are ~ 20 ST experimental facilities in the US, UK, Italy, Turkey, Russia, Japan, Brazil, and China [119]. The largest two facilities (NSTX [120] in US and MAST [121] in UK) are currently proof-of-principle experiments with D-D plasmas and their successors will burn D-T fuel.

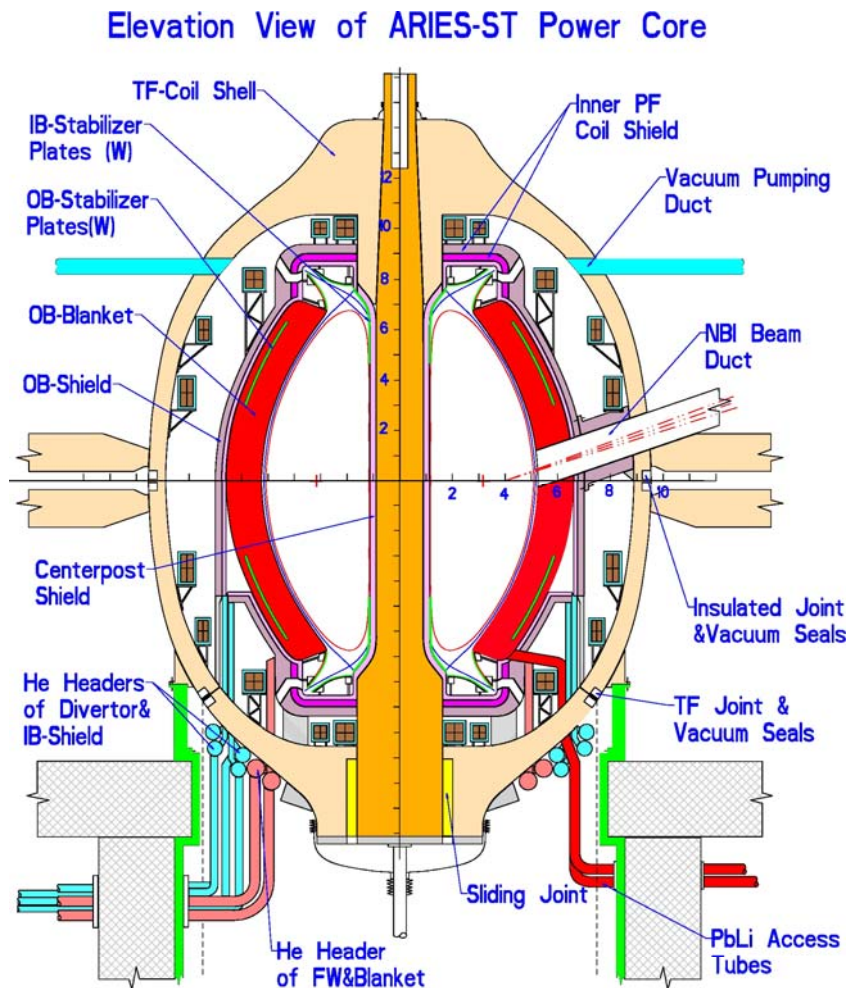


Figure 11. Vertical cross section of ARIES-ST [113].

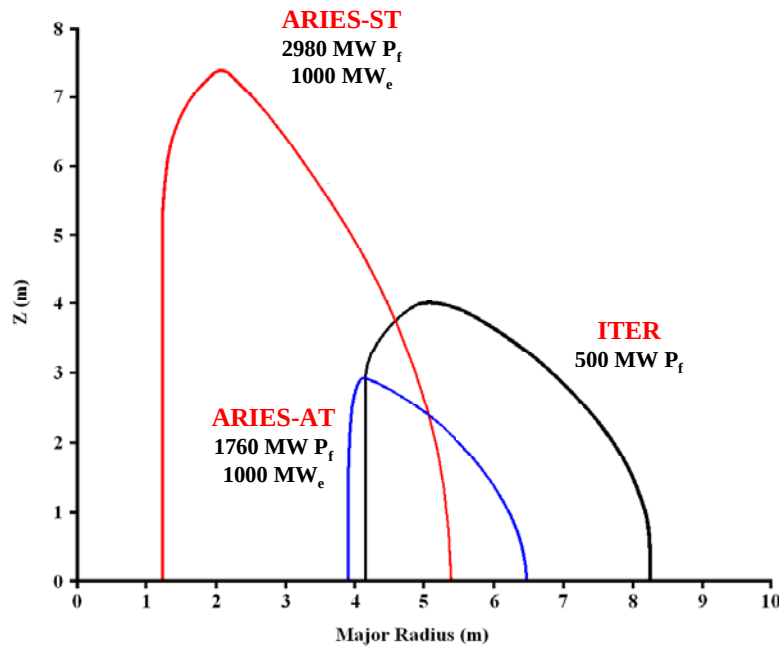


Figure 12. The last closed flux surfaces of ARIES-ST plasma in comparison with ARIES-AT and ITER.

The impressive experimental achievements of the 1990s promoted further interest in exploring the ST potential as a power plant. The first large-scale ST study was undertaken in the US by the ARIES team in 1997, delivering the ARIES-ST power plant [112,113] with 1000 MW net electric power and 1.6 aspect ratio. Table V summarizes the reference parameters and Fig. 11 displays a vertical cross section through the main components. To put matters into perspective, Fig. 12 compares the plasma boundaries of ARIES-ST with ARIES-AT (an advanced tokamak) and ITER (an experimental device). This illustrates the true compactness of ST only in the radial direction with remarkable increase in height. During the 3-y course of the ARIES-ST study, several tradeoffs between physics and engineering disciplines were necessary to identify a viable baseline design with high beta, high bootstrap current fraction, and well-optimized power balance. Such complex tradeoffs between numerous competing factors became apparent in designing the center post (CP) – the most challenging engineering aspect of STs. In order to reduce the Joule losses and shielding requirements, a single turn coil (without electric insulator) is the preferred option for the CP. Its Cu alloy is water-cooled operating at 35-70°C. The outer legs form a continuous shell (made of water-cooled aluminum) and serve also as a vacuum vessel. The 30 m high CP is tapered at the top for a tight fit to the outer shell and flared at the bottom to reduce the Joule losses [122]. An unshielded CP does not offer an attractive design despite the fact that the shield competes with the CP for a valuable space. An effort to reduce the CP Joule losses only and neglect the benefits of the inboard shield led to a non-optimum power balance with degraded system performance. The reference 20 cm thick inboard shield (made of helium-cooled FS) has positive impacts on the tritium breeding, overall power balance, and safety aspects of ARIES-ST [18].

Table V. Key parameters of ARIES-ST and ST design developed in UK

	ARIES-ST [113]	ST-UK [114,115]
Net Electric Power (MW _e)	1000	1224
Fusion Power (MW)	2980	3260
Major Radius (m)	3.2	3.4
Aspect Ratio	1.6	1.4
Plasma Elongation	3.4	3.2
Toroidal Beta (%)	50	58
Plasma Current (MA)	29	31
Bootstrap Current Fraction	0.96	0.88
CD Power to Plasma (MW)	28	29.3
Neutron Wall Loading – Peak/Average* (MW/m ²)	6.4/4.1	4.6/3.5
Peak Heat Flux at Divertor (MW/m ²)	31	21
Thermal Conversion Efficiency (%)	45	43
Peak Magnetic Field at TF Coil (T)	7.4	7.6
Number of TF Coils	continuous shell	16
Normal Magnet Joule Losses (MW)	329	254
Recirculating Power Fraction	0.34	0.39

* For FW surface crossing the separatrix.

The high recirculating power fraction (0.34) of STs mandates designing a blanket with high thermal conversion efficiency to enhance the power balance. A novel blanket design based on the dual-cooled lithium lead concept was developed in 1997 and used for the first time in ARIES-ST [112,113]. The main blanket features include He-cooled FS structure with flowing LiPb coolant/breeder and SiC inserts to extend the LiPb output temperature to 700°C [20]. A high thermal efficiency of 45% has been obtained and the 3-D nuclear analysis confirmed the ability of the outboard-only blanket to breed the tritium needed for plasma operation [18]. A practical solution was found to handle the high divertor heat flux [20]. First, by inclining the outboard plate by 22°, the peak heat flux can be reduced from 16 to 6 MW/m². Second, most of the inboard heat flux can be deposited on the W stabilizing shells mounted at the top/bottom of the inboard shield (refer to Fig. 11).

The maintenance scheme is unique and commensurate with the ARIES-ST simple configuration. The blanket, shield, divertor, and CP are removed as a single unit from the bottom of the device [113]. Demountable joints at the outer TF shell facilitate the replacement of all components. The sliding joint near the lower end of the CP allows its removal and replacement separately, if needed. An advanced fabrication technique led to 20-fold reduction in the cost of the TF coils compared to conventional approaches [25], saving 5 mills/kWh in COE, which is significant. For the same plant capacity factor, the ARIES-ST COE (81 mills/kWh) is comparable to that of the ARIES-RS tokamak [41,39] and ~50% higher than that of ARIES-AT [13] – a more advanced tokamak. An important issue related to the environmental impact of STs is the volume of radwaste generated during operation and after decommissioning. As mentioned earlier, STs are radially compact, but highly elongated compared to tokamaks (refer to Fig. 12). The total radwaste volume of ARIES-ST is quite large (3-4 times that of advanced tokamaks). The changeout of the sizable outboard blanket and the ~100 m³ CP every 3-6 years contributes significantly to the operational radwaste.

During the ARIES-ST design process, extrapolations beyond the existing physics and technology database were deemed necessary to deliver an attractive end product. Therefore, a list of key R&D needs was compiled for future activities [113]. Experiments with proof-of-performance are needed to establish the ST-specific physics database. The key technology issues include the compatibility between materials at high operating temperatures [20] and the uncertainties in performance of both advanced FS structure and embrittled Cu alloy of CP [18] under a severe radiation environment. Means to prolong the CP lifetime should be investigated to minimize the radwaste stream.

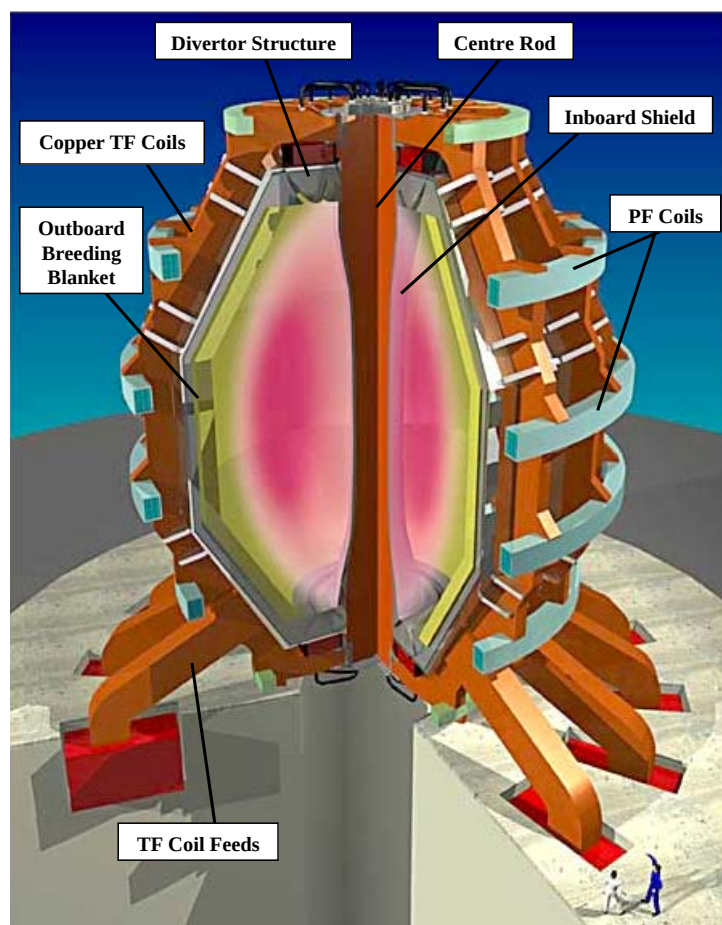


Figure 13. Isometric view of ST-UK power plant (courtesy of G. Voss (EURATOM/UKAEA Fusion Association)).

The ST-UK power plant conceptual study [114,115], developed in the early 2000s, provides details for the plasma physics parameters, fusion power core components, power cycle, coil power supply system, and site layout. The key parameters are given in Table V for the design shown in Fig. 13. There are similarities and differences between ST-UK and ARIES-ST. Similar engineering features include double-null configuration, single turn water-cooled resistive TF coil, 80 cm radius center column containing 15% water and flared at top/bottom, thin inboard shield to protect the center column, outboard-only blanket, and

vertical maintenance scheme. Other distinct engineering features [114,115] are related to TF coil construction, blanket and divertor designs, and power cycle. More specifically, these features include:

- 16 TF coils made of water-cooled Cu alloy
- 12 cm thick water-cooled inboard shield
- 6 y service lifetime for center column
- 48 blanket modules
- He-cooled Li_4SiO_4 blanket with beryllium multiplier and different Li enrichments
- Cascade flow of SiC pebbles for upper and lower divertors
- Heat recovery systems for both He coolant (@ 600°C) and water coolant (@ 70-200°C).

For consistency, the ST-UK plasma parameters have been iterated with the neutronics, thermodynamic, and mechanical design. The shape and size of the device were arrived at by determining the peak neutron wall loading (4.6 MW/m^2) that allows 2 years service lifetime for 100 dpa FS structure [114]. The plasma parameters were then derived self-consistently using various physics codes. System studies were performed and several iterations helped guide the design toward the final configuration. The TF coil system influenced the design due to its electrical power requirements that are dominated by the central column. A good compromise and advantages were achieved by shielding the central column with thin inboard shield. The number of outer return legs (16) was determined by the need to reduce the ripple to 2% at the plasma edge. The divertor target surface is composed of SiC pebbles that intercept the particle energy [115]. The pebbles flow under gravity, through ducts embedded in the inboard shield and outboard blanket, cooling the upper divertor first, then the lower divertor before exiting the power core to exchange heat with the helium coolant. The power conversion system handles a spectrum of heat quality (600°C He from blanket and divertor and 70-200°C water from shield and TF coils) to maximize the net efficiency and output power [115]. The maintenance approach takes advantage of the simple extraction of the center column along with its shield, and divertor system from beneath the machine. The blanket is then lowered in groups of four modules and moved to a hot cell [114].

In summary, both the ARIES-ST and ST-UK studies identified the strength and weakness of the ST concept as a power plant, in addition to a set of critical issues to be addressed by dedicated R&D programs. The key technological issues include the high divertor heat flux ($> 10 \text{ MW/m}^2$), structural integrity of the embrittled Cu center post, blanket materials compatibility issues at $> 700^\circ\text{C}$, and high stream of radwaste. The newly proposed cascading pebble divertor, liquid lithium divertor, and X-divertor (that expands the magnetic flux) overcome the high heat flux and erosion problems, but raise several engineering issues that need serious evaluation. Developing an advanced blanket system, such as the self-cooled LiPb blanket with SiC/SiC composites that delivers high thermal conversion efficiency (50-60%), is highly beneficial for STs to help offset the negative impact of the high recirculating power fraction and demonstrate the economic competitiveness of STs with advanced tokamaks. Recycling the CP and all other replaceable components helps minimize the radwaste volume, enhancing the environmental feature of STs. Overall, the prime missions of operational ST experiments and their planned upgrades along with future ST-specific R&D

activities are to reduce the gap between existing and next step facilities and to build the physics and engineering database for an attractive ST power plant.

3.4. Field-Reversed Configurations

The FRC family of concepts stems from the Astron idea of Christofilos [123], originally invented in the 1950s, and belongs to a set of compact tori that also includes spheromaks. FRC represents one of the simplest configurations that can be envisioned for a fusion device. Geometrically, it is a linear, open-ended cylindrical system, quite different from tokamak, stellarator, ST, and RFP. The FRC configuration, shown in Fig. 14, consists of nested magnetic flux surfaces created by currents flowing inside the plasma, with these closed flux surfaces embedded inside a linear magnetic field created by external magnets [124]. The open field lines guide the charged particles to the chamber ends, acting as a natural divertor and carrying 15-20% of the D-T fusion power. Besides removing the impurities from the plasma, this feature offers the possibility of direct energy conversion with high efficiency.

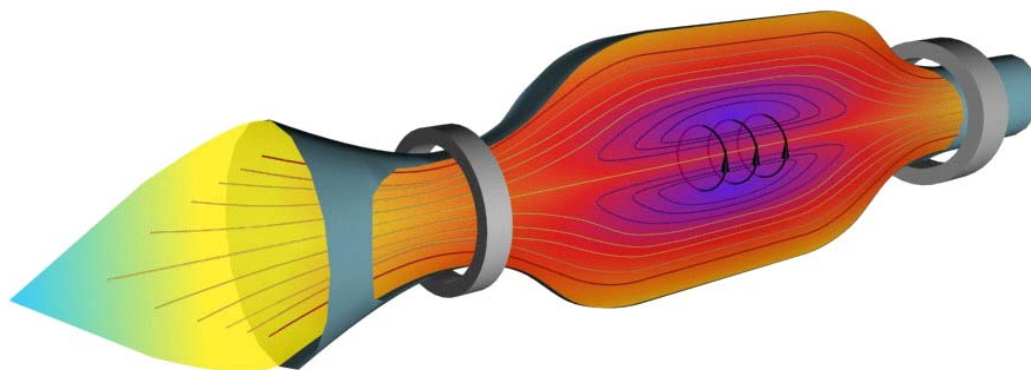


Figure 14. Isometric view of FRC plasma (courtesy of S. Ryzhkov (Bauman Moscow State Technical University, Russia) [125]). The separatrix divides the closed and open field lines, presenting a natural boundary for the hot plasma.

The FRC plasma is formed within a set of cylindrical coils that produces the axial magnetic field. Several methods of plasma formation exist. In the most common method historically [124], the external field is applied and then reversed in direction, causing the magnetic field trapped in the plasma to spontaneously reconnect and produce closed field lines. This method typically requires more input energy than is desirable for a power plant. Recent FRC research, therefore, focuses on sustainment using rotating magnetic field (RMF) current drive [126], which has been making good progress [127]. Key features of the FRC concept are the well-confined plasma, low magnetic fields, and very high beta (50-100%). The cylindrical chamber is relatively simple and permits easy construction, access, and maintenance of all components. The high beta allows a compact fusion core along with the use of low-field (3-5 T) magnets, simplifying the FRC configuration further. The physics has been studied since the 1950s in the US and Russia [128], plus somewhat later in Japan [124]. The world's largest FRC facility, the Translation, Confinement, and Sustainment Upgrade / Large S Experiment (TCSU/LSX) at the University of Washington [129,130] has 0.8 m wall

diameter and ~ 4 m chamber length. Twelve other smaller-scale FRC facilities include IPA, PHDX, PFRC, Rotamak, Co-FRC, Tri-Alpha, FRX-L, and FRCHX in the US, FIX (at Osaka University) and TS-3 (at Tokyo University) in Japan, and KT (at TRINITI) and FIAN (at Lebedev) in Russia.

In the 1970s and 1980s, researchers examined the potential of the FRC concept as an energy producing power plant [131-135]. The latest design of the 1980 series, the FIREBIRD power plant [135], is a D-T fueled, pulsed device with 0.9 m FW radius, 4 MW/m^2 neutron wall loading (NWL), and variable chamber length ranging between 33 and 91 m capable of delivering 300-1000 MW_e net electric power. The 0.5 m thick He-cooled Li_2O blanket contains Pb_3Zr_5 neutron multiplier. The 46 cm thick water-cooled shield (made of stainless steel, boron carbide, and lead) along with the blanket protects the superconducting magnets. The cyclic pulsing of the design imposed significant transient thermal stresses that affect the fatigue lifetime of the FW and blanket despite the thin 0.5 cm graphite tile placed at the FW to mitigate the stresses during pulsed operation.

The 1990s witnessed the emergence of a new steady-state approach that solved many of the pulsed system problems [126]. However, the challenging physics issue was sustaining the plasma current. Reference 136 presents an interesting steady-state D-T power plant design, giving in-depth the RMF current drive parameters, although the engineering systems were not designed in detail. In the early 2000s, a US team from the Universities of Wisconsin, Washington, and Illinois performed a scoping study of critical issues for FRC power plants [137] and invoked the RMF current drive [126] for steady-state operation. The study focused on three main tasks: systems analysis, blanket and shield design, and economic assessment. The 4 m diameter and 25 m long chamber, shown in Fig. 15, contains 1.5 m thick helium-cooled Li_2O blanket with FS structure followed by 0.6 m thick He-cooled shield and 4 cm thick cylindrical normal magnets. The design utilized thermal and neutronics analyses to design blanket and shield that can handle an average NWL of 5.7 MW/m^2 . The cost of electricity amounts to ~ 60 mills/kWh, which is competitive with advanced tokamaks.

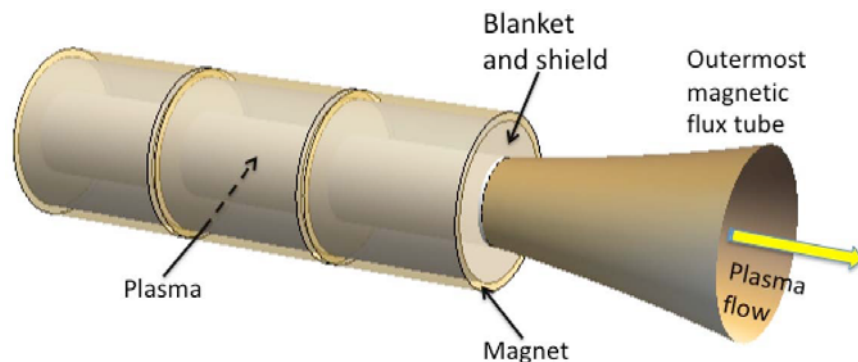


Figure 15. General layout of UW-FRC power plant (courtesy of J. Santarius (UW-Madison)).

An increased compactness (caused by the high beta) has been identified for the FRC designs to reduce the cost of electricity. As a result of this compactness, the NWL in some FRC designs exceeded the engineering design limit of $\sim 5 \text{ MW/m}^2$ and called for advanced fuels (such as $\text{D-}^3\text{He}$) to alleviate the FW problems or innovative FW protection schemes,

such as liquid walls [138]. In fact, the Astron concept was the first FRC concept to propose the use of liquid walls [123]. Even though the NWL could reach high values approaching 20 MW/m^2 , the most serious concern for liquid walls is the potential evaporation of the candidate liquids/breeders (Flibe, LiSn, or Li) that contaminates the plasma [138].

The FRC has been an important platform for investigating the potential advantages of the advanced D- ^3He fuel cycle that would alleviate many of the problems caused by energetic D-T neutrons. In conceptual D-T fusion designs, the magnetic fields optimize at 2-3 T due to engineering constraints. The high FRC beta allows increasing the magnetic field to recover the power density through the fusion power's $\beta^2 B^4$ scaling in order to offset the lower reactivity of D- ^3He fuel compared to D-T fuel. In the D- ^3He fuelled RUBY [139], ARTEMIS [140], and joint Bauman Moscow State Technical University/UW FRC [141] designs, there is no need for a breeding blanket and the neutron power is low ($\leq 5\%$ of the fusion power), requiring less shielding, while the low neutron-induced radiation damage allows all components to operate for the entire plant life with no need for replacement due to radiation damage consideration. In addition, the device could be built with today's technology [142] and, for any D- ^3He fusion device, the environmental and safety characteristics are superb compared to D-T systems [143]. Moreover, these D- ^3He designs could obtain electrical power by direct energy conversion of the charged particles with high efficiency, exceeding 70%. Note that while the D-T FRC economics compare favorably with tokamaks, preliminary indications are that the more simple and easy to maintain D- ^3He FRC reflects an additional 25% cost savings [141]. As an illustration for a typical D- ^3He configuration, the ARTEMIS [140] central burning section with neutral beams to sustain the plasma and two direct converters for the 14.7 MeV protons are shown in Fig. 16 for an overall length of 25 m. Table VI summarizes the engineering parameters for some of the FRC power plant conceptual designs developed to date. All designs are based on 1000 MW net electric power.

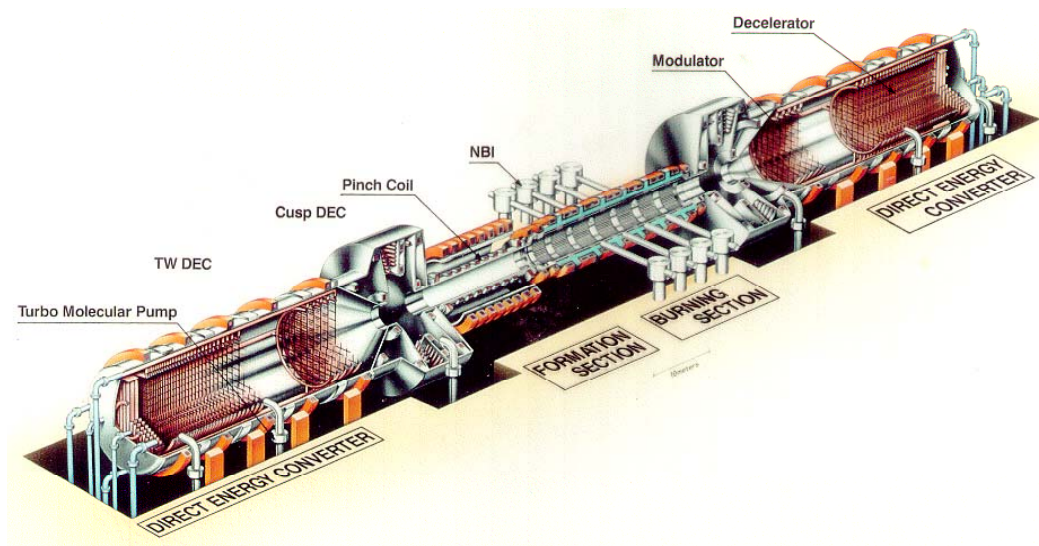


Figure 16. Layout of ARTEMIS D- ^3He fueled FRC power plant [140].

Table VI. Key engineering parameters for the most recent FRC power plants delivering 1000 MW_e

Power Plant	UW-FRC [137]	APEX-FRC [138]	ARTEMIS [140]	RF/UW-FRC [141]
Type	Steady-State	Pulsed	Steady-State	Steady-State
Fuel	D-T	D-T	D- ³ He	D- ³ He
Chamber Wall Material	Solid	Liquid	Solid	Solid
	FS	Wall	FS	FS
Plasma Radius (m)	1.87	1	1.12	1.25
Wall Radius (m)	2	2	2	1.8
Length (m):				
Separatrix	20	8	17	30
Chamber	25	10	25	35
Blanket Type	Li ₂ O/He/FS	Flibe	---	---
Fusion Power (MW)	1785	2307	1610	1937
Neutron Power (MW)	1427	1844	77	49
Average NWL (MW/m ²)	5.7	18	0.4	0.14
Surface Heat Load (MW/m ²)	0.12	0.39	1.7	2.18
Energy Conversion Efficiency	52%	40%	62%	60%

Despite the steady progress made by the FRC community over the past 3-4 decades, the remaining leading issues that hinder the progress of the FRC concept are plasma stability, energy confinement, and an efficient method for current drive. While the D-³He system alleviates the FW problems, exhibits salient environmental and safety characteristics, and offers the benefits of direct energy conversion, its physics is hard to achieve requiring 6-fold higher plasma temperature and 8 times larger density-confinement time product than D-T. Currently, the limited funding worldwide has left many FRC physics and engineering issues unresolved. As such, the FRC community relies heavily on the world-leading US FRC research activities, along with international collaboration, exchange of ideas, and sharing the outcome of theoretical and experimental research.

3.5. Reversed-Field Pinches

The RFP is an old concept, first studied in the early 1960s as an axisymmetric, toroidal geometry. At present, the RFP physics is more mature than FRC and spheromak physics. The RFP configuration is much like a tokamak except for the more than 10-fold weaker toroidal magnetic field. The dominant magnetic field at the plasma edge is poloidal. As one moves radially away from the plasma axis, the toroidal field reverses its direction, hence the name reversed-field. Figure 17 displays a schematic view of the RFP plasma configuration. The confining magnetic field is generated primarily by large driven plasma current. Usually, some of the current is self-generated by the plasma through the dynamo effect, although current profile control is being developed to minimize dynamo action as a means to improve energy confinement.

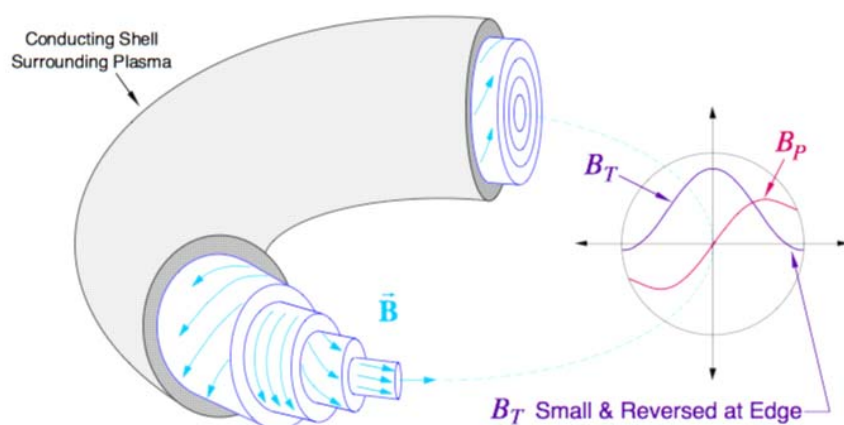


Figure 17. Isometric view of RFP plasma showing typical strength of toroidal and poloidal fields (courtesy of J. Sarff (UW-Madison)).

The distinct feature of RFP that motivates its interest as a fusion energy system is the weak applied toroidal magnetic field. This leads to positive attributes, including high beta values, high mass power density yielding a compact design with favorable economics, normal (non-superconducting) coils with less shielding, weak magnetic forces on coils, single-piece maintenance system with high system availability, and free choice of aspect ratio (limited by engineering, rather than physics, constraints). Even though pulsed current scenarios are potentially more attractive at low magnetic fields, the RFP might be operable in a steady-state mode using the Oscillating Field Current Drive inductive AC helicity injection technique, gaining the attractive features of steady-state power plant scenarios. A conducting shell surrounding the plasma is required to stabilize the ideal MHD current-driven instabilities. Hence, resistive wall instability control is required for pulse lengths meaningful for fusion energy application. Furthermore, resistive MHD tearing instabilities are created by gradients in the plasma current density, resulting in magnetic fluctuations that may drive large transport from the plasma core to the edge. Current profile control has proven effective in controlling these instabilities, although for transient periods in experiments to date. An alternate path for improved confinement is to attain a single-helicity dynamo from one tearing mode.

To address these challenging issues, the theoretical and experimental bases for RFP have grown remarkably since the late 1970s calling for constructing larger RFP experiments with more intense plasma currents [144,145]. Subsequently, the foremost RFP experiment was built in the US in the mid-1980s at UW-Madison [146]. The Madison Symmetric Torus (MST) research program is focused primarily on developing current profile control to minimize magnetic fluctuations and improve confinement, as well as verify the high beta capability of the RFP. It operates with a thick conducting shell to avoid resistive wall modes. Along with MST, three other modern facilities (RFX in Italy [147], RELAX in Japan [148], and EXTRAP-T2R in Sweden [149]) form the key elements of the international RFP experimental program. The RFX and EXTRAP-T2R devices both use a resistive shell covered by actively controlled saddle coils to mitigate the resistive wall instabilities. Collectively, these experiments have demonstrated that the transport can be reduced significantly by controlling magnetic fluctuations, 26% poloidal beta is achievable, the

resistive wall instabilities can be actively suppressed, and the energy confinement time can be increased, enhancing the triple product ($nT\tau$) by 10 fold. These encouraging results among others promoted the worldwide RFP status to a proof-of-principle program. With appropriate resources, it would be possible to establish the basis for a burning plasma experiment within 20 years.

The potential advantages of RFP as a power plant have been demonstrated during the 1980s through a few conceptual studies with 1000 MW_e net electric power [150-152]. Major differences between designs occur in the physics parameters and engineering aspects. The earlier 1981 design [150] operated in a pulsed mode, utilized superconducting magnets, and used a relatively low NWL of 5 MW/m². The 1986 steady-state design parameters with resistive coils were selected from a comprehensive trade-off study taking into consideration the technological issues related to the operation of a high power density RFP with 20 MW/m² NWL [151]. The full advantages of RFP were validated in 1990 by the multi-institutional ARIES team through the large-scale, self-consistent TITAN study [152]. This study was based on a set of strong physics assumptions and delivered two different designs (TITAN-I and TITAN-II) to demonstrate the possibility of multiple engineering design approaches to small physical size, high mass power density (MPD) power plants. TITAN-I is a self-cooled lithium design with vanadium structure while TITAN-II is a self-cooled aqueous design with FS structure. Using essentially the same plasma parameters, each design has a very high MPD of ~800 kW_e/tonne of fusion power core, approaching that of fission reactors. The TITAN study adopted a high NWL of 18 MW/m² (rather than 3-4 MW/m² for tokamaks) in order to quantify the technical feasibility and physics limits for such high MPD devices. Adding impurities to the plasma to attain very high radiated power fraction helped ease the divertor high heat flux problem. The high NWL requires the replacement of the TITAN torus on a yearly basis. A feasible single-piece maintenance procedure, unique to high MPD devices, was developed for TITAN. Another unique feature for TITAN-I is the use of the integrated-blanket-coil concept (IBC) where the Li coolant/breeder flows poloidally in the blanket and also serves as an electrical conductor for poloidal field coils and divertor coils. Designing the power supply is one of the critical issues for IBC, however. Two other types of coils are utilized to control the plasma: normal or superconducting. The general arrangement of TITAN-I is shown in Fig. 18. The overall results from the study support the attractiveness of compact, high MPD RFP as an energy system with favorable economics (COE ~40 mills/kWh in 1990 US dollars). Just recently in 2008, Miller [153] modified the TITAN-I characteristics by introducing the modern engineering and economic approaches of ARIES-AT, such as the DCLL blanket and updated costing models. Ignoring the ~4 MW/m² average NWL limit for the DCLL blanket concept, the COE of the modified TITAN (with LSA=2 and NWL of 13 MW/m²) approaches that of ARIES-AT with LSA=1. Besides the economic impact, the major penalty for backing down in the average NWL from 13 to 4 MW/m² is the larger chamber size that undercuts the rationale for compactness, high MPD, and single-piece maintenance [153].

The 1990 TITAN was based on several strong physics assumptions, such as high plasma current (17.8 MA) and high energy confinement time (0.22 s). Through investigation of the key physics issues of beta limits, energy and particle confinement, transport, and current sustainment, the ongoing US and worldwide RFP experimental program aims to validate the TITAN strong physics assumptions among others.

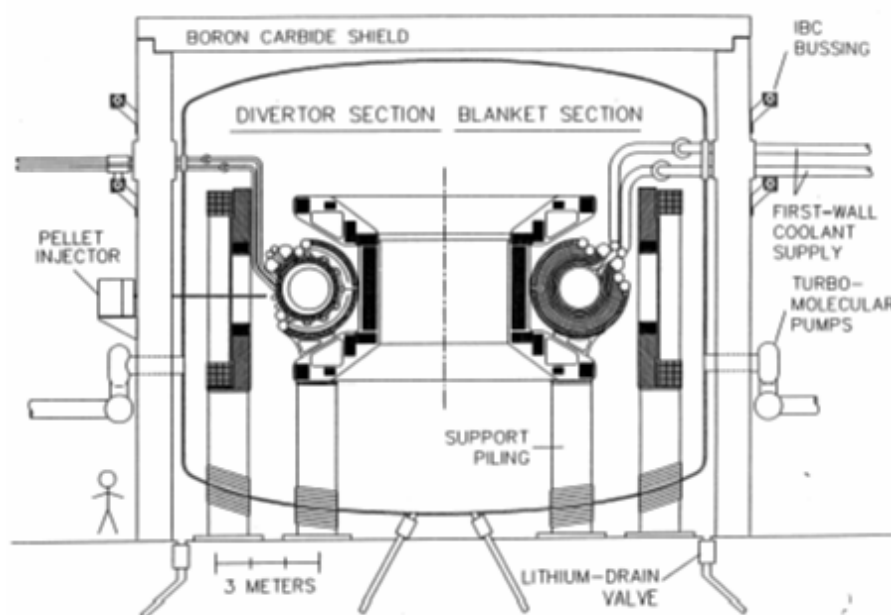


Figure 18. Elevation view of TITAN-I with 3.9 m major radius, 0.6 m minor radius, 0.4 T toroidal field at plasma surface, 23% poloidal beta, 30 cm thick blanket, and 45 cm thick shield.

3.6. Spheromaks

The spheromak belongs to a family of compact tori that includes FRC and field-reversed mirrors. It is a toroidally symmetric configuration distinguished from STs and tokamaks by the simple, compact geometry without toroidal field coils and with no inboard CP or materials, offering a truly compact fusion device with low aspect ratio, high beta (10-20%), and comparable toroidal and poloidal fields. Spheromaks confine the roughly spherical plasma in a cylindrical structure using only a small set of external stabilizing coils. A distinct feature is that the confining magnetic fields are self-generated by the plasma. The super-hot, fast-moving plasma produces magnetic fields that pass through the plasma, generating more current that reinforces the magnetic fields further. The surrounding metallic structure contains the magnetic field. Although the overall design is simple, the plasma dynamo behavior is very complex and difficult to predict or control as it often involves magnetic fluctuations and turbulence. Since the early 1980s, research efforts [154] have focused on understanding how the fluctuations affect the confinement and how to sustain the plasma with sufficient energy confinement and high temperatures.

Several spheromak experiments have been constructed in the US, Japan, and UK over the past two decades. The early 1980s experiments did not demonstrate the anticipated higher plasma temperature compared to tokamaks. As a result, interest in spheromaks declined in the US but continued internationally. In the early 1990s, a careful review of the data from the key Los Alamos National Laboratory (LANL) experiment triggered interest in reviving the spheromak concept. Subsequently, a thorough reanalysis, led by Fowler and Hooper at the Lawrence Livermore National Laboratory (LLNL), indicated the spheromak plasma parameters are much better than originally calculated. In light of the 1990s reanalysis, several

new spheromak experiments were constructed in the US, including SSPX [155], SSX [156], and Caltech Spheromak [157].

Over a period of three decades, numerous scientists believed the simpler spheromak configuration made a better power plant with much lower cost than tokamaks [158-165]. As such, the first US conceptual design [159] was launched by LANL in the mid-1980s to explore the potential of a steady-state spheromak power plant by means of closely coupled physics, engineering, and costing models, using the COE as a figure of merit. Figure 19 displays the LANL spheromak configuration. Several engineering assumptions were made, including 70 cm thick blanket and shield surrounding the plasma, 15 MWy/m² end-of-life fluence for the blanket structure, and 5 MW/m² divertor heat flux. A minimum COE occurs near 20 MW/m² NWL for 2-3 m radius chamber, requiring a blanket replacement every year. The resistive coil thickness (~60 cm), its cost, and recirculating power were closely monitored. The COE varied between 45 and 110 mills/kWh (in 1984 US \$) as the net electric power changed from 1000 to 250 MW_e. As larger plasma radii yield low NWLs, the relatively low cost of the chamber could allow an economical multiple chambers with lower NWL than 20 MW/m² to drive a GW_e plant [159].

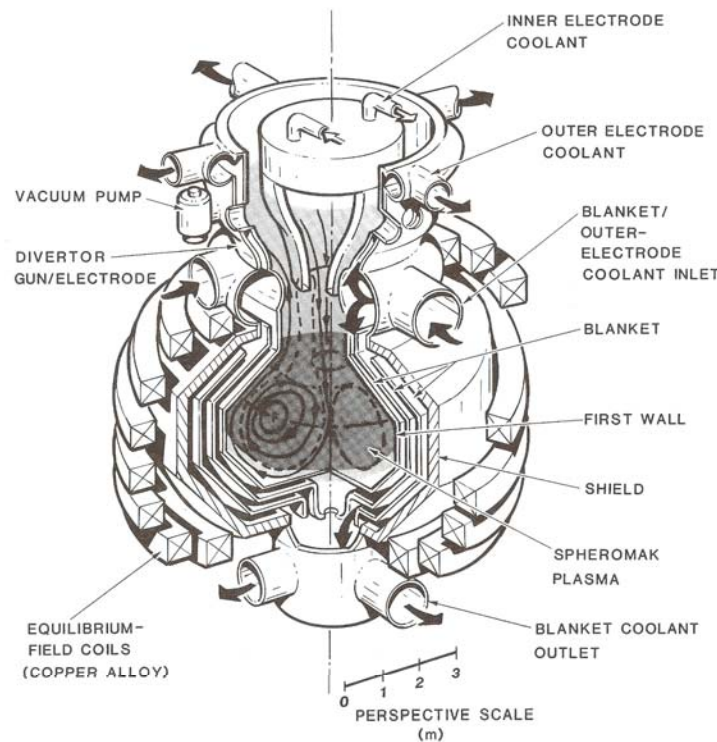


Figure 19. Schematic of LANL spheromak power core with ~3 m radius chamber [courtesy of R. Miller (Decysive Systems, NM)].

The LLNL conceptual 1 GW_e spheromak power plant [161,162] is shown in Fig. 20 for a configuration with two X-points. The steady-state, high temperature plasma is supported by several solenoidal coils that help produce the shape and elongation of the magnetic flux surfaces. The plasma current is driven by an external coaxial, electrostatic gun. The geometry

has a natural divertor with no interference from coils. For such an open system, the divertor heat flux can be spread over a large area as needed. Furthermore, the external location of the divertor offers unique maintenance flexibility for this component. The compactness of spheromaks suggests the consideration of a liquid wall (e.g., Li or Flibe) to handle the high NWL and protect the structure against intense radiation [162]. However, several physics and engineering problems must first be addressed before pursuing this option. These include the design impact of a conducting liquid wall, liquid evaporation, liquid flow path, and means for guiding the liquid through the machine. In this respect, the Flibe coolant would have much lower electrical conductivity and vapor pressure. Reference 164 suggests keeping the liquid flow to the outside by centrifugal force for molten salts (Flibe or Flinabe) and by a magnetic guide field for liquid metals (Li or SnLi). For 1 GW_e power plant, the divertor heat flux could reach 620 MW/m² and can be handled by high-speed (100 m/s) liquid jets. The tritium breeding is adequate for 85% blanket coverage. Several inconsistencies were cited in this design calling for engineering resolution and improved physics performance [164].

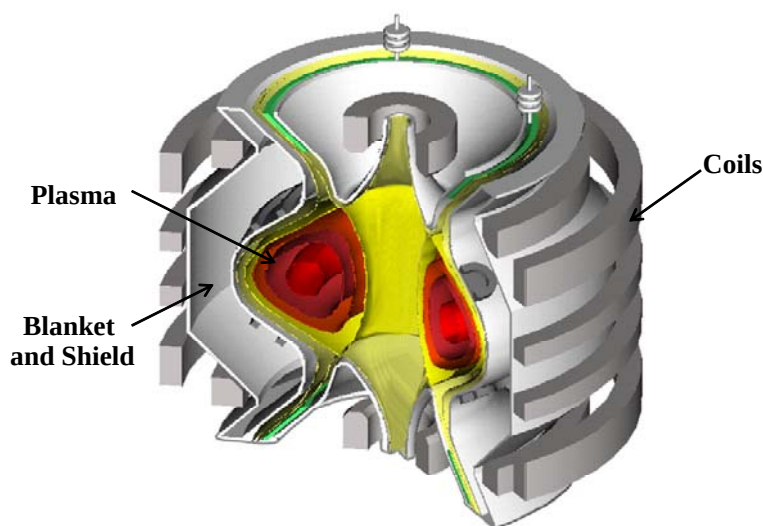


Figure 20. Schematic of LLNL spheromak power core with 4.5 m radius chamber (courtesy of E. Hooper (LLNL) [162]).

Overall, the simplicity, compactness, and absence of toroidal field coils make the constructability of spheromaks relatively easy and inexpensive compared to tokamaks. The spheromak will continue to offer an alternate concept if the plasma confinement exhibited in SSPX [155] extrapolates to larger systems. While pulsed power plants, as suggested in Reference 163, may remain viable, experimental results from SSPX cast doubt on a steady-state plant sustained by the simple method of electrostatic gun injection envisioned in Reference 159. However, new theoretical results suggest that an attractive steady-state plant could be developed using neutral beam injection to sustain the plasma current [165]. Even if the spheromak concept is not successful in realizing a path to a power plant, its physics and technology database can be applied to other fusion concepts, such as FRC, RFP, and tokamaks.

3.7. Tandem Mirrors

Substantial interest has been generated in tandem mirrors over the two decades of the 1970s and 1980s. In contrast to tokamaks, TM is linear in nature. The basic configuration is a long central cell (90-170 m) terminated by end cells. There are many configurations for the latter, each offering merits and drawbacks. TM is more amenable to maintenance compared to toroidal systems. Other positive attributes include the high beta (30-70%), no driven plasma current eliminating disruptions, the potential for direct conversion of charged particle power into electricity at high efficiency, and the expandable magnetic flux tube to reduce the heat flux on end cell walls.

Historically, the TM research embarked on decades of single-cell mirror physics beginning in the 1950s [166,167]. Much of the success in TM physics attributes to the achievements gained through research on simple mirrors [168,169]. In the mid 1970s, the TM concept was simultaneously proposed in the US [170] and Russia [171]. Geometrically, there are many possibilities to arrange the various elements of the TM. In 1997, Baldwin and Logan [172] introduced the thermal barrier concept for an attractive power plant, allowing high Q values (10-20) while reducing the technology demand on magnets and end plugs. Over a period of ~10 years, the physics and engineering aspects of TM have been the subject of significant theoretical and experimental studies in the US, Russia, and Japan. By the mid-1980s, a few major TM experimental facilities were operational in the US besides ~10 smaller experiments on the basic mirror approach. The mirror concept was actively pursued in the US by Lawrence Livermore National Laboratory, which built two large mirror and tandem mirror experiments in the 1980s, but were never completed nor operated. A variation on the mirror concept was the Elmo Bumpy Torus (EBT) experiment (built in the US at the Oak Ridge National Laboratory) that combined multiple mirror cavities in a toroidal arrangement. The EBT was discontinued when a fatal instability was discovered in the 1980s.

The TM-based activities of the 1980s delivered four conceptual power plant designs fuelled with both D-T (WITAMIR [173], MARS [174], and MINIMARS [175]) and D-³He (Ra [176]). Figure 21 displays the overall configurations and Table VII compares the key parameters. It was customary in the 1980s to design plants with high power level exceeding 1 GWe to take advantage of the economy of scale. Normally, the high power requires lengthy TM devices. The overall length of central cell, end plugs, and direct converter system ranges between 140 and 250 m. The shortest length (140 m) belongs to MINIMARS [175] that investigated a lower power level (600 MW_e) along with more advanced physics and technology. Most of the magnet problems are in the end plugs where the magnetic field exceeds 14 T. The simplicity of the end cell progressed steadily from WITAMIR-I to MINIMARS, replacing the complex yin-yang coils by C coils and incorporating a gridless direct converter system to handle the wide spread of ion energies. These improvements led to a compact MINIMARS with lower mass and lower end plug cost compared to MARS. The central cell required modest technology such as 3-5 T solenoidal coils, low-activation FS structure operating at 500°C, and a simple maintenance scheme.

Table VII. Selected design parameters for TM power plants

Power Plant	WITAMIR-I [173]	MARS [174]	MINIMARS [175]	Ra [176]
Fuel	D-T	D-T	D-T	D- ³ He
Net Electric Power (MW _e)	1530	1200	600	600
Overall Length (m)	250	220	138	158
Central Cell Length (m)	165	131	88	100
Central Cell Plasma Radius (m)	0.76	0.49	0.37	0.51
Average Beta (%)	40	28	60	73
Maximum Field in End Cell (T)	14	24	24	24
Neutron Wall Loading (MW/m ²)	2.4	4.3	3.3	0.05
Blanket/Shield	FS/LiPb	FS/LiPb	FS/LiPb/He/Be	FS/H ₂ O
Net Efficiency (%)	39	34	35	49
COE (mills/kWh)	36	46	41	34+ ³ He cost

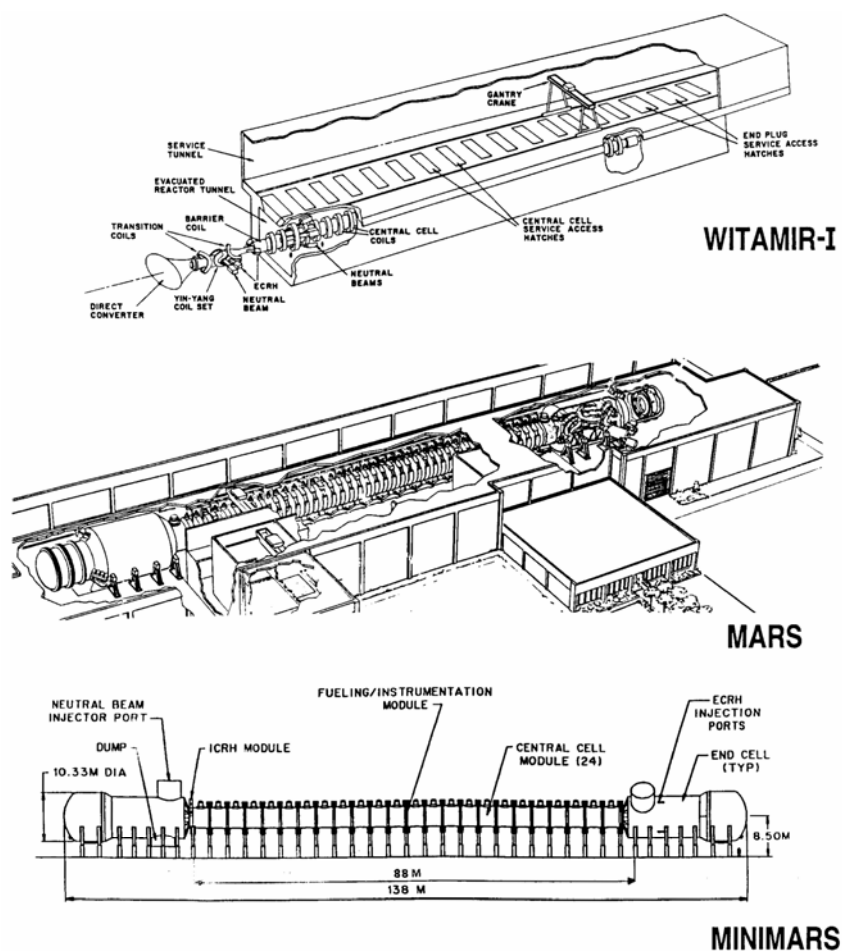


Figure 21. Overall configuration of WITAMIR-I (250 m), MARS (220 m), and MINIMARS (138 m).

In 1987, the UW group proposed the Ra [176] TM power plant that burned D and ^3He from lunar soil. Most of the energy in the D- ^3He reaction is in the form of charged particles, commensurate with direct conversion. Thus, Ra channels about half of the 14.7 MeV protons to the direct converter before they thermalize within the central cell, resulting in a high system efficiency of $\sim 50\%$. The key engineering features of Ra include very low NWL, permanent central cell components, no tritium breeding blanket, and longer overall length compared to MINIMARS [175]. References 177 and 178 present the 1990s view of what needed to be done differently to revitalize the TM program, proposing new configurations for both D-T and D- ^3He fuel cycles. Only a few minor improvements were recommended for MINIMARS, while several changes were proposed for Ra to enhance the overall efficiency, shorten the length, and lower the cost.

Initiated by the US-DOE, Forsen [179] reviewed the progress of the mirror program through the 1980s. Despite Forsen's strong recommendations to continue the TM program, the DOE canceled the nearly completed MFTF-B TM experiment in 1986 and began to terminate the TM program in favor of tokamaks and non-TM alternate concepts. Worldwide, the magnetic trap systems with open ends (or mirror machines) were actively developed in Russia during the 1950-1980 time period. However, a shorted magnet kept the planned upgrade of the AMBAL simple mirror to the AMBAL-M tandem mirror from being accomplished [180], exacerbated by budget difficulties. Currently, the development of magnetic traps is progressing at the Budker Institute of Nuclear Physics in Novosibirsk where two facilities are under operation: the gas-dynamic trap and multi-plug trap [181,169]; neither of these is a TM. In Japan, the only operational TM experiment is GAMMA 10 at the University of Tsukuba [182,183]. It is considered by the Japanese fusion society as an educational device, rather than a facility for energy research. Looking forward, there is no strong growth potential for the TM concept. The US DOE made no effort to revive the TM concept for energy applications despite the 1990s effort [177,178] to readdress the TM commercial viability and stimulate interest in the TM concept.

4. Fusion Roadmaps and Timeline of Fusion Power

All previous studies have identified the ultimate characteristics of fusion power plants in a fully mature, commercial fusion market (tenth of a kind plant). As will be discussed shortly, the main concept supporting the path from ITER to a power plant is the tokamak. The alternate concepts will help validate the fusion science, offer possible improvements for future tokamaks, and provide risk mitigation (alternate pathways). Since the early 1970s, researchers have been developing roadmaps with the end goal of operating the first fusion power plant in 50 years (i.e., by 2020), believing strongly that fusion should be an option in the 21st century energy mix. But this has been a sliding scale vision with the current expectation still remaining at 50 years in some countries. Recently, optimism about fusion has resurfaced with the construction of ITER in France. Nevertheless, developing fusion energy will cost billions of dollars and would span decades. The key strategic questions are: what technologies remain to be developed and matured for a viable fusion power plant, what other facilities will be needed between ITER and the first power plant, what will it cost, and how long will it take, assuming the existing social and political climate continues? On the other hand, if the social and political climate creates a demand-pull situation, how long will it take

to construct the first fusion power plant if the fusion program is treated as a “Manhattan” project with unlimited funds and a limited timetable?

In the early 2000s, the US Fusion Energy Sciences Advisory Committee (FESAC) developed a plan with the end goal of the start of operation of a demonstration (Demo) fusion power plant in approximately 35 years. The Demo is viewed as the last step before the first commercial power plant. The FESAC plan recognized the capabilities of all fusion facilities around the world and identified critical milestones, key decision points, needed major facilities, and required budgets for both magnetic fusion energy (MFE) and inertial fusion energy (IFE) [184]. Assuming ITER operates successfully, the FESAC report recommends three MFE facilities before the construction of a tokamak Demo in 2029: Performance Extension Facility, International Fusion Materials Irradiation Facility (IFMIF) [185] operating in parallel with ITER, and Component Test Facility (CTF) operating in 2023.

A more recent FESAC study highlighted the specifics of the US Demo [186]. It is a net electrical power producing tokamak plant, demonstrating fusion is practical, reliable, economically competitive, and meeting public acceptance, operating reliably and safely for long periods of time, and employing the same physics and engineering technologies that will be incorporated in commercial power plants. This last requirement is fundamental in determining the unique features of the US Demo that demonstrates and matures the commercial power plant systems. Generally, the US plan has an aggressive vision for Demo (based on advanced modes of performance and operation) with the CTF as an essential element of the US fusion development program.

In 2001, the Europeans decided to follow a “fast track” approach [187] and develop the fusion power with one device only (a tokamak Demo) between ITER and the first commercial power plant. Both ITER and Demo will be accompanied by an extensive R&D program and specialized facilities (such as IFMIF) to investigate specific aspects of plasma physics, plasma engineering, fusion technology, and materials [188]. In the reference scenario, the EU Demo construction starts after the completion of ITER phase-I operation and operates around 2027. The EU Demo should satisfy the safety and public acceptance requirements for EU fusion power plants. The near-term Models A, B, and AB of PPCS (refer to Section 3.1.2) are currently the main candidates for the EU Demo [188]. For the first power plant, however, more advanced concepts – such as the PPCS Model C – are also being seriously considered. Therefore, in Europe, fusion could be a practical energy source without advanced modes of plasma operation or major material advances, even though the benefits of such advances are clearly recognized and will be integrated in the power plant designs, if proved feasible. The EU Demo will operate in two phases: the first phase confirms the FW lifetime and provides info on compatibility and reliability issues, while the second phase is more oriented towards commercial aspects such as electricity production, tritium self-sufficiency, high availability, and extended operation. The electricity generation from the first EU commercial fusion power plant is anticipated ~40 years from the go-ahead decision on ITER construction.

In Japan, the 2005 “National Policy of Future Nuclear Fusion Research and Development” document [189] outlines the four phases of fusion research. The ongoing 3rd phase will help make the decision to construct a tokamak Demo. Besides the basic R&D and Broader Approach (BA) activities, the major supporting facilities during the current 3rd phase include ITER, IFMIF, and national experimental facilities (JT-60SA, LHD, and FIREX). The 4th phase will assess the technical feasibility of Demo [190,191] that must operate in a steady-state mode without interruption for at least one year. Other characteristics include high

availability, high efficiency, tritium breeding ratio > 1 , 10-20 MWy/m² neutron fluence and 1 MW/m² heat flux at the FW, and higher heat loads at the divertor. Just recently in 2008, the Japanese fusion community proposed a roadmap to Demo as a model case [192]. The success of ITER and ITER-TBM is regarded as the most important milestone in the newly proposed roadmap that has not been officially endorsed by the Japanese government yet. The 2008 roadmap suggests that IFMIF operates in parallel with ITER while Demo operation starts in 2035 (near the completion of ITER Phase-II operation) with electricity production in 2039. Japan will be ready for power plant construction by ~2050 following the demonstration of a stable, long operation of Demo. As noticed, the Japanese strategy suggests advanced operating modes, stressing the importance of technology and materials developments, but without mentioning a CTF.

In China, three steps have been envisioned for the Chinese fusion program [193,194]:

1. Speed-up of the domestic fusion program in 2006-2010
2. Establish solid domestic MFE base in 2011-2020
3. Fast track for Demo construction in 2021-2040.

The EAST, HT-7, and HL-2M experimental devices play an essential role in establishing the physics and technology bases for steady-state tokamaks. Major new facilities to be constructed in China by 2040 include FDS-I (a hybrid reactor with 150 MW fusion power for transmutation of fission wastes and breeding of fissile fuels) [195], FDS-ST (a spherical tokamak reactor to test the T breeding technology) [196], FDS-II (an electricity generator reactor with 2500 MW fusion power, high power density, high thermal efficiency, and comparable mission to that of US and EU Demos) [75], and FDS-III (a high temperature reactor with 2600 MW fusion power for hydrogen production) [197].

Clearly, the pathway to fusion energy is influenced by the timeline anticipated for the development of the essential physics and technologies for Demo and power plants as well as the demand for safe, environmentally attractive, economical, and sustainable energy sources. Worldwide, the roadmaps take different approaches, depending on the anticipated power plant concept and degree of extrapolation beyond ITER. Several Demos with differing approaches should be built in the US, EU, Japan, China, and other countries to cover a wide range of near-term and advanced fusion systems. Recognizing the capabilities of national and international fusion facilities, it appears that, with unlimited funding, the first fusion power plant could add electricity to the grid by 2030-2035. On the other hand, with limited funding and no clear vision, the timetable could extend beyond the proverbial 50 years.

Since electricity from fusion is a few decades away, numerous researches [198-203, 49,195,197] suggested a departure from the traditional approach of making electricity and proposed a number of non-electric applications, such as hydrogen production, transmutation of fission waste, breeding of fissile fuels, production of medical radioisotopes, desalination, space propulsion, explosives detection, and altering materials properties. These applications take advantage of the neutron-rich fusion system and offer near-term opportunities to advance fusion development with modest physics and technology requirements. If successful, the public will retain interest in fusion and recognize its potential contributions to society before fusion penetrates the commercial market in 2030 or beyond.

5. Conclusion

Numerous fusion studies (> 50), extensive R&D programs, more than 100 operating experiments, and an impressive international collaboration led to the current wealth of fusion information and understanding. As a result, fusion promises to be a major part of the energy mix in the 21st century. Power plant studies will continue to be developed and future design processes will deliver more efficient, safe, economical, and maintainable designs that operate at peak conditions. Internationally, the D-T fuelled tokamak is regarded as the most viable candidate for magnetic fusion energy generation. Its program accounts for over 90% of the worldwide magnetic fusion effort. The R&D activities for the six alternate concepts are at different levels of maturity. Even if these alternate concepts are not successful in realizing the path to a power plant, their physics and technology database will offer possible improvements for tokamaks and fusion sciences in general.

The philosophy adopted in international designs varies widely in the degree of physics extrapolation, technology readiness, and economic competitiveness:

- US view: power plant must be economically competitive with other available electric power sources, mandating advanced physics and advanced technology
- EU/Japan view: the first generation of power plants will enter the energy market because of major safety/environmental advantages and large fuel reserve, even if they produce electricity at somewhat higher cost.

The future of fusion power looks bright and fusion will certainly be a major part of the energy mix in 2030 and beyond. The fusion roadmaps take different approaches internationally, depending on the degree of extrapolation beyond ITER. Evidently, several tokamak Demos should be built in the US, EU, Japan, China, and other countries to cover a wide range of near-term and advanced fusion systems. With limited funding and no clear vision, the timetable could extend beyond the famous 50 years that fusion researchers have been envisioning since the early 1970s. Nevertheless, with unlimited fusion funding, the first fusion power plant could add electricity to the grid by 2030-2035.

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Chapter 7

OPTIMIZATION OF CONFIGURATION UNDER DOMINANT ELECTRON HEATING IN TOKAMAKS

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Abstract

Higher power LH wave (1.5MW) is injected into the diverted plasma with a slightly asymmetric spectrum. Dominant electron heating and current profile control are investigated with numerical simulation. Plasma heating by electron Landau interaction results in operation scenarios of preferentially dominant electron heating. Due to the off-axis driven current, an optimized q-profile is formed, and an enhanced confinement regime with steep electron temperature gradient is produced. The clear decrease of the electron thermal conductivity in the LH power deposition region shows that an electron-ITB is developed. Establishment of the current profile like in the hybrid scenario is studied under the condition of dominant electron heating in HL-2A. The scenarios with injecting LH and EC waves are under numerical study. Carefully adjusting the position of non-inductive current driven by two groups of gyrotron, an optimized q-profile was obtained with $q_a=3.78$ and weak shear region extending to $\rho \sim 0.45$ (where ρ is the square-root of toroidal flux normalized to its value at plasma boundary) in low-density discharges of $\bar{n}_e = 1.0 \times 10^{19} m^{-3}$. When 0.5 MW LH power in the current drive mode and 0.95MW EC power mainly for plasma heating are used to control the current profile, a hybrid discharge scenario with weak magnetic shear region extended to $\rho = 0.6$ and $q_a = 3.21$ is established through controlling the EC absorption position. The mechanism of the LH wave absorption in the HL-2A plasma causes interplay of the distribution of the LH driven current with the modification of the plasma configuration, which constitutes non-linearity in the LH wave deposition. Due to the non-linearity the LH wave deposition position changes spontaneously or oscillates. The oscillatory behavior caused by non-linear effect of the LH wave deposition is analyzed.

1. Introduction

Significant progress has been achieved in reducing anomalous energy transport in tokamak plasmas. In addition to the well-known improved H-mode regime with a transport barrier at the plasma edge, the experiments performed in most tokamaks have shown that spontaneous reduction in anomalous transport can also occur inside the plasma core to form an internal transport barrier (ITB). Though the ion-ITB, which manifests itself by higher gradient of ion temperature inside it, has been studied extensively, the database and the physics understanding of electron-ITBs (eITB) are not so much extended as those of ion-ITB. In many tokamaks, such as FTU, DIII-D, TCV, ASDEX Upgrade, and JT-60U [1-5], electron cyclotron resonance heating (ECRH), which is a dominant electron heating scheme, has been used to enter in improved confinement electron mode in low collisional plasma and eITBs have been observed. By using the available lower hybrid scheme in HL-2A, the plasma heating by electron Landau interaction can establish operation scenarios of preferentially dominant electron heating to develop electron internal transport barrier.

The plasma transport through electron channel is larger than the neoclassical prediction by about two orders of magnitude, which is attributed to micro-turbulence. The electron transport is affected by turbulence of all wavelength, that is short wavelength ($k_\theta \rho_s > 1$, where k_θ is the poloidal wave vector and ρ_s the ion gyro-radius calculated at $T_e = T_i$) like electron temperature gradient mode (ETG), intermediate wavelength ($k_\theta \rho_s \sim 1$) like trapped electron mode (TEM), and long wavelength ($k_\theta \rho_s \sim 0.1$) turbulence like ion temperature gradient mode (ITG). Gyrofluid simulation [6] and gyrokinetic simulation [7] have found the stabilizing effect of negative shear and large pressure gradient in the $(\hat{S} - \alpha)$ ballooning diagram (where $\hat{S}$ is the magnetic shear, $\alpha = -q^2 R \beta'$) on the TEM/ETG instabilities. Therefore, control and optimization of the plasma current profile is a key point in enhancing the plasma performance.

It is demonstrated that the configuration with flat q -profile in the central plasma region is beneficial to improving plasma confinement. Discharges with ITB have been established with optimized magnetic shear (OS) in JET [8] and ASDEX Upgrade [9], and the developed ITB improved central plasma confinement. Recently, so called hybrid scenarios characterized by a current density profile, enclosing a large volume of low magnetic shear with q_0 near 1, have achieved improved confinement and higher beta limits [10-14]. Combining high fusion gain and steady state operation, the ELMy H-mode has been the reference regime for ITER design so far. However, this performance is limited at moderate plasma pressure (typically for $\beta_N < 2$) by the triggering of neoclassical tearing modes (NTM) driven by the sawtooth $m = 1, n = 1$ activity. Hybrid mode has successfully eliminated the deleterious effect of sawteeth and reduced the NTMs triggering by establishing a current profile in stationary state with q above unity. In many experiments these hybrid discharges have produced high fusion figures of merit $\beta_N H_{89} / q_{95}^2 \sim 0.4-0.6$ (where β_N is the normalized plasma pressure, H_{89} the confinement factor in terms of ITER-89 scaling) in stationary conditions at reduced plasma current. Fusion performance at this lower current is maintained by operation at β_N up to 3. The enhanced performance relative to the conventional ELMy H-mode scenario offers the potential of achieving similar values of Q_{fus} at lower plasma current, thereby increasing the

duration over which such performance can be maintained and reducing the risk of damage due to disruptions.

Various improved confinement scenarios have been established by tailoring current profile with RF (radio-frequency waves)-only schemes. In FTU, plasmas with a large central volume of improved confinement are obtained with combined injection of LHCD (lower hybrid current drive) up to 1.9MW, and electron cyclotron waves up to 0.8MW [15]. The improved confinement region extends over a region where a flat or slightly reversed magnetic shear is established by off-axis LHCD and can be even larger than $r/a = 0.5$. Transport modelling of these discharges shows that the ion heat diffusivity is close to the neo-classical value, while the electron shear dependent Bohm gyro-Bohm model accounts quite well for $T_e(r, t)$ [16]. The local scale length for the variation of the electron temperature gradient, characteristic of the electron temperature gradient driven turbulence, becomes larger than the threshold value found for an ITB in JET. Within the accuracy of the estimation of the current profile, q_0 seems to be rather close to 1.5 and the magnetic configuration seems to be intermediate between the ‘optimized’ shear configuration producing ITBs and the hybrid configuration. In TCV, improved confinement regimes, called ‘improved core electron confinements’ (ICECs) have been developed by using the flexible ECCD (electron cyclotron current drive) system [17]. To develop a stable ICEC, strong localized off-axis ECH (electron cyclotron heating) is used, which generates a broader, stable current profile. By playing with the localized counter-ECCD, reverse shear or low shear magnetic configurations with q_0 close to 1 are obtained, and improved confinement regimes are achieved. In particular, with a low shear configuration, very stationary stable plasmas with significantly improved confinement have been achieved. Large temperature gradients have been achieved, but these regimes are not called ITBs, because large abrupt changes in the temperature gradient are not observed. Experiments at Tore Supra have been performed using only ICRH (ion cyclotron resonance heating) in the minority heating scheme. The hollow current density profile was formed during the rapid I_p ramp-up ($dI_p/dt = 1.6\text{MA s}^{-1}$), just before the application of ICRH power [18]. The reversed shear configuration was transiently obtained with $q_0 = 2.5\text{--}3$, and the minimum value of q (~ 2.0) is found to be located between $r/a = 0.5$ and 0.6 . During the current plateau, this transient hollow profile evolves and remains flat until the end of the I_p plateau. The central q has been maintained between 1 and 1.5 for about 1 second. In such discharges, the density fluctuations, measured at $k = 8\text{ cm}^{-1}$, were significantly reduced together with heat transport in the core region. Consequently, the energy confinement time exceeded the L-mode value by 40–50%.

High performance plasmas have been obtained using NBI (neutral beam injection) heating. In these plasmas the ion temperature usually exceeds the electron temperature, which has the advantage of decreasing the plasma transport [19]. Nevertheless, in a next step device most of the fusion-exhaust α -particles should transfer their energy to the electrons and this increases the likelihood of a hot electron plasma. The studies showed that transport in hot ion and hot electron regimes can be very different [20, 21]. It is important to understand the hot electron regimes by dominant electron heating. The transport of the NBI heated hybrid discharges in DIII-D have been simulated [12] with GLF23 model. The GLF23 model [22] is a gyrofluid representation of the transport due to ITG, TEM and ETG modes and can optionally include the effect of $\mathbf{E} \times \mathbf{B}$ on shear on the mode spectrum and associated transport. Modeling of this class of discharges indicates that inclusion of $\mathbf{E} \times \mathbf{B}$ shear stabilization is an important ingredient in reproducing the measured temperature profiles. While this is

suggestive that $\mathbf{E} \times \mathbf{B}$ shear stabilization is important in these discharges, it is unclear whether the improved confinement is due to large levels of $\mathbf{E} \times \mathbf{B}$ shear, which could be caused by the substantial momentum injection during NBI, or reduced turbulence drive associated with a favorable current profile with low magnetic shear. The improved confinement scenarios established by the RF-only control scheme should be helpful to distinguish these mechanisms for the confinement improvement.

HL-2A is a divertor tokamak at SWIP (Southwestern institute of Physics), Chengdu, China, with main parameters of major radius $R = 1.64\text{m}$, minor radius $a = 0.4\text{m}$, toroidal magnetic field $B_T = 2.8\text{T}$, and plasma current $I_p = 0.48\text{MA}$. In HL-2A, the various schemes of auxiliary heating and current drive including NBI (2MW), LHCD (1MW) and ECRH (2MW) combined with diversities of plasma fuelling method including pellet injection, SMBI (supersonic molecular beam injection), and gas-puffing offer opportunities to optimize the plasma profiles. A single-null divertor (SND) configuration had been established by magnetic control [23, 24]. In order to elevate the plasma parameters and achieve more interesting operation scenarios, optimization of the plasma profiles will be carried out using the available controlling schemes. Pellet injection has been used to produce high density plasma. The pellet injection experiment produced high peaked density plasma with hollow electron temperature profile, and the plasma confinement is improved [25]. In such a discharge of $q_a = 3.4$ the sawtooth oscillation disappears following the pellet injection implying that the current profile is flattened in the central plasma region. In the absence of an MSE measurement, the experiment is analyzed with TRANSP and it is indicated that a configuration with low central magnetic shear was produced after the pellet injection [26].

Access to the hybrid mode requires establishment and maintenance of a q -profile with $q_0 \geq 0$ and low central magnetic shear. Although the current density profile produced with pellet injection is characterized by $q_0 > 0$ and low central magnetic shear, it is transient and not controllable. In order to establish stationary optimized q -profile, RF waves will be applied to control the current density profile. To know the prospective operation scenario, here, establishment of the configuration with central weak magnetic shear is investigated under the current profile control by EC and LH waves. While LHCD is an efficient scheme to generate non-inductive current for controlling the plasma current profile, under the weak electron Landau damping condition of the HL-2A plasma LH wave rays make many passes through the wave propagation, which constitutes the LH power deposition nonlinearity [27,28]. To realize the availability of robust control algorithms for the feedback scheme of the current profile control, the flexible control of driven current position is essential. Due to the uncertainties in the LH wave power deposition related to the nonlinearity, LHCD, as one of the effective schemes for current profile control, will be constrained by some conditions.

2. LH Wave Absorption by Landau Damping

By choosing a local Cartesian coordinate system [29], such that $\hat{\mathbf{z}} \times \bar{\mathbf{B}} = 0$ and $\bar{\mathbf{k}}$ is contained in the x-z plane:

$$\bar{\mathbf{k}} = k_{//}\hat{\mathbf{z}} + k_{\perp}\hat{\mathbf{x}}, \quad (1)$$

the LH wave dispersion relation is decomposed into its real and imaginary parts,

$$D = D_r + iD_i = 0 \quad (2)$$

with

$$D_r = -\alpha k_\perp^6 + \varepsilon_\perp k_\perp^4 + [(\varepsilon_{//} + \varepsilon_\perp)(k_{//}^2 - \varepsilon_\perp k_0^2) + \varepsilon_{xy}^2 k_0^2] + \varepsilon_{//} [(k_{//}^2 - \varepsilon_\perp k_0^2)^2 - \varepsilon_{xy}^2 k_0^4] \quad (3)$$

$$D_i = \frac{\partial D_r}{\partial \varepsilon_{//}} K_{zz,i} \quad (4)$$

where $k_0 = \omega/c$, $\varepsilon_{//}$, $\varepsilon_\perp$, and ε_{xy} are components of the dielectric tensor for a cold plasma, α is the thermal term that would be important near lower hybrid resonance,

$$\alpha = \frac{3}{4} \frac{\omega_{pe}^2}{\omega_{ce}^4} v_{Te}^2 + 3 \frac{\omega_{pi}^2 v_{Ti}^2}{\omega^4} \quad (5)$$

where ω_{pe} and ω_{pi} are the plasma frequencies, v_{Te} and v_{Ti} are the thermal velocities. The anti-Hermitian part of the dielectric tensor $\vec{\tilde{K}}$ is retained as a perturbation. Here, the principal such term enters as an imaginary correction to K_{zz} , which describes the Landau damping of the LH wave in plasmas, namely the interaction between the wave electric field component parallel to $\vec{B}$ and electrons whose speed along $\vec{B}$ matches that of the wave:

$$K_{zz,i} = -\pi \frac{\omega_{pe}^2}{\omega} \int dv_{//} v_{//} \frac{\partial f_e}{\partial v_{//}} \delta(\omega - k_{//} v_{//}) \quad (6)$$

where $f_e(v_{//})$ is a parallel velocity distribution function of electrons normalized such that $\int dv_{//} f_e(v_{//}) = 1$.

The calculation for the LH wave absorption utilizes a toroidal ray-tracing algorithm for the wave propagation and a parallel velocity Fokker-Planck calculation for the interaction of waves and particles. Here a 1-D collision operator is used in the Fokker-Planck equation [30].

A spectral component of power W experiences a change ΔW over time interval $\Delta \tau$.

$$\begin{aligned} \Delta W &= -2D_i \left/ \left(\frac{\partial D_r}{\partial \omega} \right) W \Delta \tau \right. = -2 \frac{\partial D_r}{\partial \varepsilon_{//}} K_{zz,i} \left/ \left(\frac{\partial D_r}{\partial \omega} \right) W \Delta \tau \right. \\ &= 2\pi \frac{\omega_{pe}^2}{\omega} \int dv_{//} v_{//} \delta(\omega - k_{//} v_{//}) \times \frac{\partial D_r}{\partial \varepsilon_{//}} \left/ \left(\frac{\partial D_r}{\partial \omega} \right) W \Delta \tau \right. \end{aligned} \quad (7)$$

The current driven on each flux surface is calculated according to

$$j_{LH} = \frac{-en_e}{v_r} \int dv_{||} D_{ql}(v_{||}) \frac{\partial f_e(v_{||})}{\partial v_{||}} \frac{\partial W_s(u)}{\partial u} \quad (8)$$

where

$$D_{ql}(v_{||}) = \frac{\pi}{2} \left(\frac{e}{m_e} \right)^2 E_{||}^2 \delta(\omega - k_{||} v_{||}), \quad (9)$$

$$v_r = (\ln \Lambda) n_e e^4 / 4\pi \varepsilon_0^2 m_e^2 |v_r|^3, \quad (10)$$

$$v_r = -\text{sgn}(eE_{DC}) \sqrt{n_e e^4 \ln \Lambda / 4\pi \varepsilon_0^2 m_e |eE_{DC}|}. \quad (11)$$

The key quantity is $W_s(u)$, the energy (normalized to $m_e v_r^2 / 2$) imparted to the electric field E_{DC} by an electron as it slows down.

3. Lower Hybrid Wave Heating and EITB

The SDN (single null divertor) plasma ($\delta_L=0.25$, $k \sim 1.0$) obtained in the HL-2A experiment [23] is used in the simulation. The geometry of the boundary (99.8% flux surface of the diverted plasma) is specified as a general function of time, which evolves from circular to the SDN shape during the current ramping-up phase and then keeping the same shaped boundary during the current plateau. The interior flux surfaces, which are computed by solving the Grad-Shafranov equation, are parameterized by the square-root of the normalized toroidal flux, ρ .

The LH wave is injected with a multi-junction launcher (2×12) in HL-2A. The radiated power spectrum by the launcher is calculated with the Brambilla coupling theory [31]. The calculated spectrums for different relative wave-guide phasing ($\Delta\phi$) are shown in Fig. 1. The asymmetric power spectrum ($\Delta\phi = 90^\circ$) is used for current drive, and the symmetric spectrum ($\Delta\phi = 180^\circ$) is used for plasma heating. In order to produce some non-inductive current to control the current profile, a nearly symmetric spectrum of $\Delta\phi = 170^\circ$ is assumed in the LH heating simulation. The Brambilla coupling calculation combined with the LH absorption calculation is in conjunction with the TRANSP code to obtain the plasma heating in a dynamic case. The local electric field E_{DC} is supplied by TRANSP as part for iteration.

The energy transport model is a mixed theory model. Normally the transport observed in tokamak experiments greatly exceeds that of collisional transport theory and this anomalous transport is usually attributed to turbulent fluctuation arising from various micro-instabilities. Since ion energy transport drops to roughly the neoclassical level in the enhanced confinement regime, the ion heat diffusivity is assumed in terms of neoclassical transport enhanced by η_i turbulence. The electron energy transport is based on the Rebut-Lallia-Watkins (RLW) model [32] which, from heuristic and dimensional arguments, introduces a critical electron temperature gradient ∇T_{ec} such that the electron heat flow is neoclassical

when $|\nabla T_e| < |\nabla T_{ec}|$ or $\nabla q < 0$. The ion temperature gradient or η_i driven fluctuations are drift waves with $1/k_\theta \leq \rho_s$. The RLW model is a gyro-Bohm model that would be appropriate for diffusive transport due to fluctuations on a micro-scale ρ_s . The distinctive feature of this hybrid model is that both η_i model and RLW model have been tested against a wide range tokamak devices [33].

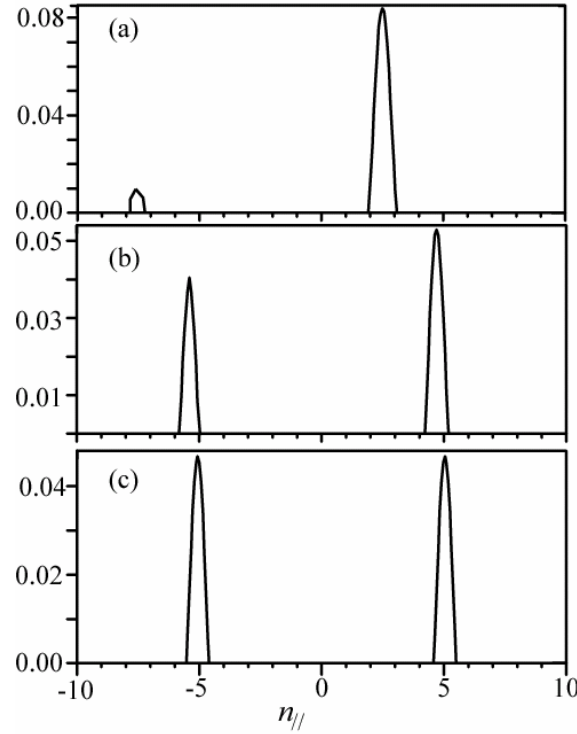


Figure 1. Relative LH wave power versus $n_{||}$. (a) $\Delta\phi = 90^\circ$, (b) $\Delta\phi = 170^\circ$, (c) $\Delta\phi = 180^\circ$

Scenario of the low-density plasma heated by higher LH power is investigated. The parameters of the target plasma are: $I_p = 220\text{kA}$, $B_T = 2.0\text{T}$, and $\bar{n}_e = 1.0 \times 10^{19}\text{m}^{-3}$, deuterium gas. 1.5MW LH wave power is injected during the flat-top phase ($t = 0.7 - 1.2\text{s}$). Due to higher power electron Landau heating, electron temperature increases significantly. The central electron temperature is about 0.6keV before injection of the LH wave, and rises to more than 1.4keV during the LH heating phase (Fig. 2). In contrast to the large increment of the electron temperature, the ion temperature only has a small change (namely, the central T_i rise $\Delta T_{i0} \approx 0.17\text{keV}$ from the Ohmic value $T_{i0} = 0.5\text{keV}$) because of low plasma density.

Since parallel refractive index of the used LH power spectrum ($\Delta\phi = 170^\circ$) is rather high (the central refractive index $n_{||0} \approx 5.0$), the injected LH wave with lower phase velocity can be absorbed in outer region resulting in off-axis electron heating [Fig. 3(a)]. The analysis of wave deposition region in the $(x, n_{||})$ phase space shows that constraint imposed by the wave propagation condition limits the maximum allowed $n_{||}$ upshift [34]. Taking into account the

Landau damping condition, it is shown that LH power can deposit off-axis. In addition to the plasma heating, a non-inductive current ($I_{LH} \approx 80kA$) is driven by the LH wave because of the slight asymmetry of the LH wave spectrum and the asymmetry of electron velocity distribution function caused by the electric field, E_{DC} . Due to the off-axis driven current, an optimized q-profile, of which the magnetic shear is weak in the central region and negative in the mid-plasma region ($\rho \approx 0.5-0.65$), is formed [Fig. 3(b)].

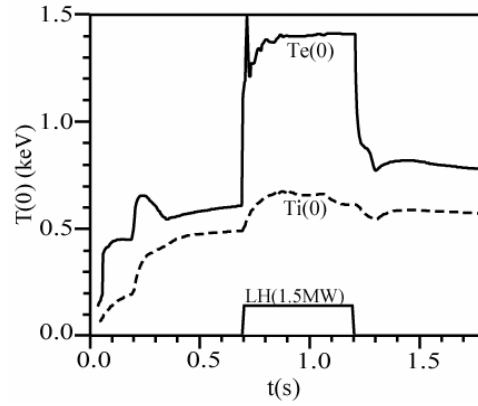


Figure 2. Temporal evolution of central T_e (full line) and central T_i (dotted line).

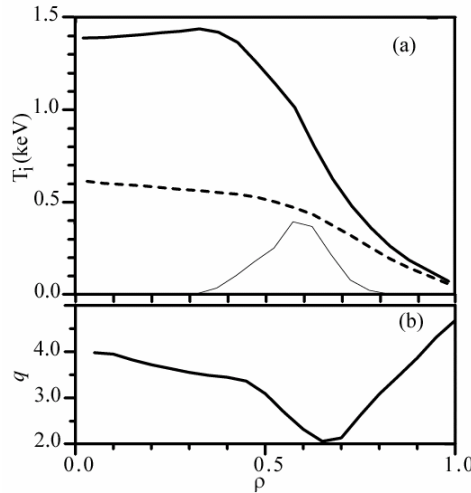


Figure 3. (a) T_e -profiles during the LHH phase at $t = 0.9s$ (full line) and during Ohmic heating only at $t = 0.68s$ (dotted line). Fainter line indicates the location of LH absorption. (b) q -profile at $t = 0.9s$ during LH heating.

The Ohmic and RF profiles of electron temperature are plotted in Fig. 3(a). The RF profile corresponds to 200ms later after the RF has been turned on. LH heating raises electron temperature largely and a steep temperature gradient is formed around the power deposition region. The normalized gradient R/L_T (where $L_T^{-1} = \nabla T_e / T_e$) at the steepest gradient, where $\rho = 0.65$ and the electron temperature $T_e = 0.75keV$, is $R/L_T = 18$. This gradient exceeds

largely the critical gradient value ($R/L_T < 10$) for temperature profile stiffness [35]. The critical gradient is due to short or intermediate wavelength ETG/TEM instabilities. The large R/L_T value is consistent with the expected stabilizing effect of weak or negative shear on ETG/TEM instabilities. In FTU and DIII-D [1,2], ECRH has been used during fast current ramps to obtain negative or flat q -profile by using the skin effect. In the FTU experiment the eITB was developed with extremely high temperature gradient. The maximum normalized gradient of $R/L_T = 19$ was obtained, implying that the plasma behaves as it is far from the stiff critical gradient. In DIII-D, eITB was established with injecting ECRH off-axis into low-density plasma, and an extreme steep temperature gradient was formed outside the power deposition region. The gyrokinetic linear stability analysis on DIII-D showed that the experimentally measured ∇T_e at the barrier was very close to the expected critical gradient for ETG mode, provided the stabilizing effect of negative shear and large pressure gradient in the $(\hat{s} - \alpha)$ ballooning diagram was included in the calculation to reduce the turbulence growth rate.

As in the case of ion-ITB one would clarify an eITB when a clear decrease of the electron thermal conductivity is observed at the steep temperature gradient or in the region inside it. The simulation results show that the electron thermal conductivity is reduced obviously at the steep temperature gradient and in the central plasma region (Fig. 4). IFS-PPPL is a first-principle model of anomalous transport [36, 37]. This model is based on nonlinear gyrofluid simulations, which predict the fluctuation and thermal transport characteristics of toroidal ITG turbulence, and on comprehensive linear gyrokinetic ballooning calculations. The formulas of thermal conductivity in this transport model are theoretically derived results from numerical toroidal simulations; no features of this model were obtained by referring to experimental data. By adding the anomalous transport resulted from the IFS-PPPL model to the neoclassical transport, the evaluation of electron thermal conductivity shows that it decreases obviously at the steep temperature gradient and in the central plasma region (Fig. 4), which is of similar trend to the electron heat diffusivity of the simulated plasma.

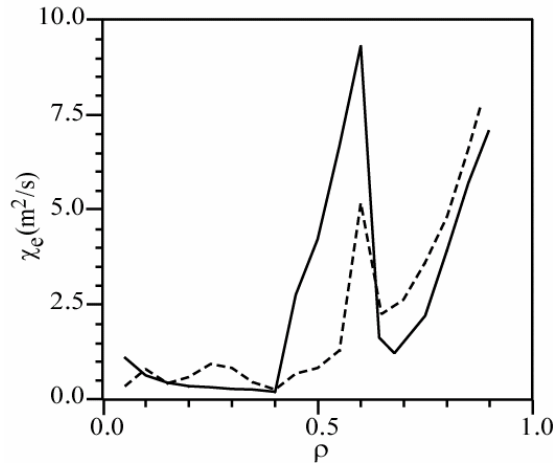


Figure 4. Electron thermal conductivity versus ρ (dotted line), and the electron thermal conductivity calculated from the IFS-PPPL model versus ρ (full line).

A parameter used to describe the eITB characteristics is [38]

$$\rho_T^*(R, t) = \rho_s(R, t) / L_{Te}(R, t), \quad (12)$$

where $L_{Te} = -T_e / (\partial T_e / \partial R)$ is the local temperature gradient scale length, R the plasma major radius on the equatorial plane, $\rho_s = c_s / \omega_{ci}$ the ion Larmor radius at the sound speed, c_s the ion sound speed and ω_{ci} is the ion gyro-frequency. The studies of turbulence transport through extensive use of computer code have found that a possible mechanism for the stabilization of ITG modes and TEMs in tokamaks combines the $\mathbf{E} \times \mathbf{B}$ rotation flow and the magnetic shear effects. The dimensional analysis of the stabilization criterion $\gamma_{E \times B} \geq \gamma_{\max}$ (where $\gamma_{E \times B}$ is the $\mathbf{E} \times \mathbf{B}$ shearing rate, and $\gamma_{\max}$ is the maximum linear growth rate of ITG modes) shows that the strength of the eITB is quantified by the maximum value of $\rho_T^*(R, t)$ profile and its location. For the simulated HL-2A discharge the profile of ρ_T^* is shown in Fig. 5. It is shown that the maximum value of $\rho_T^*(R, t)$ profile is located around the shear reversal point.

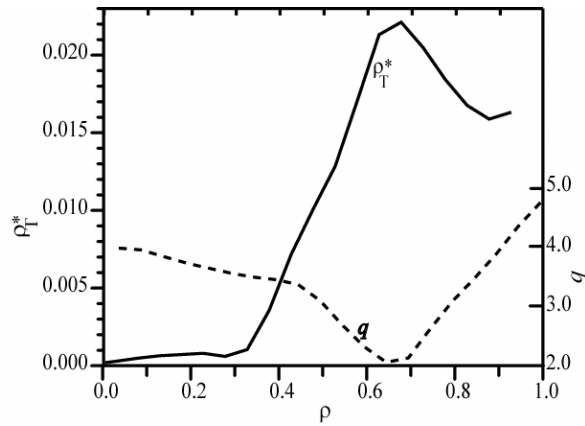


Figure 5. Profile of ρ_T^* (full line) and q -profile (dotted line).

To realize both plasma heating and current profile control by using LH wave, an alternative way is that the LH wave is injected with double-antenna: one antenna is used to radiate symmetric spectrum ($\Delta\phi = 180^\circ$) for heating, and another radiates asymmetric spectrum ($\Delta\phi = 90^\circ$) for current drive. By adjusting the LH power for current drive, a discharge with similar features as in the single-antenna case could be obtained. With 1.5MW LH power used for heating and 0.13MW for LHCD, the electron temperature profile and q profile similar to the profiles shown in Fig. 3 were produced (Fig. 6). Compared to the electron temperature profile shown in Fig. 3, we see that as the plasma profile is controlled by injecting LH wave with double-antenna, the eITB is wider as shown by the electron temperature profile, while the central electron temperature is a little bit lower.

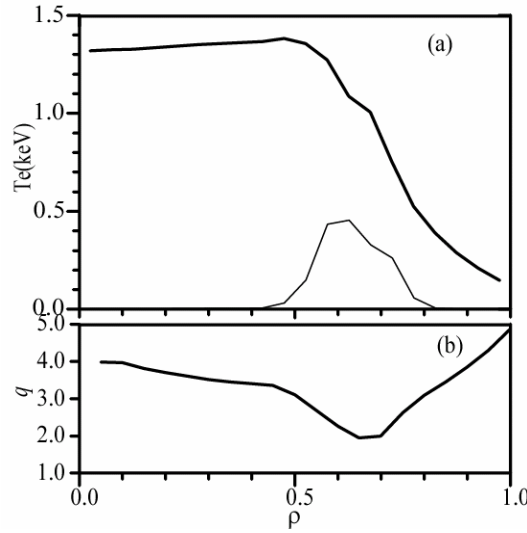


Figure 6. (a) T_e -Profile, fainter line indicating the location of LH absorption. (b) q -profile.

4. Configuration of Flat Q -Profile Established by Profile Control with RF Waves

HL-2A has two different RF systems at different frequency ranges: lower hybrid wave at frequency of 2.45GHz and electron cyclotron wave at frequency of 68GHz. The LH power is generated with 4 klystrons of 0.5MW each and radiated by a multi-junction (2×12) antenna. The EC power is generated by 4 gyrotrons of 0.5MW each. Directions of the radiated EC beams can be varied toroidally and poloidally by rotating the steerable mirrors. The EC and LH waves are injected from the low field side on the equatorial plane. Both of the heating schemes heat electrons directly producing hot electron plasmas. They are proved to be effective current drivers as well, and their driven current profiles were studied in some detail. To know the optimized operation scenario upon the current profile control, the discharges with injecting LH and EC waves are simulated with TRANSP. The propagation and absorption of LH wave is calculated by LSC [30] and that of EC wave is calculated by TORAY-GA [39].

4.1. Profile Control with EC Wave

We first investigate the current profile optimization by carefully adjusting the position of non-inductive current driven by two groups of gyrotron (each consisting of 2 gyrotrons). As a dimensionless parameter $\beta_N H / q_{95}^2$ is considered to be the figure of merit for evaluating the hybrid scenarios, the criterion for the optimization is set to be that the volume of weak magnetic shear with $q_0 \approx 1$ is as large as possible and the edge q (q_a) as low as possible. The target plasma is a Ohmic heating plasma with $I_p = 340\text{kA}$, $B_T = 2.43\text{T}$, and $\bar{n}_e = 1.0 \times 10^{19} \text{m}^{-3}$. In order to drive non-inductive current to control the current profile the

EC wave (ordinary mode at first harmonic) is launched toroidally at an azimuthal angle of 200° , where azimuthal angle is defined as the angle of the central ray with respect to the major radius on the equatorial plane. The driven current position is controlled by the polar launch angle which is defined as the angle of the central ray with respect to the tokamak symmetric axis on the plasma cross section. Since ECCD drives spatially localized current in a smaller scale, the EC waves from different gyrotrons are launched at different polar angles of 82.9° , 79.9° and $2 \times 77.9^\circ$ respectively to produce broad EC power deposition [Fig. 7(b)]. With $2 \times 0.95\text{MW}$ EC power injected at $t = 0.6\text{s}$, the central q gradually increases to $q_0 \geq 1.0$ at $t \approx 0.9\text{s}$ producing a configuration of weak magnetic shear. In this configuration the low shear region extends to $\rho \sim 0.45$ with $q_0 \geq 1.0$, $q_a = 3.73$ [Fig. 7(a)], and it is steadily maintained until the EC power is taken off at $t=1.4\text{s}$.

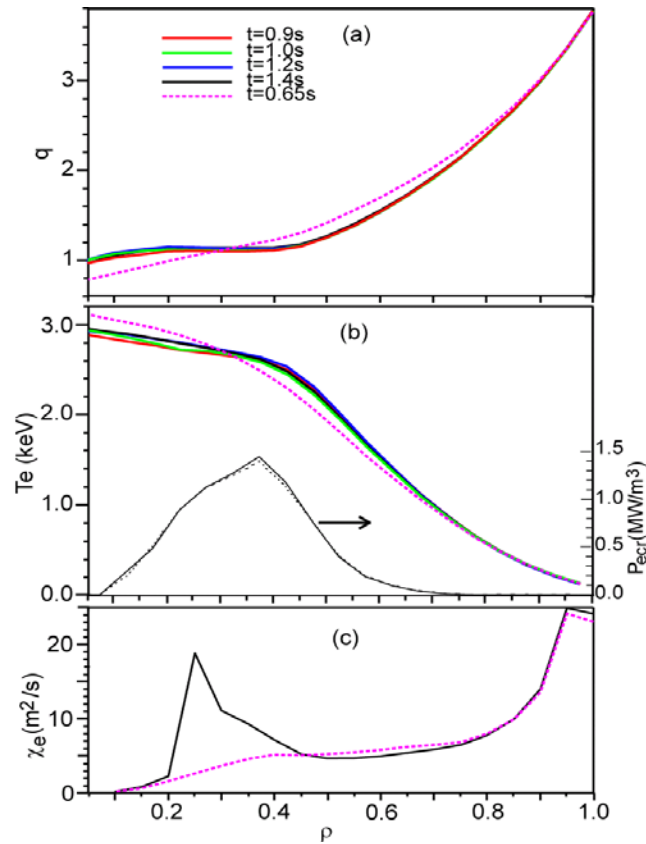


Figure 7. (a) q -profiles, and (b) T_e -profiles at different times during ECCD, dotted lines being profiles at early time when the optimized configuration is not formed. Thin black lines in (b) indicate the EC power deposition at $t=1.4\text{s}$ (full line), and $t=0.65\text{s}$ (dotted line). (c) Profiles of electron thermal diffusivity at $t=1.4\text{s}$ (full line), and $t=0.65\text{s}$ (dotted line).

The time evolution of T_e is shown in Fig. 8. It is shown that concomitant with the q profile evolution the electron temperature temporally evolves forming a stationary profile with a large gradient region after $t \approx 0.9\text{s}$. T_e profile with the regions of low and large temperature gradient, which is concomitant with regions of low and high magnetic shear, have been

obtained, but it is unlikely that a distinct electron-ITB is developed because there is no abrupt decrease of the electron thermal diffusivity corresponding to the steep T_e -gradient region [Fig. 7(c)].

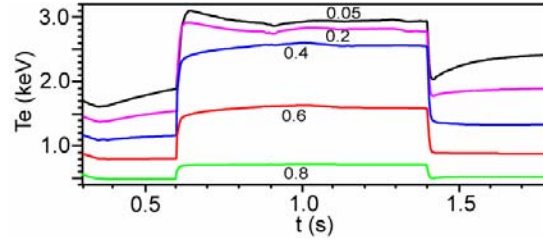


Figure 8. Time traces of T_e at various flux surfaces in EC controlled discharge.

Although ECCD has its strong controllability of local current density, its efficiency is strongly dependent on T_e and rather low. As we try to extend the weak shear region broader by moving the EC power deposition outside, the EC driven current is too low to maintain the optimized magnetic shear. Similarly, if we reduce q_a by increasing the plasma current, the fraction of non-inductive current driven by EC, which is $I_{ec} \sim 60\text{kA}$ in the above case, is not enough to elevate the central q to $q_0 \geq 1.0$. It is also due to the rather low non-inductive driven current by EC that the central weak shear configuration is not able to be formed when the plasma density is higher.

4.2. Profile Control by ECH+LHCD

Considering the advantages of the LH scheme that drives current efficiently, we employ LHCD for large-scale $q(r)$ control. To compare with the EC wave controlled scenario above, here a low-density plasma of $\bar{n}_e = 1.0 \times 10^{19} \text{ m}^{-3}$ and $I_p = 400\text{kA}$, $B_T = 2.43\text{T}$ is considered. The target plasma is heated by EC of $0.48\text{MW} + 0.47\text{MW}$ lunched from 2 gyrotrons. By adjusting the polar lunch angle the EC power from 2 gyrotrons deposits around $\rho = 0.2$ and $\rho = 0.3$ respectively, and one gyrotron is tuned toroidally at an azimuthal angle of 160° to generate some negative driven current ($\sim -20\text{kA}$) to compensate the centrally peaked current. From the evolution of q at various locations shown in Fig. 9, we see that the q -profile has a little change in the ECH phase. To control the current profile, 0.5 MW LH power in the current drive mode (the multi-junction antenna phasing $\Delta\phi = 90^\circ$) is injected. After the LH wave injection the q -profile adjusts slowly, and the safety factor between $\rho = 0.0$ and $\rho = 0.7$ (q has negligible change beyond $\rho = 0.7$) evolves gradually to the new quasi-steady values on resistive time scale. With current profile fully relaxed, the q values of $\rho = 0.0 - 0.6$ constrict to a narrow range of $1.0-1.3$ (Fig. 9), and a q -profile with weak shear region extended to $\rho = 0.6$ and $q_a = 3.21$ [Fig. 10(a)] is established. It is sustained until LHCD is turned off. Though the q -profile in the weak shear region is not as flat as that in the discharge controlled by ECCD, the absolute value of the magnetic shear is rather low [Fig. 10(b)]. Compared to the EC-only control scheme, the current profile control with the combination of EC and LH is more flexible and more efficient. In the ECH+LHCD scenario, the EC wave, which heats the

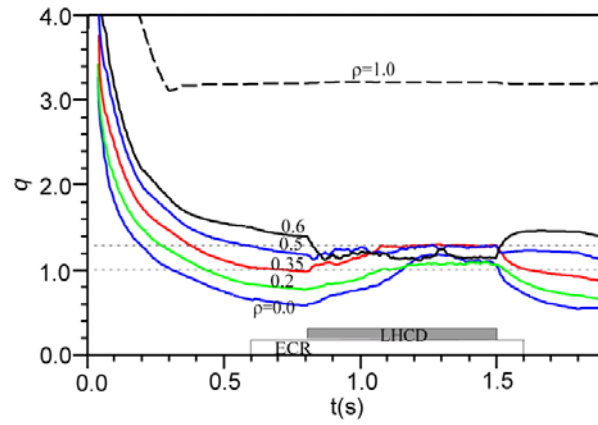


Figure 9. Temporary evolution of q at various flux surfaces in the EC+ LHCD controlled discharge.

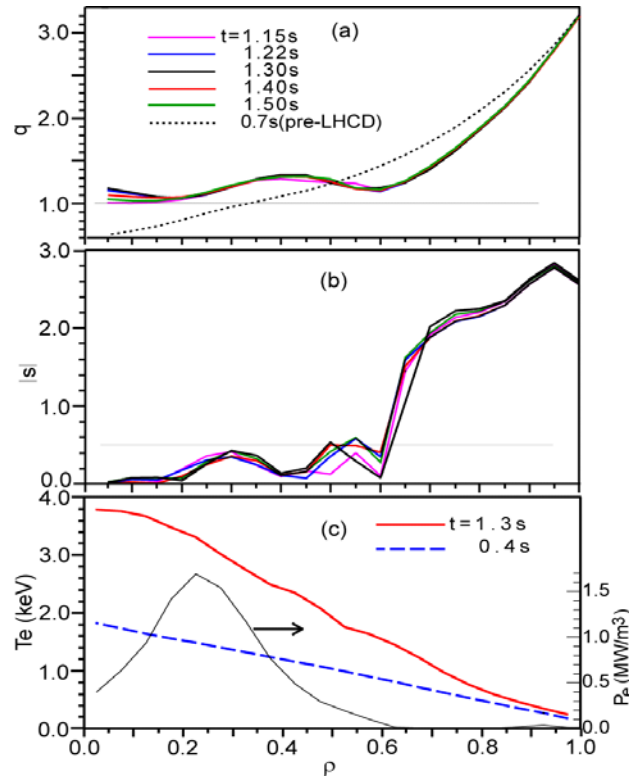


Figure 10. (a) q -profiles (smoothed), and (b) absolute value of magnetic shear versus ρ at various times. (c) T_e -profiles at $t=0.4$ s in Ohmic heating phase, and $t=1.3$ s during optimized current profile phase. Thin black line indicates electron heating power.

plasma efficiently, increases the electron temperature in the central region. The elevation of the central electron temperature makes the LH wave penetrating deeply into the plasma to drive current in a wider region (Fig. 11). It is due to the wider region for the LH current driving and the much higher current drive efficiency ($\eta_{CD} \equiv R\bar{n}_e I_{lh} / P_{lh} \approx 1.5 \times 10^{19} \text{ A/Wm}^2$,

where I_{lh} and P_{lh} is the LH driven current and injected LH power respectively) that the central flat q -profile with wider region and lower edge q value can be formed, and it has achieved fully non-inductive current drive (Fig. 11) with LH driven current of $\sim 440\text{kA}$. By using the similar control scheme, the q -profile with weak magnetic shear region extended to $\rho = 0.45$ and $q_a = 3.36$ has been produced in a higher density plasma of $\bar{n}_e = 2.3 \times 10^{19} \text{ m}^{-3}$.

As shown in Fig. 10(c) the electron temperature gradient does not show an abrupt change corresponding to the optimized current profile as is often the case with an electron-ITB developed. However, the electron temperature increases largely, and its normalized gradient R/L_{Te} (where $1/L_{Te} = \nabla T_e/T_e$) becomes larger than the critical gradient value ($R/L_{Te} < 10$) for temperature profile stiffness in the confinement region of $\rho < 0.8$, characteristic of the suppression of the ETG and/or TEM driven turbulences. These characteristics of the plasma confinement are consistent with the hybrid discharge scenarios established in the tokamak experiments [10-13].

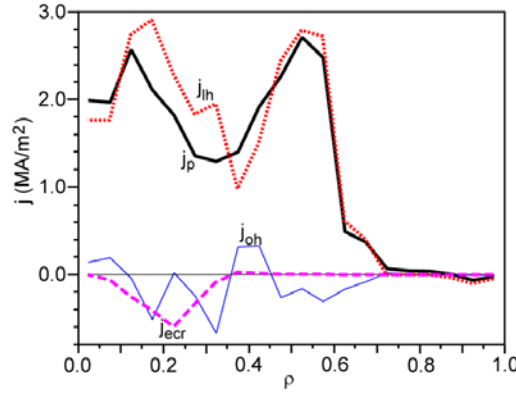


Figure 11. Current profile at $t = 1.4\text{s}$: total plasma current j_p (full line), ohmic current j_{oh} (thin full line), LH driven current j_{lh} (dotted line), and EC driven current j_{ecr} (dashed line).

5. Uncertainties of the LH Wave Power Deposition due to Non-Linearity Effects

In high-temperature reactor grade plasmas, LH waves can be deposit in single pass by strong Landau damping. Nevertheless, the plasma temperature in HL-2A is much lower than that in future reactor. To achieve LH wave power deposition in the weak absorption regime, large spectral gap between the initial parallel LH phase velocity and the electron thermal velocity must be filled in the course of wave propagation. The LH wave propagation domain is defined as the domain in phase space $(\mathbf{r}, \mathbf{k})$ where the wave phase is real. In tokamak geometry the appropriate canonical coordinates are $(r, \theta, \phi, k_r, m, n)$. The components of the wave-vector are $(k_r, m/r, n/R)$ where R and r is the major and minor radius respectively. To fill the spectrum gap one generally relies on the fact that the canonical number m is not conserved in toroidal geometry, and this leads to large $n_{//}$ -upshifts when the wave makes

multiple passes through the plasma before being absorbed. Determined by the toroidal axis-symmetry geometry effect, the limit of $n_{//}$ - upshift in phasing space ($\rho, n_{//}$) is

$$n_{//} \leq n_{//0} \frac{R_0 / R}{1 - (\omega_{pe} / \omega) / (\tilde{q} \varepsilon_{\perp}^{1/2})} \quad (13)$$

where $n_{//0}$ and R_0 refer to the toroidal mode number n launched at the antenna R_0 , $\tilde{q} = q_{cyl} / \varepsilon \rho$ (q_{cyl} is the cylindrical safety factor, and $\varepsilon = a/R$) is a magnetic geometry factor, and $\varepsilon_{\perp} = 1 + \omega_{pe}^2 / \omega_{ce}^2 - \sum_j \omega_{pi,j}^2 / \omega^2$ with ω_{ce} the electron gyro-frequency, ω_{pe} the electron plasma frequency and $\omega_{pi,j}$ the ion plasma frequency of j th species.

Since the wave propagation condition limits the maximum allowed $n_{//}$ - upshift, the LH power deposition is constrained by boundary of the LH wave propagation domain which is [27]

$$\bar{n}_{//} = n_{//0} \frac{R_0}{R} \frac{\tilde{q}^2 \mp \sqrt{1 + (1 + \tilde{q}^2)(\omega_{pe}^2 / \omega^2) / \varepsilon_{\perp}}}{\tilde{q}^2 - [1 + (\omega_{pe}^2 / \omega^2) / \varepsilon_{\perp}]} \quad (14)$$

Furthermore, there are too few velocity-resonant electrons to carry driven current density comparable with the ohmic current density if the LH wave phase velocity is higher than 3.5 times the electron thermal velocity. To guarantee substantially current drive, strong electron Landau damping (ELD) condition of

$$n_{//} \geq 6.5 / \sqrt{T_e [\text{Kev}]} \quad (15)$$

is required. Thus the LH wave absorption is bounded in the region defined by the strong Landau-damping limit and the boundary of wave propagation domain.

The region defined by strong electron Landau damping and the boundary of wave propagation domain is related to the distribution of plasma parameters, especially, the wave propagation domain is dependent explicitly on the safety factor profile. Accordingly, the LH power deposition position depends on both the current and pressure profiles. As the current density profile changed due to LHCD, both the current profile and pressure profile are modified, in turn affecting the LHCD location again. The interplay of the LHCD location with the modification of the plasma configuration constitutes non-linearity in the LH wave deposition.

In tokamak operation, to control the LHCD position through adjusting the radiated power spectrum is a feasible way for the current profile control. We inject LH wave with various radiated spectrums into a NBI ($P_{NB}=2.0\text{MW}$) heated plasma of $I_p = 265\text{kA}$, $B_T = 2.8\text{T}$, and $\bar{n}_e = 2.32 \times 10^{19} \text{m}^{-3}$ to compare the effects of LHCD on the plasma performance. The LH wave power spectrum radiated by the multi-junction launcher (2×12) is calculated with the Brambilla coupling theory. Three radiated power spectrum are produced by changing the

antenna phasing $\Delta\phi$, for which $n_{//c} = 2.48, 2.10$, and 1.65 (where $n_{//c}$ is the central parallel refractive index of the spectrum) corresponding to $\Delta\phi = 90^\circ, 75^\circ$, and 60° respectively.

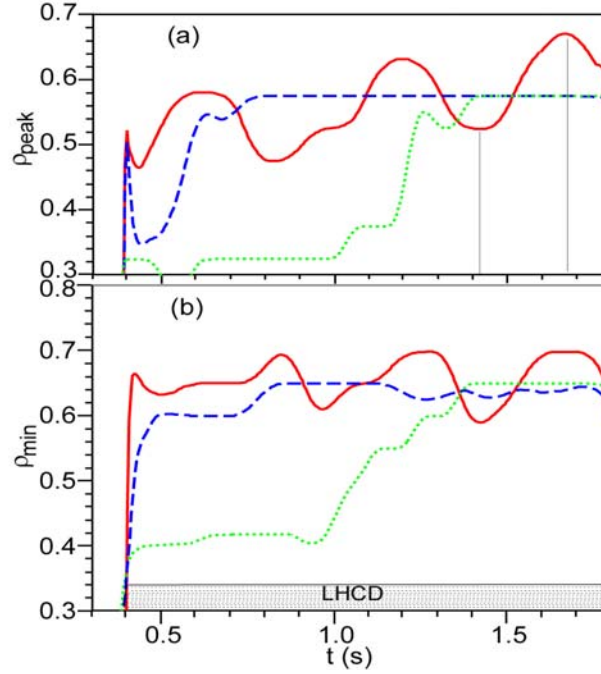


Figure 12. Time evolution of (a) location of peak of the LH driven current profile, and (b) location of the minimum q in the oscillating RS discharge (full line), the two-phase RS discharge (dotted line, the LH driven current profile and q profile are smoothed), and the stationary RS discharge (dashed line).

The LSC code combined with the Brambilla coupling calculation is used in conjunction with TRANSP to model the discharges with profile controlled by LHCD. By using the spectrum produced with $\Delta\phi = 90^\circ$, a stationary off-axis LH-driven current profile is formed. Accordingly, stable reversed magnetic shear (RS) configuration is established (we refer to it as the stationary RS discharge) [40]. As changed the radiated LH spectrum to that of $\Delta\phi = 75^\circ$, location of the LH wave driven current changes spontaneously because of the non-linearity in the LH wave deposition, generating two distinct quasi-stationary RS configurations under the same LH wave control condition in a single discharge (we refer to it as the two-phase RS discharge) [27]. In addition to the two-phase RS discharge, the spontaneous change of the LH wave deposition due to nonlinearity causes oscillatory behavior in plasma as well. As the antenna phasing changed to $\Delta\phi = 60^\circ$ (the target plasma current also changed to $I_p = 300\text{kA}$) the location of the peak of LH driven current presents oscillation with irregular cyclic, which leads to oscillating ρ_{min} (ρ_{min} is the location of the minimum q) (we refer to it as the oscillating RS discharge). These oscillating behaviors are shown in Fig. 12 in contrast with the other two scenarios. In the LHCD controlled plasma ion-ITB is developed due to the RS current profile (Fig. 13). Corresponding to the steep temperature gradient in ITB the ion thermal diffusivity decreases abruptly to a very low value, and the plasma transport inside ITB is only determined by collisional transport model

[Fig. 13(b)] since η_i is much lower than the critical value for triggering η_i turbulence. As the location of ITB following the shear reversal point, the oscillation of $\rho_{\min}$ causes variation of the plasma performance. Shown in Fig. 14 are the pressure profiles and q -profiles at $t = 1.69$ s, 1.42s corresponding to the wave crest and trough of the oscillation. Comparing the plasma configuration at $t = 1.42$ s, a configuration of wider negative shear region and consequently larger area inside ITB is formed at $t = 1.69$ s due to the further outside position of the LH driven current. Because of the larger area of enhanced confinement inside ITB, higher pressure in central plasma is achieved. The temporary pressure evolution shows that variation of the plasma performance caused by oscillatory behavior occurs in the whole region inside ITB [Fig. 15 (a)]. In the vicinity of shear reversal point, the pitch angle of magnetic field line is of rather large oscillating amplitude [Fig. 15 (b)] indicating that it is feasible to measure the oscillation in experiment.

In the LHCD discharges on Tore Supra, a regime with stationary oscillation behavior has been observed because of the nonlinearly coupling effect of both wave-plasma interaction and turbulence suppression by the RS q profile. It is interpreted as that the current density and electron temperature profiles behave as a predator-prey system [41]: Since the turbulence responsible for heat transport is suppressed or reduced by effect of negative magnetic shear, and the LH-driven current density j_{LH} is an increasing function of both current and temperature, whenever the magnetic shear becomes negative in the central part, the confinement is locally improved and T_e increases, then j_{LH} grows, the central q decreases, the shear turns positive again, T_e and j_{LH} decrease, and the cycle restarts.

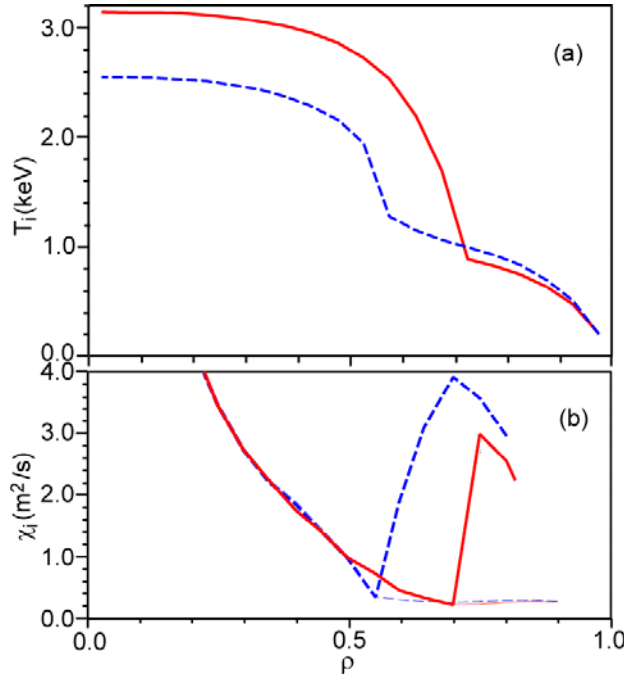


Figure 13. (a) Profiles of the ion temperature at $t=1.42$ s (dotted line), and $t=1.69$ s (full line) in the oscillating RS discharge. (b) Ion thermal diffusivity χ_i versus ρ at $t=1.42$ s (dotted line), and $t=1.69$ s (full line) with the corresponding thin lines indicating neo-classical value.

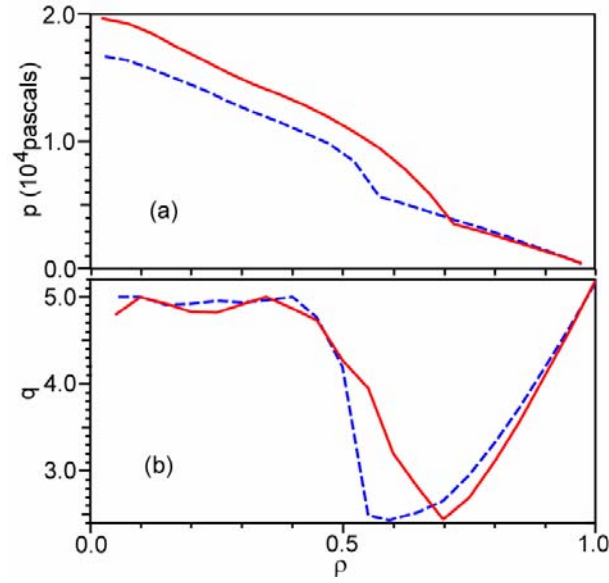


Figure 14. (a) Profiles of the plasma pressure, and (b) q -profiles at $t=1.42$ s (dotted line), and $t=1.69$ s (full line) in the oscillating RS discharge.

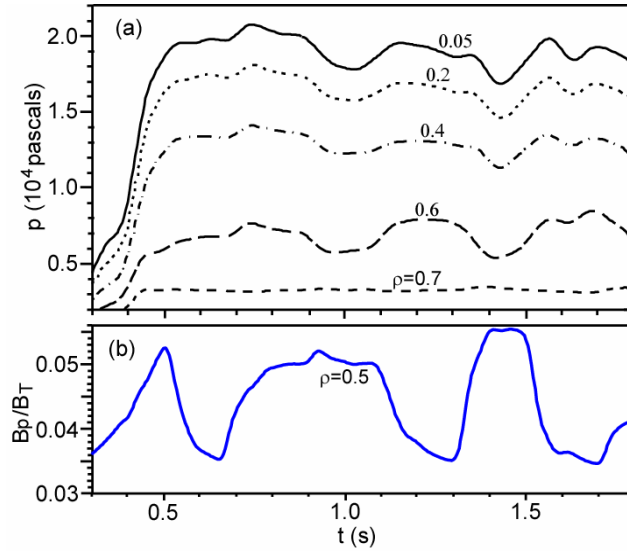


Figure 15. Time evolution of (a) plasma pressure at various ρ , and (b) pitch angle of the magnetic field line (B_p/B_T) at $\rho=0.5$.

Here, the oscillatory behavior in the LHCD controlled discharge on HL-2A is induced by nonlinear coupling of the LH power absorption position with the plasma configuration. According to the analysis by using the wave kinetic equation, in the weak damping regime absorption of the LH waves due to ELD is strongly peaked at caustic [42]. Therefore, the peak location of LH driven current is determined by the intersection between the inner boundary (caustic) of the propagation domain and the ELD limit. Fig. 16 shows the two LH

power deposition regions at $t=1.67s$ and $1.42s$ at the wave crest and trough of the oscillating driven current respectively. The mechanism of LH power deposition region coupling the q profile and T_e profile is the following: (i) In the tokamak plasma condition, $\tilde{q} \gg 1$, and $\tilde{\omega} \equiv \omega_{pe}/(\omega\sqrt{\epsilon_{\perp}}) \gg 1$. From Equation (14) the upper boundary of the LH wave propagation domain is reduced to $\bar{n}_{//u} = (n_{//0}R_0/R) \frac{\tilde{q}}{\tilde{q} - \tilde{\omega}}$. In the central plasma region ($\rho < 0.7$), $\tilde{\omega}$ is nearly a constant approximately equivalent to 18 because it is mainly dependent on square-root of the plasma density. Thus $\bar{n}_{//u}$ is a decreasing function of the magnetic geometry factor $\tilde{q}$; (ii) The ELD limit is a decreasing function of T_e , and actually it is nearly unchanged in the oscillation since it is inversely proportional to square root of T_e . Whenever the LH driven current moves inwards, $\tilde{q}$ decreases due to the safety factor decreasing, the caustic boundary is elevated. Consequently, the intersection between the caustic boundary and the ELD limit would move inward further (Fig. 16). If the LH driven current moves outwards, the picture will be the other way round. This mechanism can not lead to oscillation like in the predator-prey system in which the evolution of predator and prey populations living on the same territory is coupled each other, and notoriously admit periodic solution. The most plausible explanation for the oscillation is that under certain conditions, the LH wave deposition position changes spontaneously, as in the case of the two-phase RS discharge, at the wave crest and trough of the oscillation.

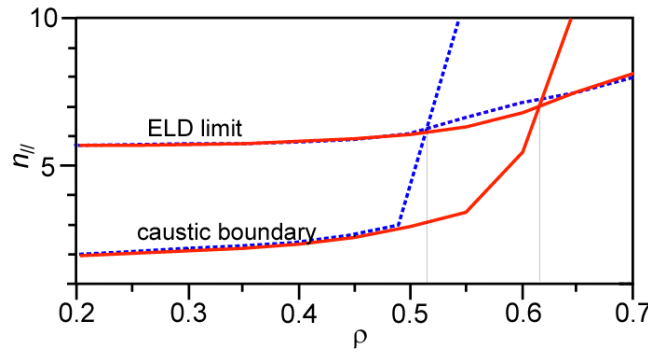


Figure 16. LH power absorption region in the phasing space $(\rho, n_{//})$ defined by ELD limit and inner boundary of the wave propagation domain at $t=1.42s$ (dotted line), and $t=1.67s$ (full line) in the oscillating RS discharge.

6. Conclusions

Lower hybrid wave is injected into a single-null divertor plasma in HL-2A. Dominant electron heating and current profile control are investigated. The magnetic flux surfaces are calculated by solving the Grad-Shafranov equation using the SND plasma boundary that is determined from the HL-2A discharge by a filament model. The LH wave power spectrum radiated by a multi-junction launcher is calculated with the Brambilla's coupling theory. Analysis of the LH wave absorption utilizes a toroidal ray-tracing for the wave propagation

and a parallel velocity Fokker-Planck calculation for the interaction of waves and particles. The LH absorption calculation combined with the Brambilla coupling calculation is in conjunction with the TRANSP code to obtain the plasma heating in a dynamic case.

Scenario of the low-density plasma heated by higher LH power is investigated. Plasma heating by electron Landau interaction results in operation scenarios of preferentially dominant electron heating. When LH wave ($P_{LH} = 1.5MW$) is injected to a low-density plasma with the relative wave-guide phase $\Delta\phi=170^\circ$, a non-inductive current ($I_{LH} \approx 80kA$) is driven in addition to the plasma heating. Due to the off-axis driven current, an optimized q -profile is formed, and enhanced confinement regime with steep electron temperature gradient is produced. The clear decrease of the electron thermal conductivity in the LH power deposition region shows that an eITB is developed. At the start time of the wave injection LH power can be deposited in the plasma center to form a central improved confinement regime.

The predictive simulation of current profile control with the available RF heating schemes in HL-2A demonstrates that the optimized q -profile with wide central weak shear region can be established by carefully adjusting the EC power deposition. In the EC-only scenario, a q -profile with $q_0 \approx 1.0$, $q_a = 3.78$ and central weak shear region extending to $\rho \sim 0.45$ is achieved. Corresponding to the optimized q -profile an electron temperature profile of large gradient region have been achieved, but it is unlikely that a distinct electron-ITB is developed because there is no abrupt decrease of electron thermal diffusivity associated with the steep gradient. The control scheme of ECH+LHCD is more effective, with which a hybrid configuration with the central weak shear region extended to $x=0.6$ can be produced. In this configuration the normalized gradient of the electron temperature, R/L_{Te} becomes larger than the critical gradient value for temperature profile stiffness in the confinement region of $\rho < 0.8$, characteristic of the suppression of the ETG and/or TEM driven turbulences.

In the HL-2A plasma the LH wave absorption is bounded in the region defined by the Landau-damping limit and boundary of wave propagation domain. This mechanism causes interplay of the distribution of the LH wave driven current with the modification of the plasma configuration, which constitutes non-linearity in the LH wave deposition. Due to the non-linear effects of the LH power deposition, the LH wave deposition position changes spontaneously, which causes oscillation in the plasma performance. Therefore, the feedback control of the current profile through controlling LHCD is a challenge in the steady state high performance tokamak operation.

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Chapter 8

IMPURITY RADIATION AND OPACITY EFFECTS IN FUSION PLASMAS

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Abstract

Recent years the optical opacity effects in impurity seeded plasmas in fusion devices are discussed. In many practically interesting situations plasmas are far from coronal as well as from local thermo-dynamical equilibria. Impurities in clouds, surrounding diagnostic pellets, in noble gas jets injecting into tokamak etc., are transparent for some lines and opaque for others. Hence, accurate simulations of thermal balance, impurity dynamics and so on, are extremely cumbersome. At the same time the influence of plasma optical opacity on plasma parameters may be very important. Reduced models for impurity description with taking into account opacity effects were developed. Coincidence of some theoretical and experimental results has been achieved taking into account opacity effects with the models mentioned above. The present paper is the review (may be, not complete) of results obtained by some theoretic teams in last few years. Carbon pellets, noble gas jets and disruptions are discussed.

1. Introduction

Despite of low concentration of impurities in fusion plasmas the impurity radiation losses often determine the plasma dynamics, especially for plasma regions with relatively low temperatures. The radiation in lines is most important for temperatures values of few hundred eV or less. Plasma radiation plays the determining role in temperature balance, equilibrium and stability of plasmas at the edge of fusion devices. For example, the Micro-faceted Asymmetric Radiation From the Edge (MARFE), the detached plasma regimes and the density limits are related to radiative losses (See review papers [1-3]). If the fusion plasmas are transparent for ionization radiation and bremsstrahlung, opacity effects may be significant for radiation in lines due to resonance character of photon absorption by impurity ions. Sometimes, the multi-electron ion densities even may exceed the hydrogen ion density, for example, in clouds surrounding the diagnostic pellet, at the current decay stage during

disruption in tokamaks, and in noble gas jets injected into tokamak plasmas in order to mitigate the disruptions.

In many cases, plasmas are far from coronal equilibrium (CE) as well as from Local Thermodynamical Equilibrium (LTDE). Moreover, plasmas may be opaque for some lines and transparent for others. The mean free path of photon l_0 is equal to the inverse absorption coefficient in a center of the resonance line given by the expression [4]

$$l_0^{-1} = \kappa_0 \approx \pi n_I \left(\frac{\lambda}{2\pi} \right)^2 \frac{\gamma}{\gamma + \Gamma}, \text{ cm}^{-1}. \quad (1.1)$$

Here and below the impurity density n_I is expressed in eV, E_{tr} is the transition energy, $\gamma = \frac{\Delta E_{nat}}{E_{tr}}$ is the relative natural line broadening, $\Gamma = \frac{\Delta E_{ext}}{E_{tr}}$ is the external one. The latest is supposed below to be Doppler's one. If l_0 for the selected radiation line is significantly higher than the typical plasma size, the plasma is transparent for the line. Other important parameter determining the radiation trapping in the plasma volume is the ratio of the excited state decay probability by the electron impact to the probability of the natural decay [4],

$$\beta = \frac{n_e \cdot 2.7 \cdot 10^{-13}}{(E_{tr})^3 \sqrt{T_e}} \left[1 - \frac{E_{tr}}{T_e} \exp\left(\frac{E_{tr}}{T_e}\right) E_1\left(\frac{E_{tr}}{T_e}\right) \right]. \quad (1.2)$$

Here and below the electron density is expressed in cm^{-3} , $E_1 = \int_x^\infty \frac{\exp(-t)dt}{t}$ is the integral exponent. Transition energies E_{tr} and electron temperature T_e are expressed in eV.

The parameters l_0 and β for four brightest lines of carbon ions are presented in Tables 1, 2 and 3. The electron temperature is supposed to be equal to 5 eV (Tables 1 and 2), and 40 eV (Table 3). The most representative carbon ion for the electron temperature $T_e = 5 \text{ eV}$ are the ions CIII (ion charge $z = 2$) and CIV ($z = 3$) under the condition of the CE. The first rows of the Tables show the transition energies. The second and the third rows represent the mean free path l_0 and the parameter β respectively. The electron and impurity densities are supposed to be equal to 10^{14} cm^{-3} and 10^{12} cm^{-3} respectively. The densities and temperatures are typical for regular regimes of fusion device edges. Impurity densities in clouds surrounding the diagnostic pellet, in noble gas jets injected into tokamak plasmas as well as averaged densities at the stage of the current decay are significantly higher [5-8].

Atomic data tables [9] are used here and below. One can see from Tables 1 and 2 that the mean free path of long wavelength photons may be smaller than the typical size of cold regions at the edge of fusion devices. Hence, the edge may be partially opaque for radiation in

lines even under regular operations. The effect increase significantly for higher impurity densities.

Table 1. $T_e = 5 \text{ eV}$. Ion CIII.

$E_{tr} \text{ (eV)}$	12.72	32.2	38.48	42.72
$l_0 \text{ (cm)}$	15.1	126.7	$7.7 \cdot 10^2$	$8.64 \cdot 10^2$
β	$1.4 \cdot 10^{-3}$	$4.39 \cdot 10^{-5}$	$2.23 \cdot 10^{-5}$	$1.49 \cdot 10^{-5}$

Table 2. $T_e = 5 \text{ eV}$. Ion CIV.

$E_{tr} \text{ (eV)}$	8.03	39.8	50.76	55.8
$l_0 \text{ (cm)}$	25.6	$1.79 \cdot 10^2$	$7.57 \cdot 10^2$	$1.88 \cdot 10^3$
β	$7.53 \cdot 10^{-3}$	$1.96 \cdot 10^{-5}$	$7.69 \cdot 10^{-6}$	$5.34 \cdot 10^{-6}$

Table 3. $T_e = 40 \text{ eV}$. Ion CV.

$E_{tr} \text{ (eV)}$	308.7	355.5	371.8	379.4
$l_0 \text{ (cm)}$	$3.53 \cdot 10^3$	$1.84 \cdot 10^4$	$5.02 \cdot 10^4$	$1.06 \cdot 10^5$
β	$6.44 \cdot 10^{-9}$	$3.68 \cdot 10^{-9}$	$3.08 \cdot 10^{-9}$	$2.84 \cdot 10^{-9}$

On the other hand, plasmas become completely transparent for carbon line radiation in lines with the electron temperature increase.

It seems that the de-excitation of excited states by electron impact may be neglected because $\beta \ll 1$. However, the resonant absorption of photons increases the “effective” natural decay time of excited states, and the de-excitation by electron impact may be significant even if $\beta \ll 1$.

Generally speaking in order to calculate radiation losses from partially opaque plasmas one has to use the complete radiation-collisional model. The full model is extremely complex. Biberman-Holstein photon transport equation for every line must be solved. Usually, the radiation spectrum of multi-electron impurity ion includes few tens of lines (sometimes, few hundreds). However, for many practically interesting situations the problem may be reduced significantly. First, if excited state populations are small, transitions between excited states may be ignored in calculations of radiation losses. Second, if ionization-recombination times are higher than excitation and de-excitation times, the problem may be divided into two parts: calculation of ion distribution over ionization states and calculation of radiation as a sum of radiations of ions with given charges. Third, one can take into account only the transitions with $\Delta z = \pm 1$ like the coronal model. Here z is the charge of the impurity ion.

The present paper is devoted to the reduced models and some important results obtained with the models.

The paper is organized as follows. The reduced models for impurity description are presented in Part 2. Calculations of plasma parameters in clouds surrounding the carbon diagnostic pellet performed with the reduced models are presented in Part 3. The processes in noble gas jet injected into tokamak plasmas are described in Part 4. The current decays after occasional disruptions and disruptions after jet injections are discussed in Part 5. Main results and unsolved problems are discussed in Discussion.

2. Reduced Models

a) Ionization and Recombination Rates

In this part the brief description of reduced models for the dynamics of impurity distributions over ionization states and for radiation is presented. The detailed description of the models one can find in [10, 11].

Usual model describing the impurity distribution over ionization states in transparent plasmas takes the form

$$\frac{\partial n_z}{\partial t} + \text{div}(n_z V_z) = n_e (I_{z-1} n_{z-1} + R_{z+1} n_{z+1} - (I_z + R_z) n_z) + n_h (R_{z+1}^{cx} n_{z+1} - R_z^{cx} n_z) \quad (2.1)$$

Here n_z and V_z are the concentration and velocity of ions with the charge z respectively, n_e is the electron density, n_h is the neutral hydrogen concentration, I_z , R_z , and R_z^{cx} are the ionization, recombination, and charge-exchange rates respectively. The ionization and recombination transitions with $\Delta z \geq 2$ are ignored. Only the charge-exchange of impurity ions with neutral hydrogen is taken into account. The set of equations (2.1) may be valid also for partially opaque plasmas if n_e and n_z do not exceed 10^{17} cm^{-3} .

If the impurity and electron densities are low, only the ionization from the ground state may be taken into account. It is described well with the approximate expression [12]:

$$I_{z \text{ ground}} = 10^{-5} \frac{\sqrt{T_e / E_z^{\text{ion}}}}{(E_z^{\text{ion}})^{3/2} (6.0 + T_e / E_z^{\text{ion}})} \exp(-E_z^{\text{ion}} / T_e), \text{ cm}^3/\text{s}. \quad (2.2)$$

Here E_z^{ion} is the ionization energy measured in eV here and below. For high impurity and electron concentrations the ionization from excited states must be taken into consideration [13]:

$$I_{z \text{ ex}} = \frac{3.27 \cdot 10^{-5}}{(T_e)^3} \exp\left(-\frac{E_z^{\text{ion}}}{T_e}\right), \text{ cm}^3/\text{s}, \quad I_z = I_{z \text{ ground}} + I_{z \text{ ex}}. \quad (2.3)$$

The value I_{zex} exceeds the ionization rate from ground state significantly if $T_e / E_z^{ion} \ll 1$. The expression (2.3) is obtained under the assumption that all excited state populations are described by Boltzmann's law. However, they are significantly lower for low plasma densities. Hence, (2.3) overestimates the real ionization rate significantly for low impurity concentrations. More realistic approximation can be derived with two state model where only one lowest excited state is taken into account. The rate of ionization from the excited state may be described with the following expression [11]:

$$I_{zex} = 2.66 \cdot 10^{-6} \cdot y_{ex} \cdot T_e^{-3/2} \cdot \frac{E_z^{ion} - E_z^1}{T_e} E_1 \left(\frac{E_z^{ion} - E_z^1}{T_e} \right). \quad (2.4)$$

Here y_{ex} and E_z^1 are the relative excited state population, and the excitation energy, respectively.

For low electron and impurity densities the population of the excited state is very small, and the ionization from the excited state may be ignored. With the impurity density increase, the resonant photon trapping appears, and the excited state "travels" from one ion to other one. It may disappear leaving the plasma volume or the resonance wavelength range. Hence, the effective living time of the excited state related to the radiation decay is increased by factor $\kappa_z^1 \rho$. Here ρ is the size of the layer occupied by ions with the charge z .

Other channel of excited state losses is the de-excitation by the electron impact. If one takes into account opacity effects the ratio β must be multiplied by factor $\kappa_z^1 \rho$, $\beta_{op} = \beta \kappa_z^1 \rho$. If $\beta_{op} \gg 1$ the population of the first excited level may be described by Boltzmann's expression. Hence, the population of the first excited state may be modeled with the expression

$$y_{ex} \approx \exp(-E_z^1 / T_e) \cdot \exp(-1 / \beta_{op}). \quad (2.5)$$

Here $y_{ground} \gg y_{ex}$ is supposed.

The value of β_{op} decreases strongly with the increase of the excitation energy and the populations of highest excited states may be ignored. Thus, the two level model is valid if the impurity and electron densities are not extremely high.

Three processes give their impacts into recombination. Three body recombination is important for low temperatures and high electron densities. The rate is determined by the expression [12]:

$$R_{3b}^z = 8.75 \cdot 10^{-27} z^3 n_e T_e^{-9/2}, \text{ cm}^3 / \text{s}. \quad (2.6)$$

Photo recombination rate may be calculated with the approximation expression [12]:

$$R_{photo}^z = 5.2 \cdot 10^{-14} \cdot z \cdot \sqrt{\frac{E_{z-1}^{ion}}{T_e}} \cdot \left(0.43 + 0.5 \cdot \ln \frac{E_{z-1}^{ion}}{T_e} + 0.469 \cdot \left(\frac{E_{z-1}^{ion}}{T_e} \right)^{-1/3} \right), cm^3 / s \quad (2.7)$$

In general, the dielectronic recombination rate R_{diel}^z is determined by the expression:

$$R_{diel}^z = B_z \sum_j A_{zj} D_{zj} \frac{\exp(-E_z^j / T_e)}{T_e^{3/2}}, cm^3 / s. \quad (2.8)$$

Here E_z^j are the energies of excited levels with numbers j . The values of B_z , A_{zj} , and D_{zj} are the cumbersome functions of the ion charge, E_z^j , electron density and temperature. The functions are defined in Ref. [14]. However, for many chemical elements the sum (2.8) may be reduced to two terms [10]:

$$R_{diel} \approx 10^{-9} \left(C_z^1 \exp\left(-\frac{E_{dr}^{(1)}}{T_e}\right) \left(\frac{n_e}{N}\right)^{-\beta_1} + C_z^2 \exp\left(-\frac{E_{dr}^{(2)}}{T_e}\right) \left(\frac{n_e}{N}\right)^{-\beta_2} \right) T_e^{-\alpha}, cm^3 / s. \quad (2.9)$$

Here $N = 10^{12} cm^{-3}$. Other parameters are presented in Ref. [10] for Be, C, O, Ne, and Ar. The impurity-hydrogen charge-exchange rate for $T_n \leq 30 eV$ may be approximated by the expression [10]:

$$R_{CX} = \langle \sigma_{CX} v \rangle \approx 3.5 \cdot 10^{-10} z (\sqrt{T_n} + 1.5), cm^3 / s. \quad (2.10)$$

Here T_n is the temperature of hydrogen neutrals.

b) Impurity Radiation

Three types of radiation are related to impurities, the radiation in lines, recombination radiation and bremsstrahlung. Only radiation in lines is discussed below because the absorption of bremsstrahlung and recombination radiation both are not resonant processes and plasmas are transparent for these types of radiation in magnetic confinement fusion devices.

The procedure of calculations of radiation losses in lines is well known for optically thin plasmas (see, for instance, [14]). Only dipole-dipole transitions between excited states and ground states are taken into consideration in the procedure. Accurate calculations of radiation losses from partially opaque plasmas are difficult and cumbersome. However, they may be estimated with a good accuracy for a slab uniform plasma layer using simple V.I. Kogan's expressions [15]. In many practically interesting cases the populations of excited levels of impurity ions are small in comparison with populations of ground states, and transitions

between excited states may be ignored. The radiation losses may be calculated in the same way as in models for transparent plasmas, but taking into account photon trapping.

$$Q_z = n_e \sum_j L_z^j B_z^j \quad (2.11)$$

Here j is the number of the excited state. The function L_z^j may be calculated in the same way as for transparent plasmas,

$$L_z^j = \frac{f_z^{0j}}{\sqrt{T_e}} A_z^j, \text{ erg} \cdot \text{cm}^{-3} / \text{s}. \quad (2.12)$$

Here f_z^{0j} is the oscillator strength for the transition from the excited state to the ground one. A_z^j depends on the principle quantum numbers of ground and excited states both, the ion charge, excitation energy, and the electron temperature [10, 14]. Usually, the ion spectrum includes many lines, and the full calculations are cumbersome. However, the spectrum may be reduced to few effective lines with effective oscillator strengths and excitation energies. The reduction of spectra for Be, and Ar has been performed in [10]. The coincidence of radiation intensity of each ion calculated with reduced spectra with full calculations inside 20 % has been achieved. For optically thin plasmas the simple approximations may be used [10].

$$L_z = \sum_j L_z^j \approx 10^{-18} T_e^{-\lambda} \left(C_1^{rad} \exp\left(-\frac{E_1^{rad}}{T_e}\right) + C_2^{rad} \exp\left(-\frac{E_2^{rad}}{T_e}\right) \right), \text{ erg} \cdot \text{cm}^3 / \text{s}. \quad (2.13)$$

The radiation damping related to the transition of the ion with the charge z from the excited state with the number j to the ground state is determined by the factor [15]:

$$B_z^j = \frac{T_a}{\beta_z^j + T_a}. \quad (2.14)$$

Here T_a is the probability for the photon to overcome the distance ρ without trapping. For $\kappa_z^j \rho \gg 1$ one can put for Doppler's line broadening, $T_a = \left(\kappa_z^j \rho \sqrt{\pi \ln(\kappa_z^j \rho)} \right)^{-1}$. In order to avoid the discontinuity for $\rho \rightarrow 0$ one can use the model:

$$T_a = \left(1 + \kappa_z^j \rho \sqrt{\pi \ln(\kappa_z^j \rho + 1)} \right)^{-1}. \quad (2.15)$$

The model was used for 0-D radiation estimations in tokamaks. The reasonable coincidence with experimental results has been achieved (see Parts 3 and 5).

The set of equations describing the impurity distribution over ionization states may be also reduced. Light impurities ($z < 10$) differ substantially in ionization and recombination rates of ions in the neighboring ionization states. Therefore, models describing dynamics of light impurity stripping in terms of two or three most abundant ions were proposed [16]. The discrete description of ionization states may be transform to the continuous description for heavy impurities [11, 17].

3. Carbon Clouds Surrounding Diagnostic Pellets

Diagnostic pellets are injected into thermonuclear plasmas [18-20] for many diagnostic applications, such as investigations of impurity transport [21, 22], alpha particles energy distributions [23], plasma current [24], magnetic field line tracing [25, 26], etc. Most of these diagnostics are based on optical measurements of cloud shape. Integral radiation emission is observed as well as radiation in lines [27]. However, measurements are indirect and require theoretical applications. In order to describe impurity cloud structure one need to model the cloud expansion with taking into account the neutral and ionized gas expansion, ionization state distribution, magnetic confinement, collisional energy transfer, radiation losses, electrostatic and magnetic shielding and pellet ablation rate calculation in a self-consistent manner. The most detailed model of ablation and cloud expansion has been realized in the numerical MHD pellet code LLP [28]. In this model, single-fluid MHD equations have been solved for the ablation cloud parameters. The calculations of impurity distributions over ionization states and radiation losses were performed in separated blocks and substituted into the energy balance equation:

$$\begin{aligned} \frac{3}{2} \frac{\partial p}{\partial t} + \frac{3}{2} (\nabla \cdot p \mathbf{v}) = & -(\nabla \cdot \mathbf{q}) - (\nabla \cdot \chi \nabla T) - (p + q_v)(\nabla \cdot \mathbf{v}) + \\ & + \left[\frac{3}{2} T_s + m(\mathbf{v}_s - \mathbf{v})^2 \right] S - Q_{ioniz} - Q_{rad\_losses} \end{aligned} \quad (3.1)$$

Here p is the gas dynamics pressure, $\mathbf{v}$ is the expansion velocity, $\mathbf{q}$ is the energy flux carried by incident electrons, T is the cloud temperature, χ is the heat conduction coefficient, q_v is the artificial viscosity used for numerical stability, S is the particle source representing a pellet, $\mathbf{v}_s, T_s$ are the velocity and temperature of the particles evaporating from the pellet correspondingly, Q_{ioniz} and Q_{rad_losses} are the energy losses on ionization and radiation respectively. Plasmas were assumed optically thin. Neither photon trapping in the plasma volume nor ionization from excited states was taken into account.

The scenario calculations by means of this code reproduced ablation rates and penetration depths of carbon pellet with reasonable accuracy. In contrast, the calculated radiation lengths were significantly larger than the measured ones [28-30] and there was a temperature plateau inside the cloud with a temperature less than 3 eV (see Figs. 1-2).

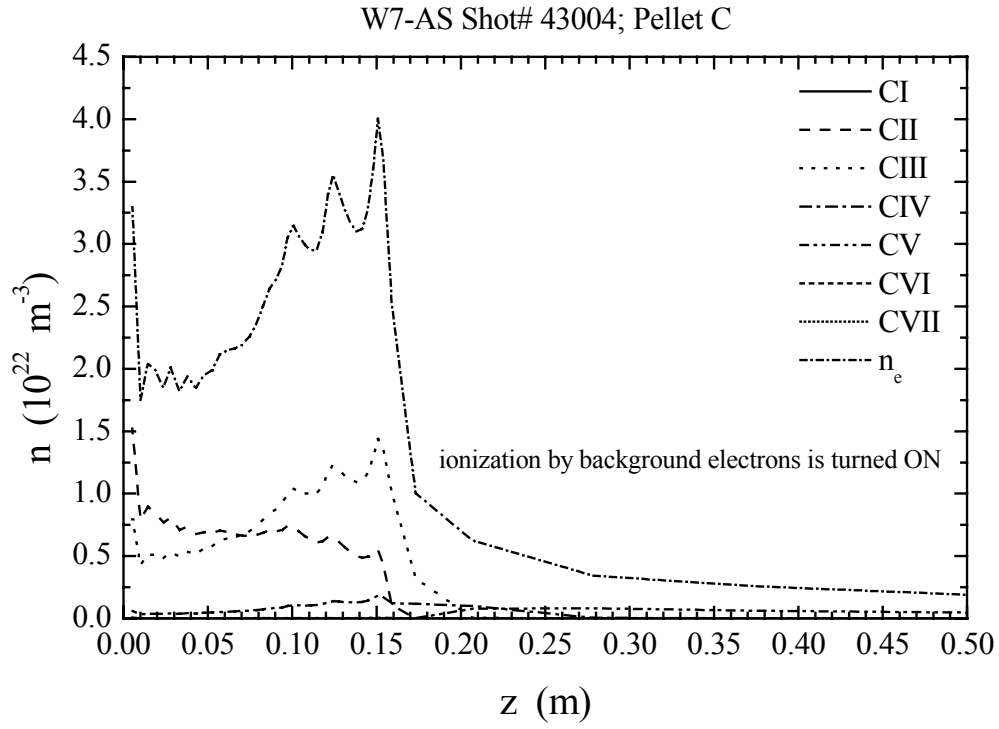


Figure 1. Ionization state distribution obtained by the original LLP code. Here z is the distance from the pellet along magnetic field.

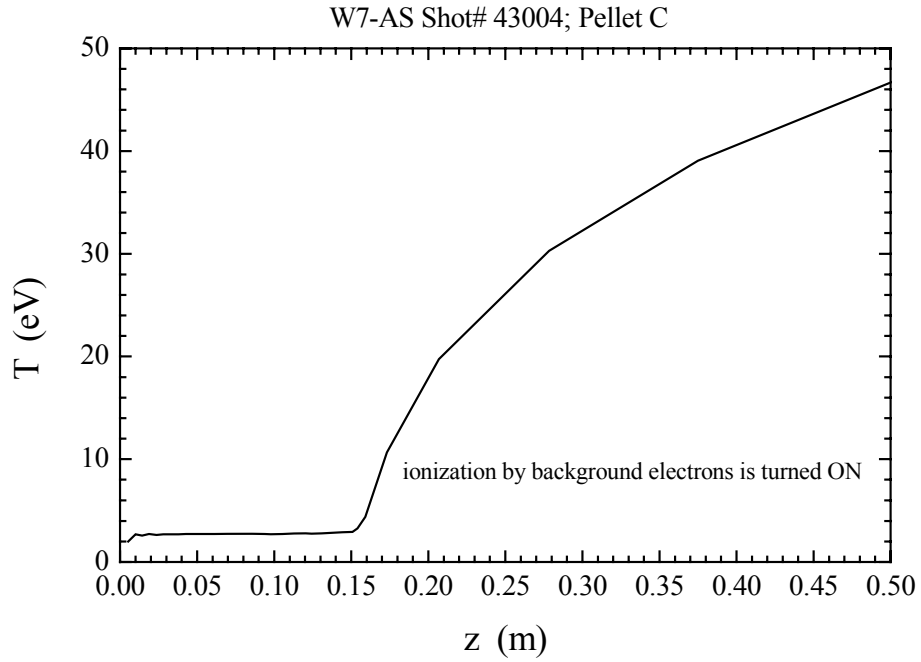


Figure 2. Longitudinal temperature profile in a carbon cloud obtained by the original LLP code.

It is necessary to remark that the wrong impression of plasma optical transparency may be produced by experimental results. The zones of CII and CIII ion domination are transparent for the radiation of ion CI, which is not resonant for the ions CII and CIII. The same situation is for the radiation of the ion CII. The radiation of this ion is not resonance for the ion CIII. Thus, in spite of the fact that each zone is optically thick for its own line radiation, the observer may watch the structure of the evaporated carbon cloud.

The modified ionization, recombination and radiation blocks taking into account the opacity effects such as ionization from excited state and resonant photon trapping are implemented into original LLP code in [5]. Carbon pellet injection into W7-AS stellarator plasmas has been analyzed. The results of the shot #43004 (pellet radius $r_p = 0.185\text{ mm}$, pellet velocity $V_p = 300\text{ m/s}$, central electron density $n_{e0} = 2.4 \cdot 10^{13}\text{ cm}^{-3}$, central electron temperature $T_{e0} = 4.5\text{ keV}$, [31]) are shown in Figs 3 and 4. The ion density and the temperature are shown as a function of the distance from the pellet along the magnetic field. The main qualitative difference between the temperature profiles obtained from original LLP code model and the new model is the absence of a temperature plateau. In the new model the temperature value on a distance of about 1 – 2 cm is high enough to burn ions CI, CII and CIII within this distance. Such a distance coincides with the experimental results, at least, qualitatively. At the same time the difference in the ablation rates is not significant. Hence, one is able to achieve a good coincidence with experiments using the fitting parameter of the order of unity.

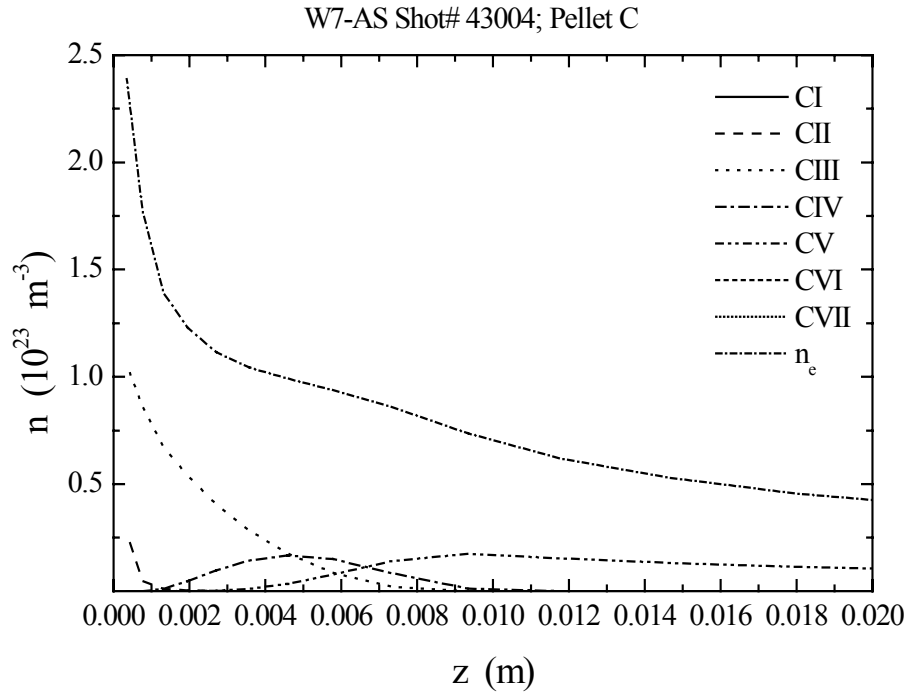


Figure 3. Ionization state distribution obtained by the LLP code with new ionization and radiation losses model included.

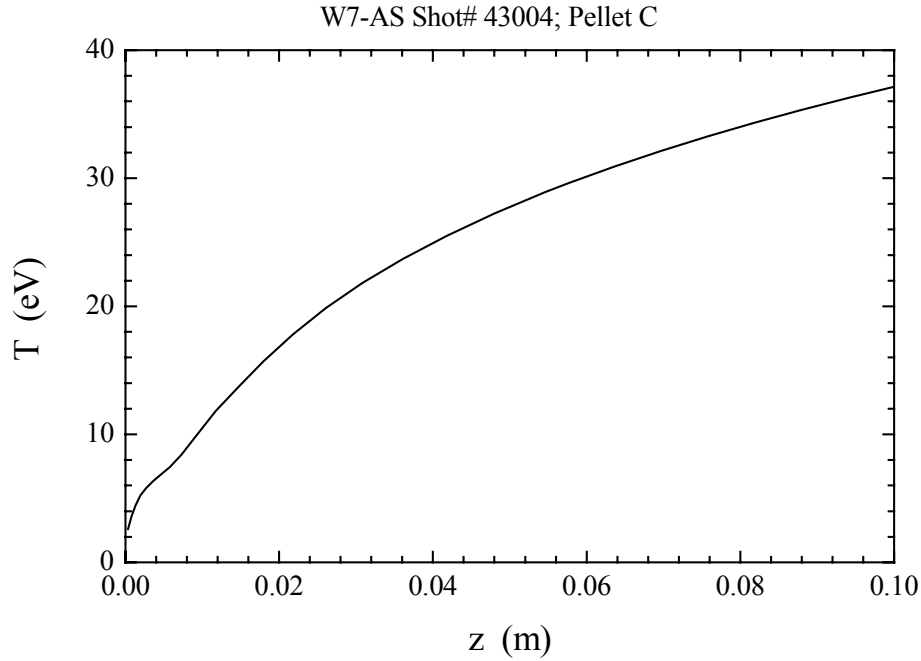


Figure 4. Longitudinal temperature profile obtained by the LLP code with new ionization and radiation losses model included.

4. Noble Gas Jets in Tokamak Plasmas

Also, opacity effects must be taken into account in simulations of noble gas jets injected into tokamak plasmas. One of the most important problems for ITER is the problem of disruptions. DIII-D, Tore Supra and JT-60 experiments have shown the mitigation of the deleterious effects of tokamak disruption by a high pressure noble gas injection [32-36].

The first experiments on jet fuelling were performed on Tore Supra [32,33]. The fuelling efficiency was reported to be intermediate between the standard gas puffing and pellet injection from the high field side (HFS) of the tokamak.

The first experiments on disruption mitigation were performed on DIII-D [4, 35]. Several gases were used: deuterium (D_2), helium (He), neon (Ne) and argon (Ar). It was demonstrated that in all cases the high-density jet ($n = 4 \cdot 10^{18} \text{ cm}^{-3}$) penetrates deep into the tokamak plasma (up to $r/a = (0.3 \div 0.5)$, where r is distance from the magnetic axis, and a is the distance up to the separatrix). Then, strong magnetic perturbations appeared. Impurities were brought deep into the core by turbulence.

In the second series of experiments [36] on DIII-D with lower jet density no significant penetration has been observed but the effect on the discharge was similar. The jet cooled plasmas. The cold front propagated inside and initiated strong MHD activity.

Therefore the high-pressure noble gas injection is considered to be a promising technique for disruption mitigation for ITER.

Jet penetration into plasmas before MHD activity was simulated in [6,7] with modified LLP code using the model of radiation losses described in Part 2. MHD equations used are presented below:

$$\frac{\partial n_k}{\partial t} + (\nabla \cdot n_k \mathbf{v}) = 0, \quad (4.1)$$

$$M n_k \frac{\partial \mathbf{v}}{\partial t} + M n_k (\mathbf{v} \cdot \nabla) \mathbf{v} = -\nabla p + [\mathbf{j} \times \mathbf{B}], \quad (4.2)$$

$$\frac{3}{2} \frac{\partial p}{\partial t} + \frac{3}{2} (\nabla \cdot p \mathbf{v}) = -(\nabla_{\parallel} \cdot \mathbf{q}_{inc}) + (\nabla \cdot \chi \nabla T) - p(\nabla \cdot \mathbf{v}) - Q_{ioniz} - Q_{rad\_losses} + \frac{j^2}{\sigma}, \quad (4.3)$$

$$\mathbf{q} = -\chi \nabla T + \mathbf{q}_{inc} \quad (4.4)$$

Here M is the atomic mass of jet atoms, $n_h = \sum_{i=0}^Z n_i$, $\mathbf{v}$ is the expansion velocity,

$p = (n_e + n_h)T$ is the total pressure, $n_e = \sum_{i=0}^Z i \cdot n_i$ is the electrons density, $\mathbf{q}_{inc}$ is the energy flux carried by the incident electrons and ions from the ambient plasma, χ is the heat conduction coefficient, Q_{ioniz} and Q_{rad_losses} are the ionization and radiation loss power per

volume respectively, j is the current density and $\sigma = \frac{n_e e^2}{m_e (\nu_{ea} + \nu_{ei})}$ is the local electric conductivity calculated with account of electron-neutral and electron-ion collisions. Equation $j_{\parallel} = 0$ was added to calculate the self-consistent electric field. MHD parallel expansion, deceleration of the ambient electrons and ions by the jet, self-consistent electric field, elementary processes and radiation are taken into account. Simulations were performed for different gases (D_2 , Ar), different values of jet density and background plasma density and temperature.

Temporal evolutions of parallel and perpendicular jet sizes [6] are shown in Figs. 5 and 6 for the Argon jet and the parameters of DIII-D experiments [34 - 36]. The values of $R/a = 1.7 m / 0.6 m$, plasma current $I_p \approx 1.5 MA$, toroidal field $B_T = 2.1 T$, electron density $n_e \approx 3 \cdot 10^{19} m^{-3}$, electron temperature $\langle T_e \rangle \approx 1.5 keV$. The valve released $4 \cdot 10^{24}$ particles in $\approx 2 - 5 ms$ into the port. The effective impurity atom density in the $18 m^3$ plasma volume is equal to $2 \cdot 10^{21} m^{-3}$.

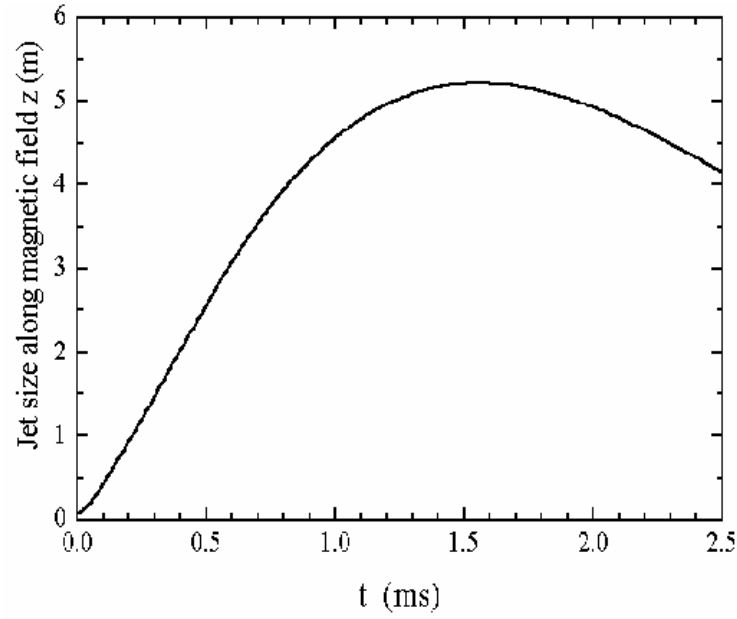


Figure 5 Temporal evolution of the longitudinal jet size.

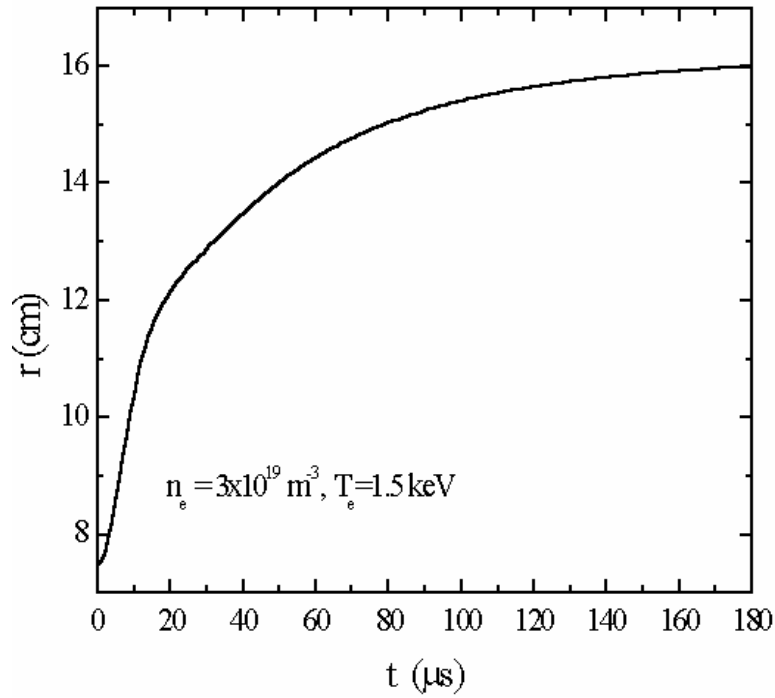


Figure 6. Temporal evolution of the transverse jet size.

The longitudinal size rises rapidly and achieves the value around 5 m. Then, it slightly decreases after the time moment $t \approx 1.5 \text{ ms}$. This behavior is connected with the finite inertia of the jet material. In contrast to the longitudinal size, the transverse size rises slowly

and does not exceed the value 16 cm. The ionization at the edge of the jet is sufficient to stop poloidal expansion of the jet by $\vec{j} \times \vec{B}$ force

The jet remains almost neutral and hence is able to move inside plasmas with the initial velocity (approximately).

Ion density and temperature distributions over the distance along the magnetic field calculated for $t = 1.5$ ms are shown in Fig. 7 and 8. One can see that the profiles calculated with the coronal model (opacity effects are not taken into account, Fig. 7a) and with opacity effects included (Fig. 7b) are different. The temperature as well as the ionization level calculated for the case (a) is significantly lower than if one takes into account the opacity effects.

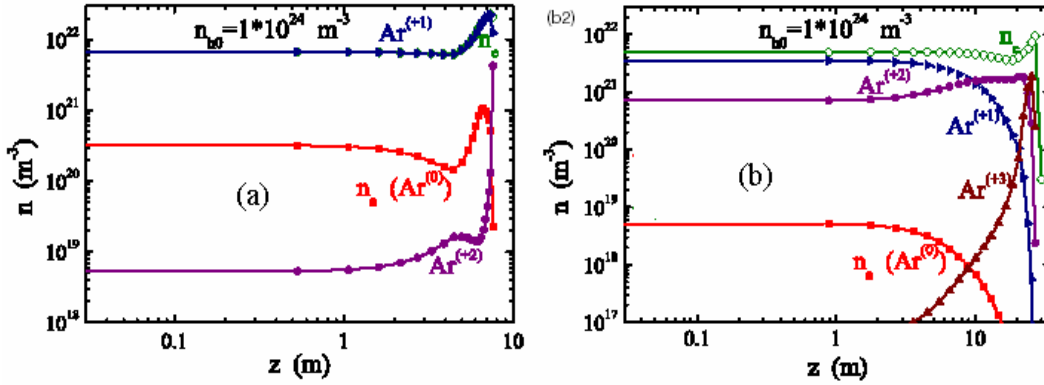


Figure 7. Density profiles along the magnetic field. (a) coronal dynamics model, (b) model for the optically thick jet. Initial density $n_{h0} = 1 \cdot 10^{24} \text{ m}^{-3}$.

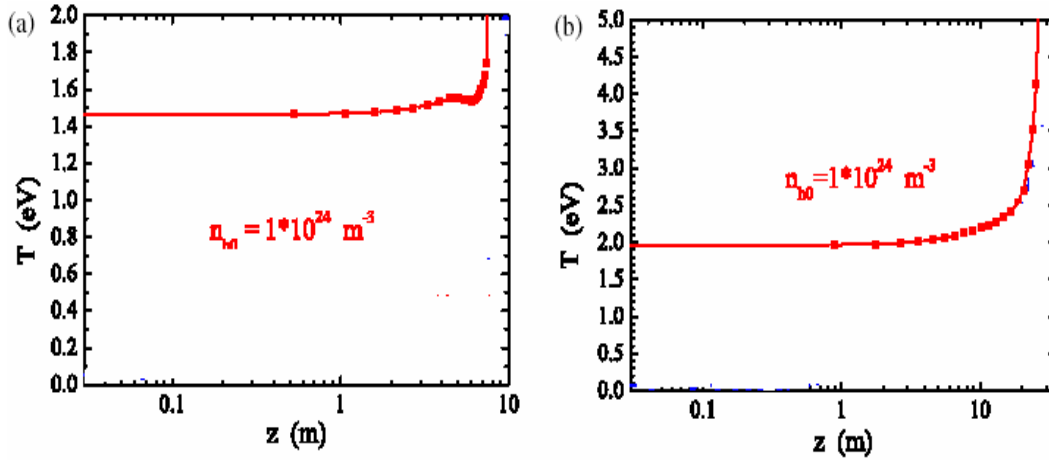


Figure 8. Temperature profiles along the magnetic field. (a) coronal dynamics model, (b) model for the optically thick jet.

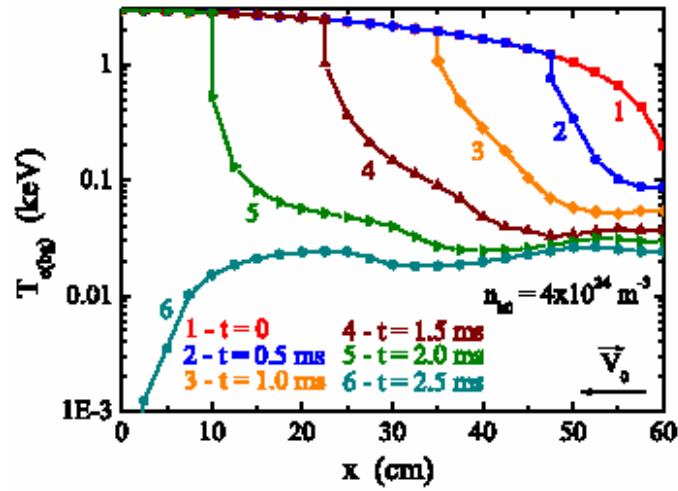


Figure 9. Temporal evolution of the ambient plasma temperature. Here x is the distance from the plasma center.

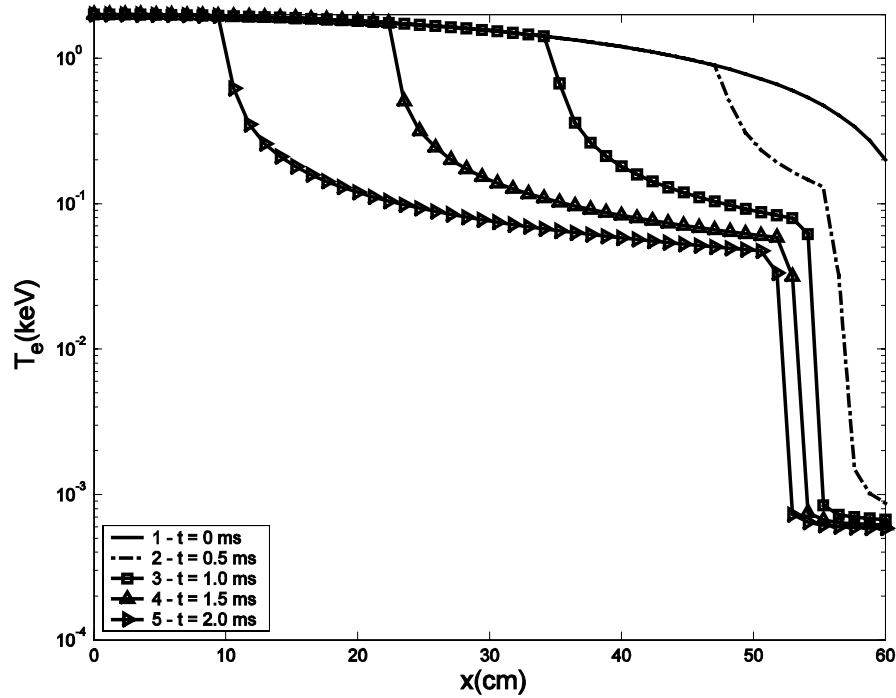


Figure 10. Thermal front propagation from the periphery. The first front is produced by the jet. The second one is produced by ambient carbon radiation stimulated by charge-exchange.

Opacity effects decrease radiation losses. The jet propagating to the plasma center cools the ambient plasmas (Fig. 9. MHD activity is not taken into account) however the ambient plasma temperature is significantly higher than the temperature calculated from the coronal model used in [37]. Experimental observations show [36] the strong MHD activity to appear after the cold front to achieve the magnetic surface $q = 2$. However in order to initiate the

MHD instability the periphery temperature must be approximately equal to 5-10 eV. The jet is not able to provide the temperature decrease sufficient for the instability initiation (see Fig. 9). On the other hand, the results of modeling [37] represent the experimental events at least, qualitatively, despite the wrong radiation model. The contradiction may be eliminated if one takes into account the ambient plasma impurity radiation (carbon radiation, for example) radiation [38]. The jet “prepares” the ambient plasma for deep penetration of neutral hydrogen from the wall. The carbon charge-exchange with hydrogen neutrals shifts the ionization equilibrium and increase the carbon radiation drastically producing the second cool front (see Fig. 10). When the second front achieves the surface $q = 2$ the strong MHD instability examined in [36] develops.

5. Current Decay after Disruptions in Tokamaks

The MHD instability causes the thermal quench. The instability mixes the central plasma region with the edge and brings the noble gas ions into the plasma center in a short time. Strong radiation is typical for this stage. The similar situation takes a place during current decays in occasional disruptions when plasmas are saturated by wall material ions. The halo currents and runaway electrons generate at this stage. It has been investigated [34] with 0-D code KPRAD under the assumption that plasmas have been transparent for radiation in lines. The 0-D radiation model is wide-spread for such type of simulations. It has been explored later in [39] and [40].

However, as it shown in [41,42] at the stage of the current decay plasmas are partially opaque for radiation in lines. Simulations of current decay in JET have been performed with DIMRUN code [43] for beryllium and carbon impurities.

In DIMRUN code system of equations includes balance of thermal energy and magnetic energy:

$$\frac{3}{2} \frac{d(n_e T_e)}{dt} V_P = P_{Oh} - Q_{rad} - P_{\Delta} - \frac{3}{2} \frac{n_e T_e}{\tau_{Ee}}; \quad (5.1)$$

$$\frac{3}{2} \frac{d(n_i + \sum_z n_z) T_i)}{dt} V_P = P_{\Delta}; \quad (5.2)$$

$$\frac{d}{dt} (L_P \cdot \frac{I_P^2}{2}) = - P_{Oh}. \quad (5.3)$$

Here $P_{OH} = (I_P - I_R) U_{res}$ is the Ohmic heating power, P_{Δ} is the energy exchange term between electrons and ions, V_P is the plasma volume; $U_{res} = R_{PL} (I_P - I_R)$ is the active plasma loop voltage, $R_{PL} = \eta_e 2\pi R_o / (\pi k a^2)$ is the plasma resistance, $E_{||} = U_{res} / 2\pi R_o$ is the vortex electric field on the plasma loop, η_e is the plasma resistivity, I_R is the runaway current, τ_{Ee} is the energy losses due to disturbance of magnetic field structure during impurity injection. Total

plasma resistivity is a sum $\eta_e = \frac{m_{e0}}{n_e e^2} \cdot \sum_s \langle \sigma_s v \rangle \cdot n_s = \eta_{ei} + \eta_{ea} + \eta_{en}$. Here η_{ei} corresponds

to elastic electron-ion collisions, η_{ea} is related to electron-atom collisions and η_{en} is related to non-elastic electron collisions (which lead to ionization, excitation, recombination etc.).

0-D model dynamics of impurity ionization states described above is used. The charge-exchange is also taken into consideration. The runaway electron generation is supposed to be described by Rosenbluth – Putvinsky model [44] where the both direct and avalanche generations are taken into account.

The additional channel of energy and runaway losses connected with perturbation of magnetic field is taken into consideration [45, 46]. Estimation of energy confinement time under magnetic disturbances (during thermal quench) can be made by following way: $\tau_{Ee} \sim$

$$k a^2 / (5.8 \chi_{\text{eff}}), \text{ where } \chi_{\text{eff}} \approx \min(\lambda, \pi q R) v \left(\frac{\delta B}{B_t} \right)^2$$

is the effective electron thermal conductivity, $\lambda = v \tau$ is the free path, τ is the electron collision time, and v is the electron velocity.

Calculated current decay times in JET as a function of beryllium and carbon concentration are shown in Figs. 11 and 12 respectively. The initial (before disruption) electron density is chosen to be equal to $n_e \approx 10^{19} \text{ m}^{-3}$ in accordance with experimental data [47]. The exact Be concentration in experiments is unknown. However, the upper limit of beryllium density after thermal quench may be estimated with experimental data [47] as follow.

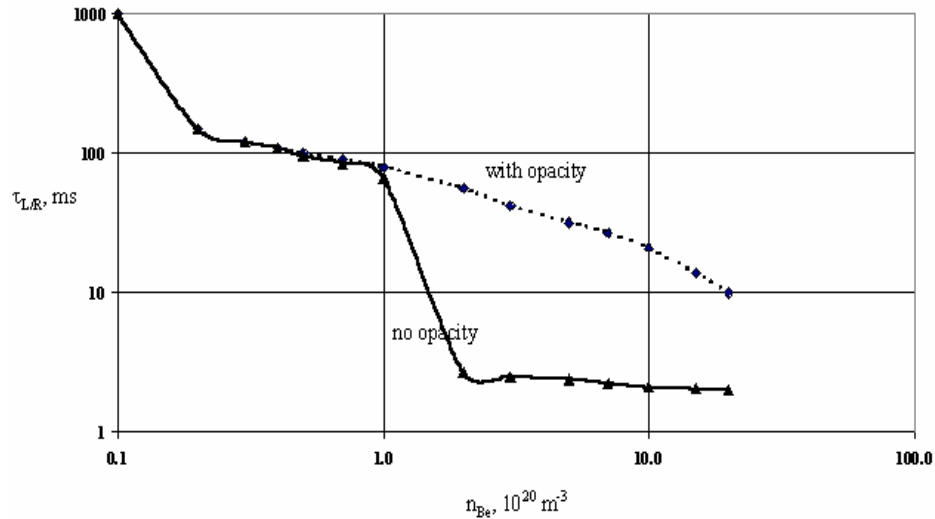


Figure 11. Calculated current decay time in JET as a function of beryllium concentration.

Typical value of plasma temperature in JET for beryllium-bounded discharge before disruption is $T_e \sim 2 - 2.5 \text{ keV}$. The total thermal plasma energy is estimated as $3n_e T V_P \sim 1 \text{ MJ}$. If one takes that 30 % of the total thermal energy falls down to Be cover and specific evaporation heat of Be is equal to 30 kJ/g [48] the amount of 300 kJ of thermal plasma energy is enough to evaporate the total mass of approximately 10 grams (i.e. $\sim 5 \text{ cm}^3$) of Be. If one takes that 50 % of evaporated Be enters the plasma, then total amount of Be in plasma will be $\sim 3 \cdot 10^{23}$ ions. It corresponds to Be density $n_{Be} \sim (2-3) \cdot 10^{21} \text{ m}^{-3}$ for JET plasma volume.

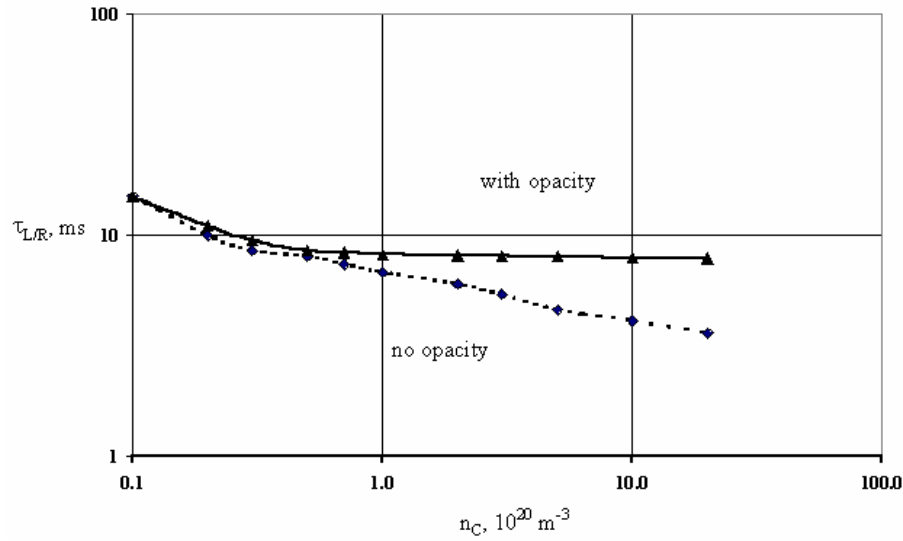


Figure 12. The same as in Fig. 11, but for carbon.

The absolutely minimal current decay time observed $\tau_{cq,min}$ may be estimated as 10 ms (see Ref. [47], Table II: $\tau_{cq} = 9.6$ ms for shot #20984(limiter)), $\tau_{cq} = 10$ ms for #20986, $\tau_{cq} = 11$ ms for #20774). The value of the current decay time $\tau_{cq} = I_p / \dot{I}_p$ averaged over the whole database [47] is equal to 175 ms.

The initial current value and electron density are assumed to be equal 3 MA and $10^{19} m^{-3}$ respectively. One can see from Fig. 11 that independently on opacity, simulation results coincide with the averaged over database experimental current decay time at least qualitatively only for small Be densities $n_{Be} < 1 \cdot 10^{20} m^{-3}$. However, the calculated shortest decay time coincides with the experimental one only if opacity effects are taken into account.

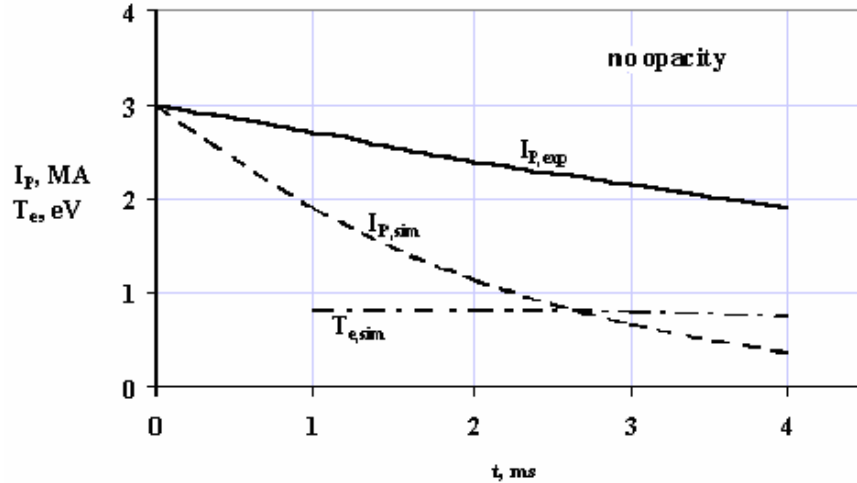


Figure 13.a. Time evolution of plasma current and electron temperature in JET (Experiment and simulation under the assumption of plasma optical transparency), $\delta B / B = 10^{-2}$.

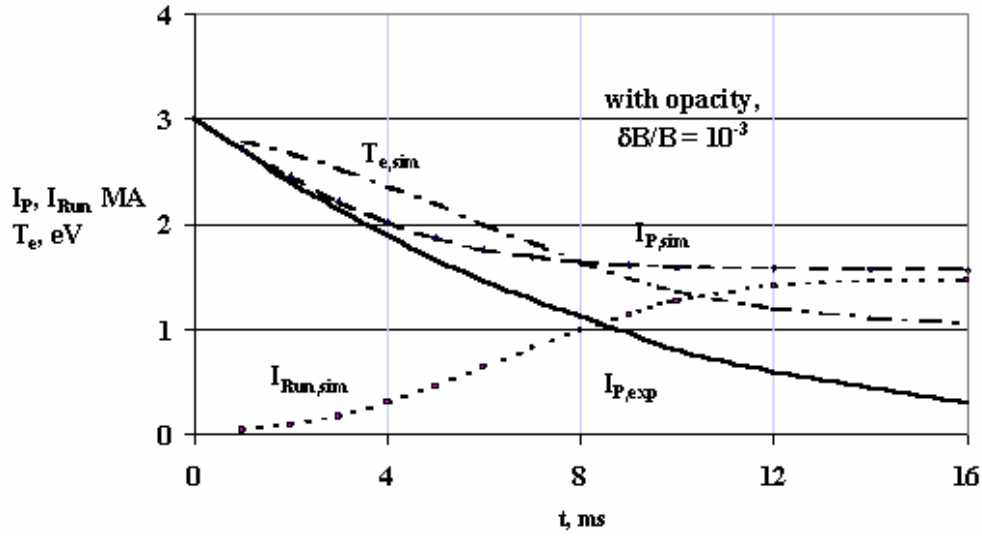


Figure 13.b. Time evolution of plasma current, runaway current and electron temperature in JET (Experiment and simulation taking into account opacity effects, $\delta B/B = 10^{-3}$.)

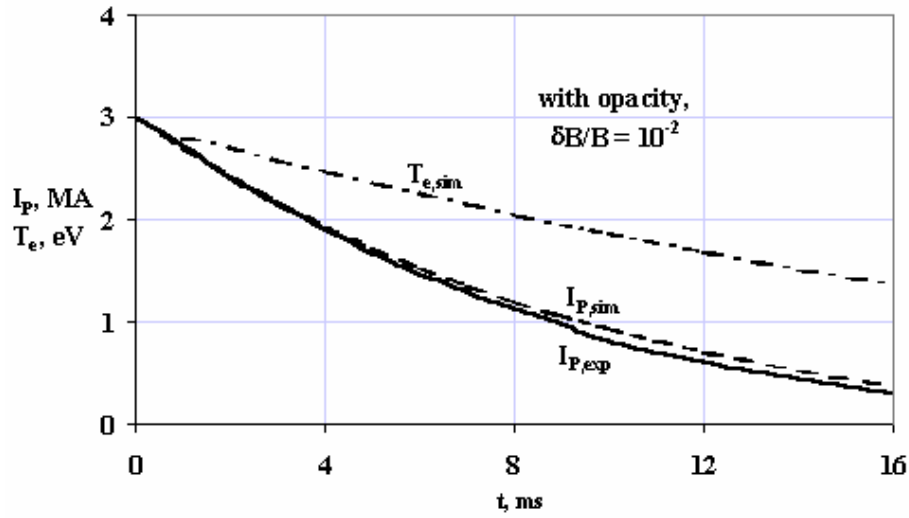
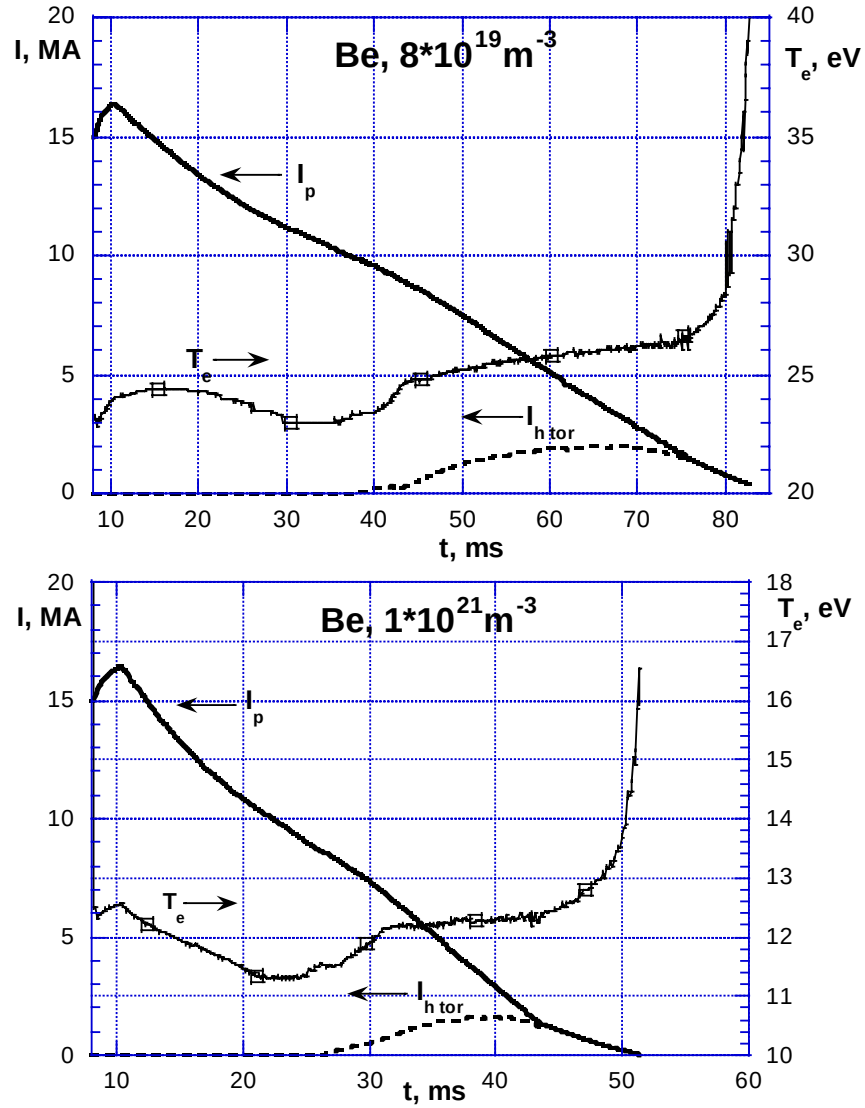


Figure 13.c. The same as in Fig. 13 b, but for $\delta B/B = 10^{-2}$.

The same result, may be, not so strong, is obtained for carbon. The current decay times for low carbon densities correspond to the experimental average decay time 14.5 ms while the shortest experimental times are inside the time interval $4.4 \div 8$ ms [47]. One can see a reasonable coincidence of simulation and experimental results.

The time evolution of the electron temperature, Ohmic I_p and runaway I_{Run} currents for two values of the magnetic field perturbations are shown in Figs. 13, a, b, and c. The experimental evolution of the plasma current for the JET is also presented for the shot #20984. Here $n_{Be} = 10^{21} \text{ m}^{-3}$. The runaway electron current and, hence, the total current depends on

the magnetic perturbation level. The observed level of magnetic perturbations $\delta B/B$ in JET and DIII-D is inside the range $10^{-3} \div 10^{-2}$ at the stage of the current quench [34, 49]. Calculations show that the coincidence of experimental results and simulations is impossible if opacity effects are ignored. The coincidence is achieved under the assumption $\delta B/B \approx 10^{-2}$, and opacity effects are taken into account. (See Fig. 3.) Under these conditions the runaway electron current is suppressed. Experimental runaway current is rare [47].



Figures. 14 a and 14 b. Plasma current I_p , electron temperature T_e , and toroidal halo current $I_{h \text{ tor}}$ evolution for Be-seeded plasmas in ITER for two Be densities, $n_{\text{Be}} = 8 \cdot 10^{19} \text{ m}^{-3}$, and $n_{\text{Be}} = 1 \cdot 10^{21} \text{ m}^{-3}$.

The current decay after disruption in ITER has been simulated for beryllium, carbon, neon and argon seeded plasmas by DINA code [50]. Generation of halo currents has been taken into account in DINA simulations of ITER scenario with use of model described in [51]. The initial current and electron density have been chosen to be equal to 15 MA and $n_e = 8 \cdot 10^{19} \text{ m}^{-3}$ respectively corresponded to the inductive Scenario 2 in ITER [52]. The evolutions of the plasma current I_p , temperature T_e and halo current $I_{h\text{tor}}$ are shown in Figs. 14 a and b for Be densities $8 \cdot 10^{19} \text{ m}^{-3}$ and 10^{21} m^{-3} respectively. All plasma parameters calculated for lowest Be densities without and with opacity effects practically coincide like in JET simulations (Fig. 14 a). The plasma current decays practically in a linear way. The current decay time calculated is very long.

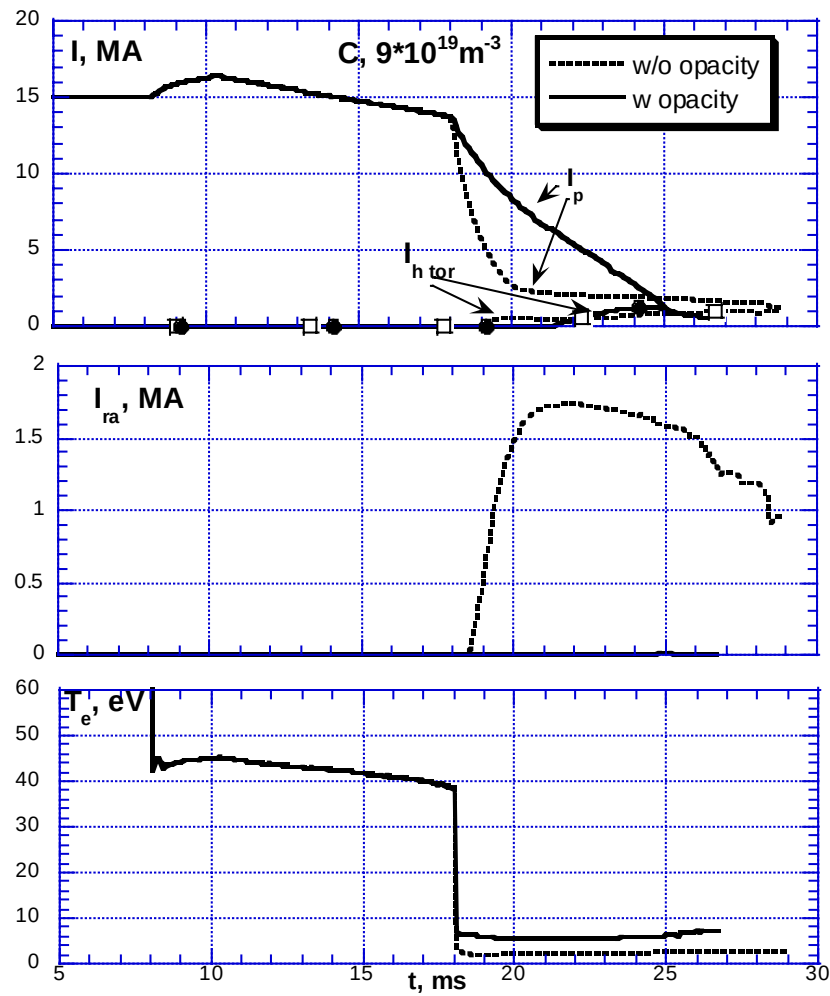


Figure 15. Plasma current I_p , electron temperature T_e , toroidal halo current $I_{h\text{tor}}$ and runaway electron current I_{run} evolutions for C-seeded plasmas in ITER for $n_C = 9 \cdot 10^{19} \text{ m}^{-3}$, with (w) and without (w/o) opacity effects.

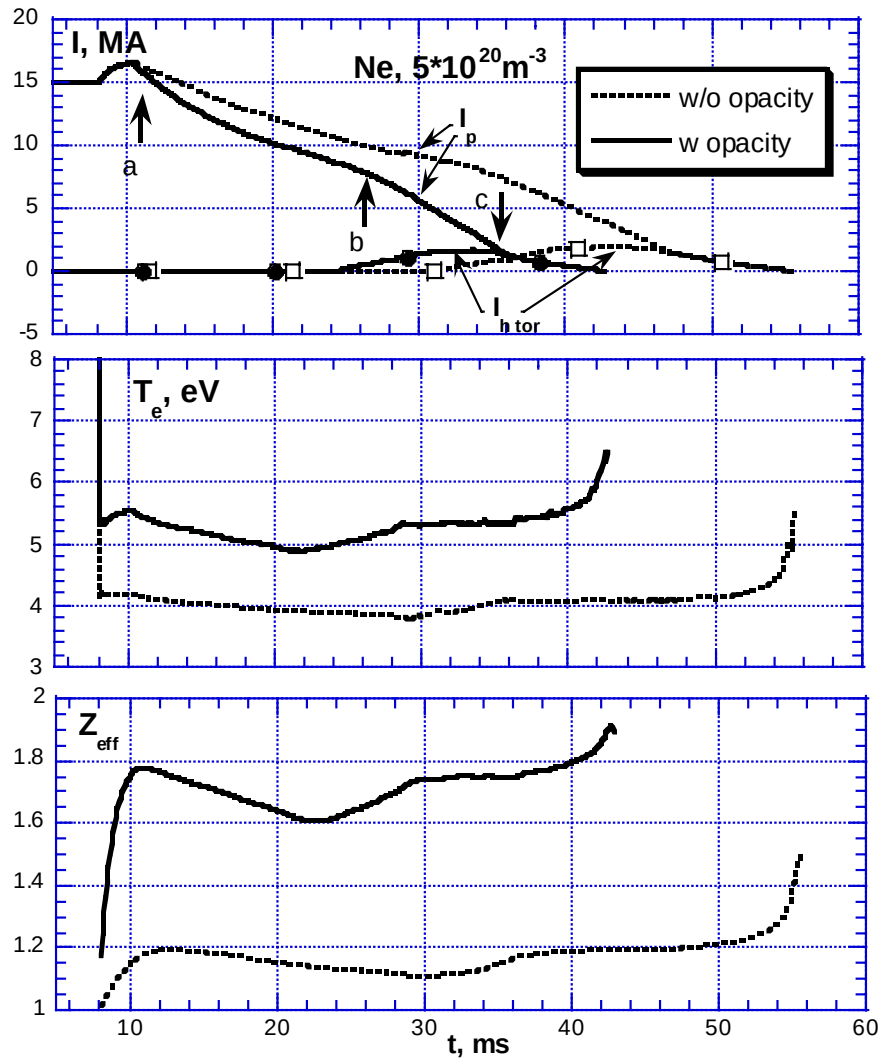


Figure 16. Plasma current I_p , electron temperature T_e , toroidal halo current $I_{h\,tor}$, runaway electron current I_{run} , and Z_{eff} evolutions for Ne-seeded plasmas in ITER for $n_{Ne} = 5 \cdot 10^{20} \text{ m}^{-3}$, with (w) and without (w/o) opacity effects.

If one doesn't take into account the opacity effects for the second (high) Be density the temperature calculated is extremely low, less than one eV. The model used is not valid because plasmas are very complex mixture of ions, neutrals and molecules at this temperature region. However, it is clear that the current decay time is extremely short. The situation is significantly different if opacity effects are taken into account (see Fig. 14 b). The temperature is higher than 10 eV. The current decay time is long. It is only slightly less than the time for the low Be density. The calculated runaway electron currents are negligible for both densities as a consequence of high electron temperatures and densities.

Evolutions of the total plasma current, toroidal halo current and the electron temperature in carbon seeded ITER plasmas are shown in Fig. 15. Two typical decay times appear. One

can see that the time derivative of the current is significantly higher if opacity effects are ignored. Also, the temperature is underestimated, and the runaway current calculated is high. If opacity effects are taken into account the runaway electron current is negligible.

The current decay stage has been examined for neon seeded plasmas. The plasma current, the electron temperature, toroidal halo current and Z_{eff} evolutions are shown in Fig. 16. One can see that the current decay time exceeds the minimal time (22 ms) recommended for ITER [39]. Runaway electron currents are negligible for opaque as well as for transparent plasmas. The current decay time calculated for transparent plasma is slightly higher in comparison with opaque one due to highest value of Z_{eff} in opaque plasmas.

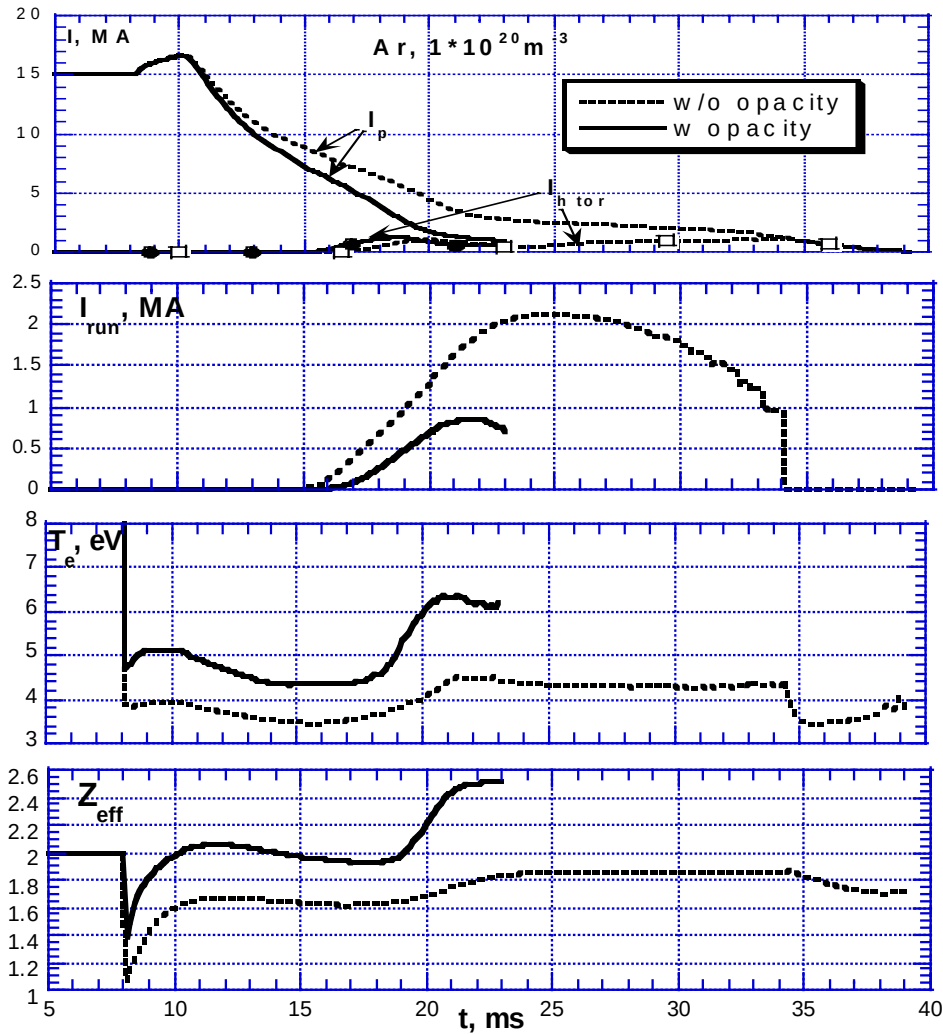


Figure 17. Plasma current I_p , electron temperature T_e , toroidal halo current $I_{h_{tor}}$, runaway electron current I_{run} and Z_{eff} evolutions for Ar-seeded plasmas in ITER for $n_{Ar} = 1 \cdot 10^{20} m^{-3}$, with (w) and without (w/o) opacity effects.

The results for argon seeded ITER plasmas are shown in Fig. 17. One can see the difference in temperatures expected for the current decay stage to be not so significant as for Be-seeded and C-seeded plasmas. The plasma temperature is underestimated for transparent plasma approximation by factor 2. Runaway current is overestimated by factor 2.5. The current decay time is overestimated. It is short. The simulations confirm the conclusion of Ref. [39] that argon is not a good noble gas for disruption mitigation in ITER.

6. Discussion

New methods of fusion plasma investigations like diagnostic pellet injection and disruption mitigation, deal with the high density low temperature multi-electron ions. Under such conditions, plasma is partially opaque for multi-electron ion radiation in lines. On the other hand, even regular plasmas in large fusion devices may be sometimes also partially opaque (see Tables 1-3 of Part 1). As it shown above, the opacity effects sometimes change main plasma parameters drastically. In particular, the qualitative coincidence of theoretical and experimental results could not be achieved without taking into account opacity effects in simulations of diagnostic pellets and current decay after disruption. Hence, such effects must be taken into account in simulations of impurity seeded plasmas. Unfortunately, the direct inclusion of radiation-collisional model is very difficult in many cases. Recent years the reduced models were developed. These models are simple enough to be included into cumbersome MHD codes. Some qualitatively new results have been obtained with the models. On the other hand, reduced models are useful if 0-dimensional approximation is valid. Hence, one can expect the development of advanced reduced models.

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Chapter 9

RECENT DEVELOPMENTS IN SAFETY AND ENVIRONMENTAL ASPECTS OF FUSION EXPERIMENTS AND POWER PLANTS

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Abstract

Electricity generating plants powered by nuclear fusion have long been envisioned as possessing inherent advantages for the health and safety of the public, the health and safety of plant workers, and good stewardship of the environment while supporting modern society. This chapter discusses the progress and state-of-the-art of these three principal aspects of fusion safety and environment. The fusion safety philosophy and advantages over traditional thermal power plants are described. Fusion workers should be protected commensurately with workers in other comparable industrial activities. The fusion radwaste management strategy must accommodate the new trend of recycling and clearance, avoiding geological disposal. Here, we discuss the technical elements as well as the US regulatory approach and policy governing the design of safe and environmentally sound fusion devices.

1. Introduction

Safety and environmental concerns for power generation include the health and safety of the public, the health and safety of plant workers, and the short- and long-term environmental impacts. Electricity generating plants powered by nuclear fusion have long been envisioned as possessing inherent advantages for enhanced safety and benign environmental impacts over the presently used fuels. Some of the advantages are the ample and widely distributed source of fuel, inherent plant safety, and avoidance of actinide elements such as uranium, plutonium, and thorium.

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At present, there are three primary fuels used in thermal power plants for electric power generation. As shown in Fig. 1, the most widely used fuel is coal, followed by natural gas, and then closely followed by fission of uranium (DOE 2007). In this chapter, the safety characteristics of fusion are highlighted and in some cases compared to the three leading fuels for electric power production: coal, natural gas, and fission.

Many of the nations pursuing fusion research have independently studied the safety and environmental aspects of fusion power. These general studies have been extensive and detailed (Holdren 1989, Sowerby 1990, Brunelli 1990, Raeder 1995, Inabe 1998, Cook 2000). All of these studies, along with numerous design-specific assessments, confirmed that fusion has inherent safety advantages in shutting down the fusion reaction, and in removing the decay heat.

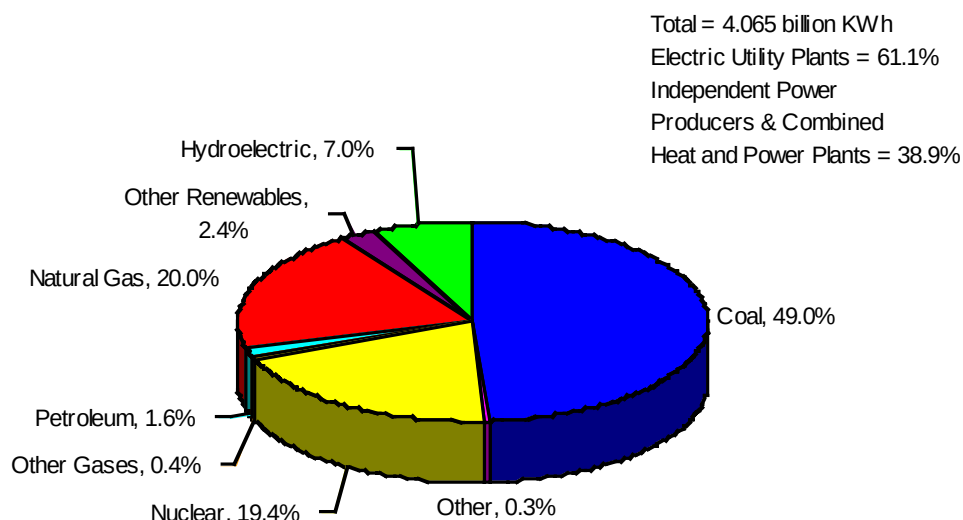


Figure 1. U.S. electric power industry net generation by fuel type, 2006 (DOE 2007).

Fusion researchers have the advantage of selecting materials based on low neutron-induced activation, which reduces the amount of radioactivity generated over decades of plant operation. However, fusion designs tend to generate a sizable amount of mildly radioactive materials that rapidly fill the low-level waste geological repositories. More environmentally attractive means to keep the volume of fusion radwastes to a minimum are being pursued via clever designs and recycling/clearance so that fusion radwaste would not constitute a permanent burden for future generations.

A unique advantage of fusion power is that fusion reactions offer the possibility of direct generation of electricity from the charged particles they emit (Bishop 1958). The direct conversion of charged particle energy into electric power would eliminate the costly and thermo-dynamically inefficient “balance of plant” portion of a thermal power plant: the steam or gas piping, the turbine and electrical generator, the steam condenser and feedwater heaters. At present, the first fusion power plants in the 21st century are still envisioned to be D-T fuelled thermal power plants that require a thermodynamic cycle. However, fusion retains the

possibility of direct conversion after the technology has matured, while all other types of power plants do not have this possibility.

This chapter demonstrates the favorable safety and environmental characteristics of fusion. Sections 2 and 3 highlight the major differences between fusion and other energy sources. Section 4 describes the philosophy of public safety, the minor consequences of accidents, and the technical justification for not needing evacuation of the public. Section 5 covers the safety provisions established to protect the workers from recognized hazards supported with nuclear industrial data on injuries and fatalities. Section 6 outlines the aspects of chemical waste, thermal pollution, and radioactive waste along with a potential radwaste management scheme toward the ultimate goal of radwaste-free fusion.

2. Fusion, Fission, and Coal Comparison

A comprehensive study was performed in the 1980s by a committee of leading energy experts to compare the most promising candidate fusion concepts at that time to existing and conceptual fission power plant designs. The results showed that the most important advantages of fusion were high public protection from reactor accidents, no public fatalities could result from accident releases due to the low radioactive inventories on site and passive barriers to inventory release, substantial amelioration of the radioactive-waste problem by eliminating the high level waste that requires deep geologic disposal, and diminution of links to nuclear weapons – easy safeguards against clandestine use of a power plant to produce weapon materials, and no inherent production of weapons materials that could be diverted or stolen for use in weapons fabrication (Holdren 1989). Fowler (1997) also discussed these safety and environmental advantages, and that the basic processes required for fusion reactor operations (vacuum, magnetic confinement of plasma, plasma heating) are very dissimilar from fusion-based weapons so there is no connection between fusion power plant technology and the hydrogen bomb.

Generally, fusion radioactive inventories are smaller than in fission, normal and accident releases are smaller than other power plants, and the use of defense in depth design reduces or prevents any radiological or toxicological releases to the public during a power plant accident. The radiotoxicities of neutron-activated fusion materials over the life of the plant are shorter-lived than for fission reactors; many of the fusion products decay over relatively short periods of a hundred years. After that decay period the remaining products are comparable or even below the radiotoxicity of the coal ash at coal-fired power plants (Pease 1991).

Fusion offers other potential environmental advantages compared to coal and natural gas. Crocker (1981) stated that thermal power plants using traditional hydrocarbon fuels have concerns with waste heat thermal pollution, release of the so-called “greenhouse gases” (such as carbon monoxide and carbon dioxide), and effluents from impurities in coal and natural gas (such as sulfur) that can lead to sulfurous acid rain. Coal combustion has other gaseous effluents, called flue gases; these include mercury, chlorine, fluorine, and radon gases. Coal combustion has airborne ash particles called fly ash, which contain arsenic, cadmium, potassium, sodium, lead, antimony, titanium, selenium, barium, beryllium, boron, calcium, cobalt, chromium, copper, magnesium, molybdenum, nickel, vanadium, selenium, strontium, and uranium. The fly ash is captured with high efficiency at the plant (Pavageau 2002). Aluminum, iron, manganese, silicon, and thorium are in the heavy ashes called bottom ash

retained at the plant (Miller 2005). Certainly, the quantities of these elements are low per ton of coal. For example, uranium has roughly 1–100 wppm concentration in many coal samples, although higher concentrations have been seen since coal varies quite widely in impurities (Bisselle 1984). Even with low impurity concentrations, consumption of a thousand tons of coal or more per day at each plant means appreciable masses to filter. McCracken (2005) stated that a coal-fired 1,000 MW electric power plant would consume about 3.5 million tons of coal in a year, and within that coal would be about 5 tons of natural uranium. Burning the coal concentrates the metal impurities inherent in the coal, leaving these metals within the flue gases, fly ash, and bottom ash. Ashes from coal create a disposal problem (Wang 1996), and captured fly ash has been used in road construction and has been investigated for use in building materials (Chou 2003). It is noteworthy that unlike radionuclides, which eventually decay to stable, benign particles (albeit some require very long time periods), chemically toxic elements (such as mercury, selenium, cadmium, chlorine, fluorine, and others) will remain toxic for all future generations. Combustion power plants also release nitrogen oxides into the air. Fusion power plants using a thermodynamic cycle would release heat to the atmosphere or to a body of water, similar to all other thermal power plants. However, fusion power plants would not release greenhouse gases, produce acid rain, release metal aerosols, or release nitrogen oxides. In the more distant future, fusion power plants using direct conversion of charged particle energy into electrical power would release much smaller amounts of waste heat to the environment than present power plants.

One of the virtues of coal-fired power plants is that any off-normal events or accidents that have occurred have not typically posed any significant hazards to the public. Coal-fired plant accidents (Frezza 1978), such as steam pipe ruptures, boiler explosions, turbine blade loss, turbine or generator fires, coal stockpile fires, etc., have presented distinct hazards to plant personnel but generally have not threatened the public nor required public evacuation from the plant vicinity (Burgherr 2008). One feature of a coal plant is that it requires a fair amount of land for its site to accommodate fly ash ponds and the piles of coal unloaded from rail cars. With a large site area, the general public is far (e.g., perhaps a mile or more) from the actual plant buildings; this creates a buffer zone called an exclusion area for protection against shrapnel and pressure waves from explosions, radiant heat and smoke from fires, and any other types of energy released from accident events. One of the few direct hazards to the public from coal power plants, besides the plant effluents, is the hazard posed by the railroad trains that deliver coal to the power plant one or more times per day.

3. Fusion Fuel Safety

Bishop (1958) described some of the earliest recognized benefits of fusion power plants. If mankind could achieve fusion of deuterium then power plants would be fueled by the deuterium (D) isotope of hydrogen. Present ideas for the 21st century fusion power plant are deuterium-tritium (D-T) fuel because the reaction is easier to achieve with less technical challenges than using deuterium fuel alone. D-D fuel is more attractive because D exists in nature and can be separated relatively easily and inexpensively from seawater (Saylor 1983) and also from fresh water, allowing the processed water to be used for other purposes. Thus, D-D fuel can be collected by distillation or other means without the safety issues and environmental impacts inherent in coal mining, uranium mining, or petroleum product

drilling for natural gas or oil. The natural abundance of the D isotope in hydrogen is about 0.015 at% (Weast 1979), which means that there is roughly one D atom per 6500 hydrogen atoms. All countries of the world have access to water and hence to fusion D-D fuel reserves, which leads to a widespread and abundant fuel supply for all countries without constraints on the use of fusion energy for centuries.

A unique fuel-related safety benefit is that a controlled fusion power plant is more inherently safe than other power plants because of fusion's distributed inventory of fuel. A fusion plant regularly injects fuel in tiny quantities, fractions of a gram per second (Murdoch 1999). The plant operates with deuterium and tritium plasma that, at any given time, weighs on the order of 1 gram (McCracken 2005). Fueling the plasma is accomplished either by injecting tiny frozen pellets of D-T into the plasma or by puffing milligram quantities of gas into the fusion chamber at the edge of the plasma. This fueling process is continuous with small amounts of D-T fuel, similar to a coal-fired power plant that pulverizes coal into small particles and injects these small quantities of coal particles into the combustion chamber of its boiler (Environmental Protection Agency 1997). A fusion plant is inherently safe because if the plant is not continuously refueled, the fusion process is limited to a few seconds burn; the fuel in the plasma would be consumed and the plant would be quickly shut down. A fusion plant stores its D-T kg supply for 1-2 days at room temperature in a chemically benign form within a specially designed building (DOE 2007a) to contain the gaseous tritium fuel against leakage. Therefore, no large quantity of fuel is stored within the machine. To the contrary, a fission power plant has over 100 tons of fuel in the core to operate for 12, 18, or 24 months until shutting down to refuel. Deuterium and tritium are combustible gases; however, the onsite inventories for an advanced fusion plant are less than a few kilograms (Murdoch 1999, El-Guebaly 2009), and this small quantity is a very low combustion hazard compared to the large quantities of natural gas or coal dust found at fossil-fueled power plants.

Another inherent fusion safety feature is the lack of a nuclear chain reaction. As stated above, if the plant fuel supply is stopped, the reaction also stops in a few seconds – the plasma will not continue operating. Similarly, if the fusion plasma experienced a postulated thermal runaway event, the extra heat produced would melt or vaporize a small depth of the face of armor tiles on the fusion chamber walls, sending kg amounts of relatively cool temperature impurity particles into the edges of the hot plasma. Such cool (compared to plasma ion temperatures) impurities in quantity at the plasma edge cause a plasma density limit disruption. The plasma would leave its confining magnetic fields, deposit its energy on the armor tiles, quickly and effectively terminating the plasma without any operator or automated safety system intervention. The tiles are built to withstand heat damage from such plasma disruptions. With more study, fusion researchers believe that they will be able to predict plasma performance and control the plasma so that plasma disruptions will be rare and wall armor tiles will no longer be needed (Najmabadi 2006).

Fusion power plants do not use actinide elements such as uranium, plutonium, and thorium. Coal (U.S. Geological Survey 1997) and natural gas and oil (American Petroleum Institute 2006) have part per million concentrations of impurity actinide elements uranium and thorium, and their progeny, radium and radon. These radioactive impurities are generally referred to as naturally occurring radioactive materials (NORM). They are not considered to be a public hazard until the amounts of raw material (coal, natural gas, or oil) grow to very large quantities. A coal-fired power plant can use thousands of tons of coal per operating day, depending on its size and electrical output. With that sort of consumption, even small

concentrations of NORM present reasonably large masses of concern. Fission power plants primarily use ≈ 100 tons of uranium fuel per year, which generates fission products. Comparatively, an advanced fusion power plant would use 100-200 kg of hydrogen isotope fuel per year (El-Guebaly 2009) and the products from D-D or D-T reactions are not radioactive, they are usually neutrons and helium nuclei. The energetic neutrons activate the plasma surrounding structural materials, but not to the same level as fission products. The primary fuel for fusion is tritium with a radioactive half-life of 12.3 years, decaying to helium-3, versus uranium with a radioactive half-life of 4.47×10^9 years and decaying to other radioactive products (Baum, 2002). Thus, a fusion power plant would not use or release any actinide elements, nor would it have the long-lived fission products or the radiotoxicity concerns of storing spent fission fuel. A key safety feature of fusion power is that while fission power plants must use some heavy element (uranium, thorium, or plutonium) for fuel and reactor materials choices are limited by fission neutronics, fusion fuel is hydrogen isotopes and fusion designers can select the most advantageous chamber and armor materials of construction, which are referred to as low activation materials (Kummer 1977). In that way, fusion produces quantities of low-level radioactive waste, which could be recycled or easily disposed of by burial at several sites in the US, versus the high-level radioactive waste (spent uranium or other nuclear fuel) which has become a disposal issue in the fission industry and raises proliferation concerns if reprocessed.

4. Public Safety and Assessment

4.1. Fusion Safety Philosophy and Assessment

This section describes the efforts taken to ensure the safety of the public and the analyses performed to demonstrate public safety. Public safety in many endeavors is attained by the use of a design principle called defense in depth. This principle is used in nuclear power (Petrangeli 2006), the chemical process industry (Kletz 1991, Pekalski 2005), aerospace (Fortescue 1991), and other applications. Defense in depth is both a design philosophy and an operations philosophy to use multiple levels of protection to prevent accidents and mitigate any accidents that might still occur. There are three tiers to defense in depth (Knief 1992):

- The first tier of defense in depth is prevention, where the designers seek to avoid operational occurrences that would result in radiological or toxicological releases that could harm members of the public. Prevention calls for high reliability components and systems and use of prudent operating procedures. Prevention includes selecting inherently stable plant operating characteristics, passive means to benignly shut down the plant in case of an accident, including: safety margins in the design, performance testing and inspections of systems, adequate operator training, and good quality assurance programs.
- The second tier of defense in depth is protection. The plant will have safety functions for protection of the public, workers, and the environment. While high reliability is important, inevitably, some component can fail and present an off-normal occurrence or accident in the plant. Safety systems, such as fast plasma shutdown, air and effluent monitoring, and safety interlock systems, are part of

protection. In fission power plants, these systems are referred to as engineered safety features.

- The third tier of defense in depth is mitigation. This final tier limits the off-site consequences of any accidents that may occur despite the first two tiers. Mitigation systems are confinement buildings (e.g., vacuum vessel and cryostat), air cleaning systems such as filters and scrubbers, ventilation stacks, emergency power systems, and passive heat sinks (Knief 1992). Existing fusion experiments use defense in depth, commensurate with the energies and hazardous materials in use. In the US, this approach is proven, prudent, and mandated by the US Department of Energy (DOE) for fusion experiments (DOE 1996a). The National Ignition Facility (NIF) at Lawrence Livermore National Laboratory (LLNL) (NIF Project) has used defense in depth, multiple barrier concept to protect its radiological and toxicological inventories (Piet 1995, Brereton 1997). The International Thermonuclear Experimental Reactor (ITER), under construction in Cadarache, France, has used defense in depth, called “lines of defense,” as part of its safety design (Saji 1995). Future fusion power plants will use defense in depth as well.

Because fusion research is presently conducted by DOE, that agency has published a safety directive for fusion (DOE 1996a). The facility safety functions to ensure public safety are:

- Confine radioactive and hazardous material within the plant
- Ensure afterheat removal from the fusion device
- Provide rapid plasma shutdown
- Control coolant internal energy
- Control chemical energy sources
- Control magnetic energy
- Limit routine airborne and liquid radiological releases.

The design approaches to accomplish these public safety functions are defense in depth concepts. These are designing for high reliability of plant systems and use of multiple confinement barriers; this includes the use of redundant and diverse components, system independence, simplicity in design, testability of systems and components, use of fail-safe and fault-tolerant design, and incorporation of best practices in human factors for the plant staff and for the control room human-machine interface.

As fusion experiments transition from testing the physics and technology to a demonstration power plant (Demo) and commercial electric power-producing facilities, there will also be a transition from operation and safety regulation by DOE to licensing, operation, and radiation exposure regulation by the U.S. Nuclear Regulatory Commission (NRC). Similar transitions with fission power plants have been seen in the past. The U.S. Atomic Energy Commission, a predecessor to DOE, supported technology development and conducted the Power Reactor Demonstration Program in the 1950s and 60s for light water-cooled reactors (Shippingport and Yankee Rowe pressurized water reactors; Elk River and Dresden boiling water reactors), a gas-cooled reactor (Peach Bottom-1), an organic cooled reactor (Piqua Plant), and liquid metal cooled reactors (Hallam Sodium Reactor Experiment

and Enrico Fermi-1) (Allen 1977). Therefore, government support of new, complicated technology being entered into the market is a plausible path and it is expected that regulations would transition from DOE to NRC when a commercial firm or consortium is a partner in fusion power plant construction and operation. The DOE safety standard for fusion power plants (DOE 1996a) is congruent with NRC regulations.

Table 1. Safety limits on releases from fission power plants

Safety limit	Dose to a Member of the Public	Regulation	Comment
Annual total dose to the public from NRC licensed operation	100 mrem total effective dose equivalent	10CFR20.1301	
Annual airborne release of gaseous effluents to unrestricted areas	10 mrem gamma or 20 mrem beta	10CFR50 Appendix I	
Annual liquid pathway releases of effluents to unrestricted areas	3 mrem whole body or 10 mrem to any organ	10CFR50 Appendix I	
Average annual concentration of beta particle and photon radioactivity in drinking water	4 mrem equivalent to the total body or to any organ	40CFR141.66	
Annual dose equivalent as a result of planned discharges of radioactive materials from the uranium fuel cycle	25 mrem to the whole body	40CFR190.10	Radon and its daughter products are not included in this limit. Fuel cycle includes milling ore, converting and enriching uranium, fuel fabrication, fuel use in a reactor, and reprocessing spent fuel.
Annual dose equivalent of radionuclide emissions to the ambient air	10 mrem effective dose equivalent	40CFR61.92	This regulation applies only to DOE facilities.
Postulated fission product release accident	25 rem total effective dose equivalent in 2 hr at the site boundary during an accident event	10CFR50.34	The 25 rem is a reference value, not an upper limit for emergency dose to the public. The value is used to evaluate designs and keep public risks as low as reasonable achievable.

No power plant, or any other engineered facility, can ever be absolutely safe. Absolute safety of any power plant would mean that the designers were infallible and also that the upper limits of effects from natural disasters could be predicted so that a power plant could be

protected against these effects with absolute certainty (Lewis 1977). Because absolute safety is not possible, all types of power plants have measures of safety developed so that risks to the public are kept as low as possible. Governments legislate licensing and regulatory requirements on power plants. In the U.S., the Environmental Protection Agency (EPA) regulates the pollutant emissions from fossil-fueled power plants and nuclear fission power plants, while the NRC regulates design, construction, operation, and decommissioning of nuclear fission power plants. The Federal Energy Regulatory Commission (FERC) regulates the sale of electrical power to promote a fair market. Because fusion power plants will generate neutrons that activate its structural materials and the initial technically feasible fusion power plants will be fueled with radioactive tritium fuel, these fusion plants are expected to be regulated with the same safety rules and regulations applied to fission power plants. There will be stringent NRC requirements on plant design, plant location, construction, operation, and decommissioning. Table 1 gives some of the existing public safety limits for operating fission power plants; these limits are expected to be applied to fusion power plants.

A question that often arises is: Are these limits safe enough? A fission power plant is mandated by the NRC in its 1986 Safety Goals to: a) limit the accident risk to any member of the public to less than 0.1% of the sum of prompt fatality risks resulting from other accidents to which members of the population are generally exposed, and b) prevent exposure of any member of the public to cancer-causing radiation that would cause greater than 0.1% of the sum of cancer fatality risks from all other causes (NRC 1986, Ramsey 1998). In essence, the NRC tried to answer the question: How safe is safe enough? (Kumamoto 2007). The NRC believes that remaining below a 0.1% additional risk is inconsequential for members of the public. This safety goal is likely to be applied to fusion power plants as well. The experience with ITER (ITER Project) and NIF, both of which easily meet this goal, suggests that future fusion power plants can meet and exceed this safety goal.

4.2. Deterministic Safety Analysis

With the public exposure limits established, fusion power plants can be evaluated by analytical methods similar to other nuclear power plants. There are two means to evaluate power plant safety, and they complement each other. The first means is a traditional approach called deterministic safety analysis. Keller (2005) describes that in the early days of the nuclear fission industry, the Atomic Energy Commission (AEC) regulatory engineers—with very little operating experience data available to them—avoided the need to calculate best estimate uncertainties for safety systems by using deterministic approaches that used conservative assumptions and calculations. Safety was defined as the ability of the fission power plant to withstand a fixed set of prescribed, or determined, accident scenarios that the AEC judged to be the most significant adverse events that could occur in a fission power plant. If the plant could maintain public safety with these events, then the plant could maintain safety in other, lesser consequence events that were more likely to occur. Therefore, the approach was to determine a set of plausible accident causes, select those causes that had the highest consequences to public health and safety, and thoroughly analyze the set of resulting accidents. If the consequences were tolerably low, then the plant would be termed safe. These events challenge the design basis of the plant. The design basis is the set of

requirements used in the basic design of the power plant, including safety, reliability, plant availability, maintainability, and plant efficiency. The accidents analyzed in deterministic safety analysis are referred to as design basis accidents. The set of high-consequence fault events to be analyzed is determined by severity rather than identified via frequency of occurrence, so this type of analysis is referred to as deterministic (Pershagen 1989). Computational models of thermal-hydraulics and neutronics are used for fission power plants to analyze the chosen events. Deterministic analysis is performed to show that permissible values of plant parameters chosen in the design basis are not exceeded to prove that the design is robust against accident events.

The deterministic analysis for fission power plants has been guided and refined by the NRC in 10CFR50 to consider a wide variety of specific events; for example, rupture of a large cooling water pipe – called the large break loss of coolant accident. Abramson (1985) and Lillington (1995) give detailed explanations and methods to analyze the accident events that must be considered. The results of the analyses are documented in a safety analysis report (SAR), which is a required document in the plant licensing process. The NRC has a Standard Review Plan (NRC 2007) that outlines what should be included in a SAR and how a review of a SAR for the licensing process should be conducted.

A SAR document has been required since the beginning of commercial fission power plants in the U.S. and has many important purposes. One of these is to describe the power plant, its design, fabrication, construction, testing, and expected performance of plant structures, systems, and components important to safety. This document shows compliance with NRC regulations, especially the General Design Criteria specified in Appendix A of 10CFR50. The SAR also presents the power plant's site and meteorology, the plant safety limits, safety settings, control settings, limiting conditions of operation, surveillance requirements (testing, calibration, and maintenance), emergency procedures, and technical specifications of operation. A third purpose for a SAR is to demonstrate that the power plant can be constructed, operated, maintained, shut down, and decommissioned both safely and in compliance with all regulations, laws, and design requirements. The SAR and its deterministic analysis have been described in Pershagen (1989). The NRC accepts a preliminary SAR and reviews it as part of the power plant operating license process. The primary NRC concern is that the proposed power plant can be operated without undue risk to the public (Okrent 1981). If the preliminary document were approved by the NRC, then the utility company would be granted a construction permit to begin plant construction. The utility staff then prepares a final SAR. The final SAR gives more detail about the final design and operation of the power plant. No matter what changes occur with the licensing process, a final SAR is required before the NRC will grant an operating license to the plant.

Fusion designs with the DOE have followed the same path: to develop a SAR on the design. However, fusion does not have the development effort or the operating experience that fission power plants have had to support identification or determination of accident events.

4.2.1. Experimental Facilities: ITER and NIF

The ITER design, shown in Fig. 2, followed a progression of steps to build its own safety process that followed the same path that fission power safety had followed. The ITER staff strived to construct a safety analysis framework that accounted for the unique aspects of

fusion. An International Atomic Energy Agency (IAEA) report called the General Safety and Environmental Design Criteria (GSEDC) (IAEA 1996) was written early in the ITER engineering design activity; it outlined the safety objectives, public safety functions, and safety design requirements for ITER systems. The GSEDC was followed by the Early Safety and Environmental Characterization Study, a project document that identified radiological and toxicological inventories, gave limits for inventories, estimated routine releases, and gave direction about calculating consequences from both routine and accidental releases of radiological and toxicological materials. These documents set the safety and environmental criteria for the design teams to follow. The ITER project team then wrote the Non Site-specific Safety Report (NSSR) to address ITER safety at a generic location because the site had not been selected (Bartels 1995, Bartels 1998). Twenty-five reference accidents were evaluated in the NSSR. ITER reference events are design basis accidents—fundamental events included in safety analyses. These accidents included plasma upset events, cooling upset events, electric power upset events, loss of coolant into the vacuum vessel, loss of coolant outside of the vacuum vessel, loss of coolant flow, loss of vacuum, tritium leaks, maintenance events, magnet arcs, and other events. All events were analyzed and found to have radioactive releases much lower than the stringent ITER release limits, which were set lower than those of the participating countries to ensure ITER would be licensable in any participant country. The analysis proved that the ITER design was robust against the reference events. A set of hypothetical accidents were also identified to investigate the “ultimate safety margins” of ITER to verify that no events, at frequencies just below the $1 \times 10^{-6}/\text{yr}$ frequency limit for reference accidents, existed which would pose high consequences to the public (Petti 1999). In DOE terms, the ultimate safety margin events would be called beyond design basis events (DOE 2006). This is referred to as cliff-edge effects, the cliff being the $1 \times 10^{-6}/\text{yr}$ frequency limit. That analysis showed that nearly all of the hypothetical events resulted in public doses of less than 1% of the strict ITER dose limit. The largest dose from the highest consequence accident, an ex-vessel loss of coolant accident with several aggravating failures and a confinement bypass to the environment (which is not plausible in the design), was less than 75% of the ITER no-evacuation dose limit of 50 mSv (5 rem). The analysis showed that there were no drastic increases in radiological releases for any events below the frequency limit. Also, ITER had several inherent safety advantages that maintained public safety during accident events:

- The vacuum vessel was designed with adequate structural margins to maintain integrity in all events
- Any ITER runaway plasma would shut down by impurity ingress from the wall materials or from in-vessel water or air leaks
- The design values of radioactive inventories were modest so the confinement barriers were not taxed to confine releases.

A passive heat removal system is used that removes radioactive decay heat from the vacuum vessel in case of an accident that includes loss of electrical power to operate the plant shut down equipment (Bartels 1996). The system uses natural convection, buoyancy-driven flow of water to the building roof so that heat is exchanged via an ambient air heat exchanger. The ITER team noted that the ex-vessel loss of coolant event did come close to the no-

evacuation dose limit and made design provisions to reduce the potential frequency of occurrence and reduce the radiological inventory to decrease the consequences if the event did occur.

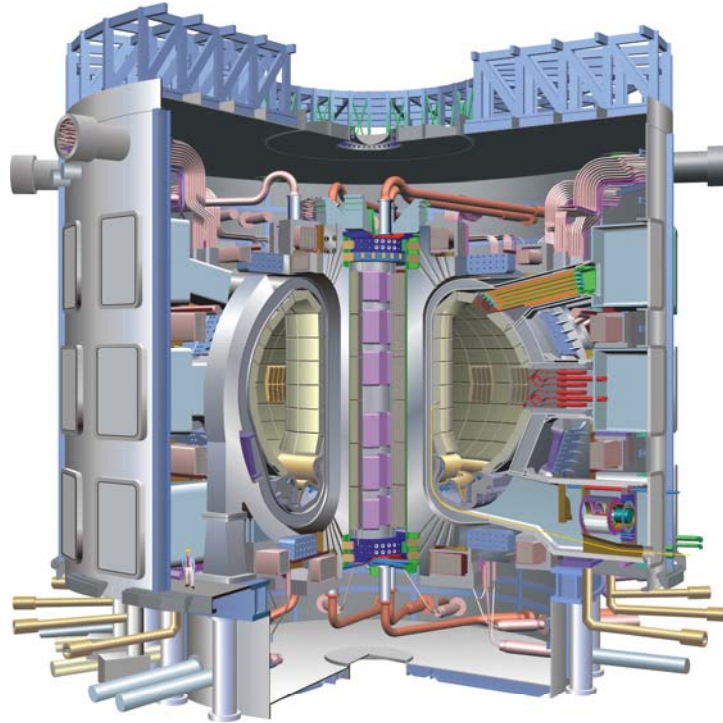


Figure 2. Isometric view of ITER (ITER Project). Published with permission of ITER.

As the ITER project progressed and developed more design detail, a Generic Site Safety Report (GSSR) was written. It is summarized in an ITER design report (IAEA 2002). The GSSR is a SAR for a generic site that might have been located in any participant country. The GSSR formed the basis of plant-level safety assessment and licensing efforts for ITER (Gordon 2001, Marbach 2003, Gordon 2005, Rodriguez-Rodrigo 2005). After the ITER site was selected in June 2005, additional safety assessment to tailor the GSSR to the Cadarache site in southern France and to account for host safety and environmental regulations was performed (Taylor 2007, Girard 2007). The GSSR and follow-on work was used as the basis for the Rapport Preliminaire de Surete (preliminary SAR) that was requested by the French safety regulator, the Autorite Surete Nucleaire (ASN) (Girard 2007, Taylor 2009). The ASN grants permission to construct and operate nuclear experiments and assigns licenses to nuclear power plants. ITER licensing as a basic nuclear installation has followed the approach of public hearings and reviews by safety experts and the ASN (Girard 2007). The ITER preliminary SAR was delivered to the ASN in early 2008 and is under revision as of this writing. The expectation is that the RPrS will be approved by the ASN in 2010 and after approval it will become a public document. French regulations for nuclear research facilities are overall very similar to U.S. regulations for nuclear facilities and power plants, so a regulatory precedent has been set for large fusion experiment projects.

A SAR was also required by DOE for licensing operation of the NIF (LLNL 1999). Figure 3 displays the NIF layout. The NIF SAR followed the DOE guide for safety analysis that has evolved over time (DOE 2006). This guidance is very similar to NRC direction for safety analysis, although the DOE guide allows the use of probabilistic techniques (refer to Section 3.3) to apply a graded approach to safety. A graded approach means that as hazards and inventories increase the level of safety assessment, the depth and rigor of safety assessment, also increases commensurately. SARs for past magnetic fusion experiment designs (Motloch 1995) have also used the DOE SAR guide. These fusion experiment SARs are similar to fission reactor SARs but account for the unique aspects of fusion.

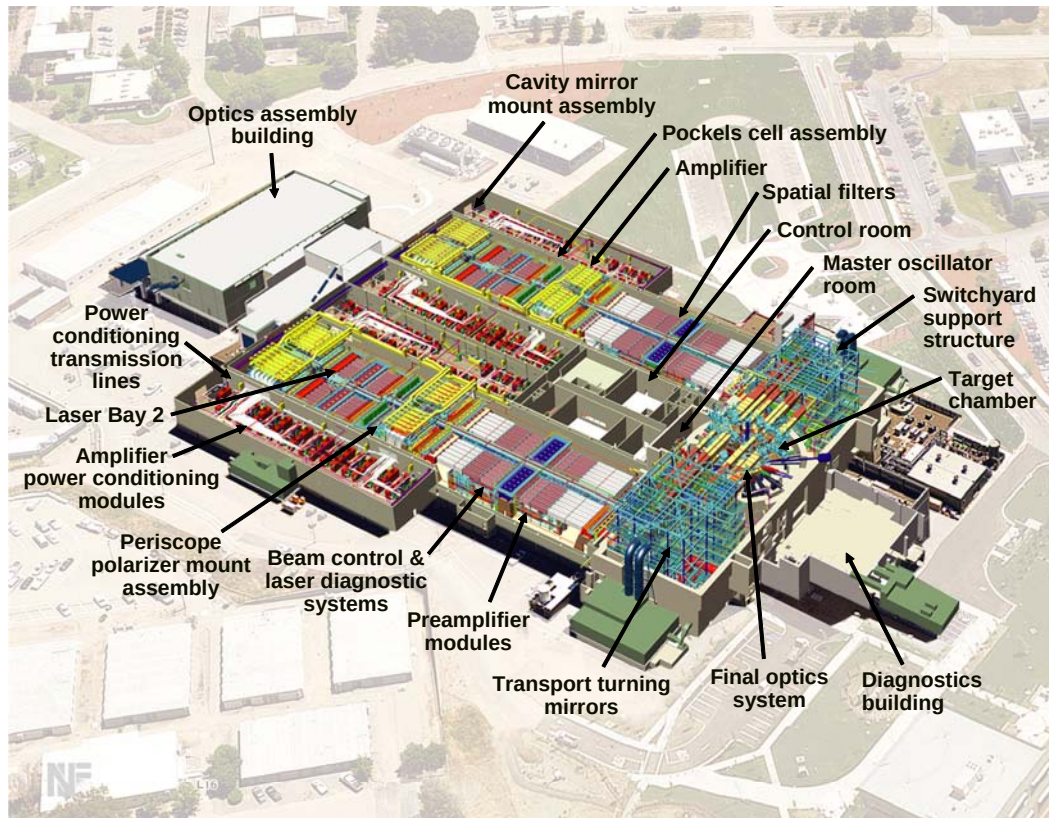


Figure 3. Layout of NIF. The photo with a cutaway roof shows the laser beam lines that traverse the length of the facility, converging on the spherical target chamber. Further information is available at the NIF website (NIF Project).

4.2.2. Power Plants

Previous studies for setting public safety bounds in both inertial and magnetic fusion used analyst judgment to identify the highest consequence accidents, with good results (Khater 1992a, Khater 1992b, Khater 1996, Steiner 1997, Khater 1998, Petti 2001, Khater 2003, Petti 2006, Merrill 2008, Reyes 2001).

In the US, the ARIES design studies look to the future and envision the far end of the fusion power development path past ITER and past a demonstration plant, to the tenth of its

kind fusion power plant. The ARIES studies have included deterministic safety analyses (Steiner 1997, Khater 2003, Petti 2006, Merrill 2008). Like ITER safety, the radiological inventories and decay heat levels were defined, and traditional accidents such as loss of coolant and loss of flow were analyzed (Khater 2003, Mogahed 1997, Mogahed 2001, Petti 2006, Martin 2007, Merrill 2008). For the ranges of accidents considered in fusion deterministic analyses, including loss of vacuum accidents, in-vessel loss of coolant with confinement barrier bypass, and ex-vessel loss of coolant, the ARIES advanced tokamak and compact stellarator designs meet the no-evacuation dose limit of 1 rem at the site boundary (DOE 1996a) for all events. The most recent advanced tokamak design (ARIES-AT) is shown in Fig. 4. All ARIES designs incorporate reduced activation materials, passive decay heat removal, and defense in depth confinement strategy of multiple barriers to releases. In summary, past ARIES studies demonstrated an adequate performance for different power plant concepts in several safety and environmental areas:

Occupational and public safety:

- No evacuation plan following abnormal events (early dose at site boundary < 1 rem) to avoid disturbing public daily life.
- Low dose to workers and personnel during operation and maintenance activity (< 2.5 mrem/h).
- Public safety during normal operation (bio-dose << 2.5 mrem/h) and following credible accidents:
 - LOCA, LOFA, LOVA, and by-pass events.
 - External events (seismic, hurricanes, tornadoes, airplane crash, etc.).

No energy and pressurization threats to confinement barriers (vacuum vessel and cryostat):

- No melting, no burning
- Decay heat problem solved by design
- Chemical energy controlled by design
- Chemical reaction avoided
- Stored magnet energy controlled by design
- Overpressure protection system
- No combustible gas generated
- Rapid, benign plasma shutdown.

Environmental impact:

- Minimal radioactive releases (such as T, volatile activated structure, corrosion

products, and erosion dust. Or, from liquid and gas leaks) during normal and abnormal operations.

- Low activation materials with strict impurity control $\Rightarrow$ minimal long-term environmental impact
- Minimal radwaste $\Rightarrow$ recycling and clearance, avoiding disposal
- No high-level waste (HLW).

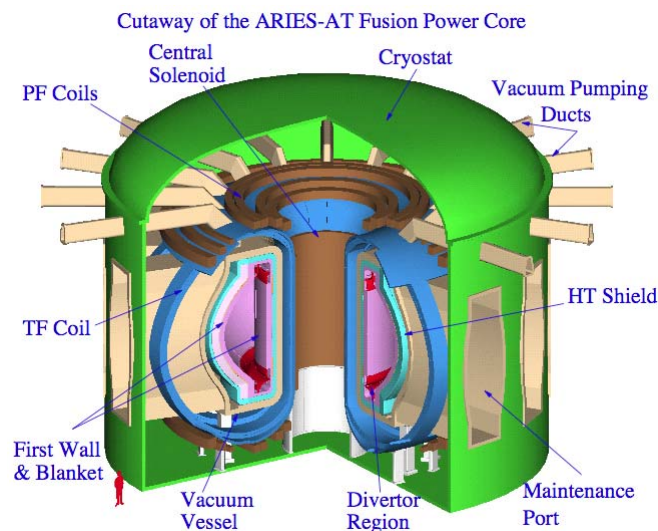


Figure 4. Isometric view of ARIES-AT (Najmabadi 2006).

In Europe, a series of studies delivered two reports (Raeder 1995, Cook 2000) on the safety and environmental assessment of fusion power (SEAFP). A wide range of postulated accidents have been analyzed, involving parametric studies, with a special consideration given to the radwaste management approaches. The SEAFP studies conclude: “it is possible to fully benefit from the advantages of fusion energy if safety and environmental concerns are taken into account when considering the first prospective studies of a reactor design. Improvements in this way will need the continuation of R&D on reactor design and materials as well as specific R&D on safety aspects.”

4.3. Probabilistic Safety Assessment

The second type of safety analysis is probabilistic risk assessment, also known as probabilistic safety assessment (PSA). The PSA approach is the opposite from the ‘What if?’ deterministic approach to identification of consequential events. PSA identifies a wide spectrum of system and component failures and errors over a wide range of occurrence frequencies, from annual plant off-normal events to very low one event per million years or lower. These failure events or errors initiate plant accidents that can lead to radiological or toxicological releases. The PSA then analyzes the safety system responses to those failures and errors. Of these many possible accident progressions, there are outcomes that are benign

to the public and outcomes that are slight or moderate hazards to the public; the worst events from the deterministic safety analysis are also included in the spectrum of plant states from the initial failure event. Keller (2005) described the progress of PSA in the nuclear fission industry after the Three Mile Island-2 accident in 1979. While estimates of accident consequences had been examined in preceding years to support federal indemnification of fission power plants (AEC 1957), the NRC embarked on a more definitive risk-based study, called the WASH-1400 study, in the early 1970s when the indemnification process was under review for another approval cycle (NRC 1975). The WASH-1400 study met that purpose, but then was not being utilized for any other purposes. After the Three Mile Island accident in 1979, nuclear safety professionals in the industry noted that the traditional safety analysis report did not treat the small break loss of coolant event as well as the WASH-1400 study (Keller 2005). After consideration, the NRC required each existing power plant to perform a PSA for the Individual Plant Examination study (NRC 1988). While PSA is not an official requirement for new fission power plant licensing, recent US fission licensing changes in 10CFR50.52 state that a summary of the PSA results must appear in the plant's SAR. PSA studies are viewed as worth the effort to identify risks and serve as a supplement to the licensing documentation. The US NRC has recognized that PSA enhances and extends the traditional, deterministic safety analysis approach by allowing consideration of a broader set of challenges to plant safety, providing a systematic means to prioritize these challenges and by allowing, or taking credit for, a broader set of plant resources to defend against the challenges (NRC 1995). The NRC has also issued a guide on PSA adequacy for use in risk-informed decision making (NRC 2009). The present view by the US NRC is that PSA is valuable and has been used in safety decisions and plant equipment upgrades. If a new license application does not include a PSA then NRC would request that a PSA be performed to fill in the gap.

Early thoughts about PSA benefits for fusion power safety assessment were made by Piet (1985, 1986). Magnetic fusion has made use of PSA techniques in some small safety assessment reports (Holland 1991, Cadwallader 1993, Brereton 1996), some system-level studies (Cambi 1992, Schnauder 1997, Hu 2007) and in ITER as multiple, independent, and exhaustive methods to support completeness in the accident identification process (Taylor 1998, Pinna 1998, Cadwallader 1998). The IGNITOR design has had an entire PSA performed, with updates (Carpignano 1995, Carpignano 1996, Ruscello 2002). Completeness in accident-initiating event identification is one of the greatest criticisms of PSA, that if an accident is not identified it will not be modeled and therefore the PSA is incomplete (Fullwood 1988). Use of multiple means to identify lists of potential initiating events is necessary to achieve practical completeness so that anything left off of the list is inconsequential. There are several good guides for risk assessment (NRC 1983, NRC 1990, McCormick 1981, Kumamoto 1996, Fullwood 1999).

Inertial fusion has also applied probabilistic techniques for accident analysis (Lazaro 1996, Cadwallader 2002, Latkowski 2003). At this writing, inertial fusion has not performed an entire PSA study and magnetic fusion has only performed one PSA study on a tokamak and another on a materials test facility (Burgazzi 2004). The most frequent problem cited for not performing a PSA, besides the cost of a PSA and lack of design detail in most fusion conceptual designs, is that there is scarce data on component failure rates available to quantify a PSA. There are two factors to consider regarding quantification data. The first factor is that a fusion experiment is composed of many standard components for cooling

systems, electric power distribution systems, confinement barriers, ventilation systems, and other systems that do have pertinent data available from other industries. Therefore, some component failure rate data are readily available for the typical support systems in a fusion plant. The fusion-specific components, especially in-vessel components, do not have much failure rate data available, as seen by efforts to quantify such components (Cadwallader 2007a). Some fusion-relevant data are available from particle accelerator operations—accelerators use hundreds to thousands of any single type of component and operate for thousands of hours per year, so they produce statistically significant data. Fusion-specific data on systems unique to fusion are also being collected in the U.S. and the European Union (Cadwallader 2007b). These data arise from the operating experiences of existing tokamak experiments and the components in these experiments do not have very high neutron fluence, so there is skepticism about applicability to future experiments and prototype power plants. Nonetheless, these are the best data available to apply to the fusion-specific equipment used on the next generation of tokamaks. With work to harvest existing, relevant data for fusion balance-of-plant systems and with collection and analysis of fusion-specific data, future fusion projects will have the tools available to make better use of PSA.

5. Personnel Safety

In nuclear facilities, there are two facets of personnel safety: radiation exposure and industrial safety. Ionizing radiation exposure, either by direct radiation or by absorption of radioactive materials into the body, tends to be a long term or chronic health issue. Industrial safety includes both acute and chronic personal injuries that may lead to debilitation or death, from hazards such as falls from height, vehicle collisions, electrical energy exposure, and workplace exposures to chemicals, noise, repetitive motion, and other hazards. This section describes both of these facets of personnel safety.

An important aspect of personnel safety is rules and regulations regarding exposure to radiation and other hazards. Present-day magnetic fusion experiments in the U.S. are either operated by the U.S. DOE and its contractors or by universities. The machines operated by DOE are the largest experiments in the U.S. These machines, notably the DIII-D experiment (DIII-D experiment) at General Atomics in California and the National Spherical Torus Experiment [NSTX] (NSTX experiment) at the Princeton Plasma Physics Laboratory in New Jersey draw fair amounts of power, several megawatts of electricity, and can use large quantities (cubic meters) of cryogenic liquids such as liquid nitrogen and liquid helium. The university machines, with perhaps the exception of the Alcator C-Mod machine (Alcator experiment) at the Massachusetts Institute of Technology, draw more modest power (kW versus MW), are smaller in size, and use protium rather than deuterium fuel. The personnel safety of existing university experiments is maintained by the college or university operating the machine and perhaps by the state hosting the university. Federal safety rules are also often incorporated at university machines for the safety of students and staff. The DOE fusion experiments have personnel safety rules developed from the general DOE worker safety rules (including U.S. Occupational Safety and Health administration [OSHA] regulations) and from identification and treatment of hazards unique to fusion research. Fusion facility safety requirements and guidance are stated in the Fusion Safety Standard and related guidance (DOE 1996a and 1996b).

The DOE standards outline some basic public and personnel safety rules for fusion power plants. For personnel safety, the DOE standard states that workers in a fusion power plant shall be protected from routine hazards to a level commensurate with that of comparable industrial facilities by a combination of administrative controls and design features. Routine hazards include exposures to radiation, electromagnetic fields, and industrial hazards. During normal operations, workers will be exposed to radiation levels below the limits given in 10CFR20 (5 rem/yr) and DOE (2005) (2 rem/yr). Because tritium is the predominant nuclear material used in fusion facilities, facility designs will include special consideration to limit worker doses from inhalation and skin absorption of tritium fuel. The as-low-as-reasonably-achievable (ALARA) principle in averting radiation exposure would be followed in fusion design like it is in fission reactor designs. Workers will also be protected from exposure to magnetic fields and radiofrequency energy fields, with the facility design keeping worker exposures below the limits published by the American Conference of Governmental Industrial Hygienists (DOE 1996a). Lastly, fusion power plant facilities will comply with all federal safety regulations for workers. Because the industrial hazards are not unique to fusion, the fusion facilities will follow the OSHA regulations and other commonly accepted practices.

Safety is a moral obligation of facility management and is a legal requirement mandated by federal law. However, there are also benefits to investing in safety. Safe operations tend to be efficient operations. Mottel, who wrote specifically about industrial safety, states that safety, worker productivity, and high quality in operations all complement each other (Mottel 1995). Fission power plants have apparently learned this lesson: plant capacity factors are high, in the 90% range (Blake 2006), and, as will be discussed later, occupational injury rates are low.

5.1. Radiation Safety

Besides decreasing the ionizing radiation source strength as much as possible, the fundamental approach to radiation protection is three-fold: increase distance to the source, reduce exposure time, and use radiation shielding (Gollnick 1994). It should be noted that radioactive inventories in low-activation fusion design have a lower radiation hazard than fission inventories by 1 to 2 orders of magnitude (Pease 1991).

The first, and most effective, approach for radiation protection is to increase the distance between the worker and the radiation source. Radiation dose is inversely proportional to the separation distance for 'line sources' (e.g., pipes holding radionuclides in water), which is perhaps the most frequently encountered situation in a power plant. Methods to increase distance include using long-handled "reach rods," designing rooms to allow work to be carried out at a distance, and using remote handling equipment that allows workers to stay further back from radiation sources.

In the second approach, exposure times are decreased as much as possible. Radiation doses are directly proportional to the time spent in a radiation field. If the worker exposure time to the radiation hazard can be reduced, then the worker is safer. Time reductions are usually accomplished by a variety of engineering means: using components designed and constructed for very high reliability so that hands-on maintenance is greatly reduced, using remote handling machines to replace personnel performing hands-on work in high radiation

areas, and designing to reduce the radiation hazard. Radiation work permits also review the time needed to perform specific tasks in radiation areas. There are a number of federal regulations that give radiation limits for workers. These are given in Table 2. Jones (2005) gives an excellent review of the history behind these regulations.

The third approach is radiation shielding. Shielding is material that will attenuate the radiation, reducing radiation energy so that it is less hazardous after traversing the shielding. In fusion devices, all in-vessel components (the blanket, shield, and vacuum vessel) provide a shielding function, and the shielding is close to the source of radiation. The types of radiation to be shielded are generally alpha particles, beta particles, gamma rays, and neutrons. Alpha particles are easily shielded by thin layers of nearly any material. Beta particles are easily shielded by thin coatings of metals. Gamma rays are more penetrating and are best shielded by very dense materials, such as lead (Gollnick 1994). Lead bricks are often stacked up near experiments to form shielding walls for gamma rays, but lead is not as effective for neutrons. Neutrons tend to be more difficult to shield. If neutrons can be slowed down to low energies, then they can be absorbed. Neutron slowing down is best accomplished by heavy metals, such as tungsten, and hydrogen-containing materials, such as water and concrete. The tungsten and hydrogen slow down the high-energy neutrons through collisions, then neutron-absorbing materials, such as boron carbide and tungsten carbide, can be used to absorb the majority of low-energy neutrons (El-Guebaly 1997, El-Guebaly 2006). A concrete-based 'bioshield' normally surrounds the fusion power core to capture the leaked neutrons and protect the workers and public against radiation exposure.

Table 2. Primary U.S. radiation safety regulations

Description of Regulation	Regulation Citation
The licensee shall control the occupational dose to individual adults to an annual limit of 5,000 mrem. Derived air concentration (DAC) values and annual limit on intake (ALI) values may be used to determine an individual's dose and to demonstrate compliance with occupational dose limits.	10CFR20.1201 Occupational Dose Limits for Adults
The occupational dose limits for minors are 10% of the annual dose limits specified for adult workers.	10CFR20.1207 Occupational Dose Limits for Minors
The occupational exposure of a declared pregnant woman, during the entire pregnancy, shall not exceed 500 mrem.	10CFR20.1208 Dose Equivalent to an Embryo/Fetus
The total effective dose equivalent to individual members of the public from licensed operations does not exceed 100 mrem/yr.	10CFR20.1301 Dose Limits for Individual Members of the Public
A site will be considered acceptable for unrestricted use if residual radioactivity after cleanup results in a total effective dose equivalent that does not exceed 25 mrem/yr, including ground water used for drinking water and that radioactivity has been reduced to ALARA levels.	10CFR20.1402 Radiological Criteria for Unrestricted Use

Table 2. Continued

Description of Regulation	Regulation Citation
The occupational dose received by general employees shall be controlled such that a total effective dose of 5,000 mrem is not exceeded in a year. Note: the US DOE set an Administrative Control Limit of 2,000 mrem/yr (DOE 2005).	10CFR835.202 Occupational Dose Limits for General Employees
Any planned special exposures of workers must remain within 10CFR835.202 limits.	10CFR835.204 Planned special exposures
Any emergency situation that leads to voluntary personnel emergency exposure, for actions whose benefits exceed the risks, can exceed 10CFR835.202 limits	10CFR835.1302 Emergency exposure situations
Control personnel radiation exposure from external sources in continuous occupancy areas (2000 hr/yr) to levels below an average of 0.5 mrem and as far below as is reasonably achievable.	10CFR835.1002 Facility Design and Modifications
Airborne radioactive material shall be controlled to avoid releases to the workplace atmosphere in normal operations, and in any situation inhalation of such material by workers shall be controlled to levels that are ALARA; confinement and ventilation shall normally be used.	10CFR835.1002 Facility Design and Modifications
No employer shall cause any individual in a restricted area to receive in any one calendar quarter a dose in excess of 1,250 mrem.	29CFR1910.1096 Ionizing Radiation
The annual dose equivalent does not exceed 25 mrem to the whole body of any member of the public as the result of planned discharges of radioactive materials to the general environment from uranium fuel cycle operations. Fuel cycle operations are defined as uranium ore processing (not mining), uranium fuel fabrication, generation of electricity by fission, and reprocessing of spent fuel.	40CFR190.10 Standards for Normal Operations
The average annual concentration of beta particle and photon radioactivity from man-made radionuclides in public drinking water must not produce an annual dose equivalent to the total body greater than 4 mrem/yr	40CFR141.66 Maximum Contaminant Levels for Radionuclides
Emissions of radionuclides to the ambient air from DOE facilities or regulated facilities shall not exceed amounts that would cause any member of the public to receive an effective dose equivalent of 10 mrem in a year.	40CFR61.92, 40CFR61.102 Standard
No source at a DOE facility shall emit more than 20 picocuries per square meter per second of Radon-222, averaged over the entire source, into the air.	40CFR61.192 Standard

Comparing radiation exposure experience between a fusion research reactor and commercial nuclear reactors is an interesting exercise. Presently, fusion experiments operate for only fractions of a year, such as 20 weeks per year, and only for about 8 hr/day. The

ITER reactor plans to operate for 25% of a calendar year. Mature nuclear fission power plants operate for nearly an entire year at a time, with refueling outages occurring every 18–24 months. Table 3 shows personnel radiation doses for a fusion experiment; Table 4 shows the same rates for commercial plants.

Table 3. DOE occupational radiation exposure in fusion research at PPPL

Year	Number of Personnel with Less than Measurable Dose	Total Number of Personnel Monitored for Dose	Collective Dose for Site (person-rem)	Average Annual Measured Dose per Person (mrem)
1994	519	620	3.155	31
1995	474	544	3.254	46
1996	320	424	6.023	58
1997	293	381	2.943	33
1998	240	275	1.080	31
1999	372	406	0.817	24
2000	407	466	2.941	50
2001	376	484	7.420	69
2002	281	426	3.707	26
2003	237	348	0.593	5
2004	231	355	1.141	9
2005	209	345	1.164	9
2006	204	359	1.544	10

Source: DOE 1994–2006.

Table 4. NRC occupational radiation exposure in commercial fission power reactors

Year	Number of Personnel with Less than Measurable Dose	Total Number of Personnel Monitored for Dose	Collective Dose for All Sites (person-rem)	Average Annual Measured Dose per Person (mrem)
1994	68,927	142,707	21,695	294.0
1995	62,080	133,066	21,674	305.3
1996	59,238	127,420	18,874	276.8
1997	58,501	126,689	17,136	251.3
1998	77,080	148,424	13,169	184.6
1999	74,867	150,287	13,665.7	181.2
2000	73,793	147,901	12,651.7	170.7
2001	73,206	140,776	11,108.6	164.4
2002	76,270	149,512	12,126.2	165.6
2003	77,889	152,702	11,955.6	159.8
2004	80,473	150,322	10,367.9	148.4
2005	82,574	160,701	11,455.8	146.6
2006	84,558	164,823	11,021.2	137.3

Source: NRC 1994–2006.

The NIF laser fusion facility has completed installation and testing of the last of its 192 lasers in the fall of 2008. Thus far NIF has operated very little and there are no annual occupational exposure data to present. NIF has set a maximum occupational radiation exposure goal of 500 mrem/yr for individual workers. As seen from Table 2, this is one-tenth

of the federal occupational radiation exposure limit (5 rem/yr) and one-fourth of the DOE administrative control limit (2 rem/yr). The NIF will meet this goal by shielding design and analysis, use of temporary shielding during maintenance activities, use of delay times before personnel access the machine, constraints to allowable stay times in radiation areas, use of remote operated equipment, radiation safety training, and proper procedures (Brereton 1997). The NIF collective dose goal is ≤ 10 person-rem/yr (Latkowski 1999), which is much lower than the annual person-rem estimates given in Table 4 for fission power plants. Using the person-rem data in Table 4 and noting that there are 104 operating power reactors, the average collective dose is approximately 100 person-rem or more annually for a fission power plant.

The ITER magnetic fusion project has set radiological exposure goals based on international recommendations. The ITER worker dose limit is 2 rem/yr and a maximum of 200 mrem/shift (Moshonas 2001, IAEA 2002). The IAEA describes the approach to radiation safety at ITER, with radiation zones coded white (unlimited access for all workers, < 0.05 mrem/hr), green (unlimited access for radiation workers, < 1 mrem/hr), amber (limited access for all workers, < 100 mrem/hr), and red (restricted access, > 100 mrem/hr). Considering Table 4, U.S. power plant workers have an individual worker exposure limit of 5 rem/yr and the averaged exposures show that workers are experiencing factors of 15 to 35 times less than the limit. ITER will also strive to have low radiation exposures; ITER will almost certainly be between existing fusion experiment doses given in Table 3 and fission power plant doses given in Table 4. The ITER collective personnel radiation dose has been estimated to be 25.8 person-rem/yr with hands-on work and 19.7 person-rem/yr with remote handling assistance to workers (Sandri 2002). The ITER team is using experiences from existing fusion experiments to assist with setting realistic and safe worker radiation exposure goals (Natalizio 2005a; Natalizio 2005b). The ITER limit for collective dose to workers is 50 person-rem/yr (Uzan-Elbez 2005).

5.2. Industrial Safety

Industrial safety includes industrial hygiene and occupational safety. Industrial hygiene is the recognition, evaluation, and mitigation or control of work-related environmental factors or stressors that may cause sickness, impaired health and well-being, or significant discomfort and inefficiency (Goetsch 2008). Occupational safety dwells on identification of energy sources and workplace hazards to workers and mitigation of these hazards so that employers provide a workplace free of recognized hazards that are causing or are likely to cause death or serious physical harm to employees. This is directed by OSHA in the General Duty Clause (29CFR1903.1) and by DOE in 10CFR851.10.

Fusion experiments presently have most, if not all, of the typical industrial hazards, including electrical energy, pressurized fluids energy, compressed gas energy, walking and working surfaces, working at elevation, material handling (e.g., fork lift trucks and cranes), welding and hot work, chemical exposures, fire hazards, work with machine tools, and confined spaces. Fusion experiments also incorporate a number of potential hazards that individually may be found in other industries but are grouped together in a fusion facility such as: cryogenic fluids, microwave radiofrequency heating, high magnetic fields, lasers, vacuum chambers that present large vacuum reservoirs, capacitor banks, high voltage power

supplies, and ionizing radiation. The expectation is that these hazards will remain inherent in fusion power plants as fusion energy matures to provide electricity.

Industrial hazards are presently managed well for fusion by the time-tested combination of engineering controls, administrative controls, and personal protective equipment (PPE). Engineering controls are passive measures designed into the workplace to prevent contact with a harmful substance or other hazard. Engineering controls may include eliminating hazards or substituting lesser hazards, changing the process design, adding barriers, and isolating or enclosing hazards. Administrative controls include limiting the time workers are exposed to hazards, worker rotation to minimize exposure, proper housekeeping in the facility, and good training. PPE refers to supplied air or respirators, anti-contamination clothing, industrial helmets, safety glasses, etc. Engineering and administrative controls reduce or eliminate hazards; these are the preferred means of hazard control. By its nature, PPE is not a strong barrier between the worker and the hazard, and the hazard is still present so PPE is the last means used for hazard control (Laing 1992).

Analysis of personal injury reports from fusion experiments has shown that the leading causes of injuries result from standard industrial issues such as falls from height, slips, trips and falls, dropped loads, strains, and sprains (Cadwallader 2005). Therefore, strong occupational safety programs for fusion should give time and attention to the typical industry hazards addressed by OSHA as well as the hazards unique to fusion energy. Occupational safety can be analyzed, rather than merely relying on prescriptive sets of rules. Two good texts for analysis are Harms-Ringdahl (2001), which outlines several proven analysis and modeling techniques, and Brauer (2006), which describes personnel vulnerabilities to many hazards and energies.

Table 5 lists the lost workday cases in the electrical power industry. Lost workday means that an injury or illness warranted remaining home from work to recuperate or heal; such cases are more severe than minor needs such as first aid.

Workers at fossil-fueled power plants experience injuries and fatalities each year. There have been only a few attempts to analyze the safety of these power plant workers. Frezza (1978) described a number of case history events that included electrocutions, burns, boiler explosions, and power plant fires that resulted in injuries and fatalities. More recent work includes discussion of electrical linemen (Sahl 1997) and electric meter readers (Sahl 1998), who are also utility company employees. Loomis (1999) analyzed power plant worker fatalities. Electrical current (45%), homicide (18%), and falls (13%) were the three leading causes that accounted for 76% of the fatalities. The homicides were surprisingly high and include employee confrontations with citizens who are not lawfully purchasing electrical power, workplace violence, and from causes not directly related to employment.

In 2001, Yager (2001) noted that the statistical data on utility employee injuries and illnesses was only reported in summary form. Since then, the Electric Power Research Institute has developed a database to track specific information submitted by member utility companies. Thus far, only Kelsh (2004) and Fordyce (2007) have published analyses from these data. As the database is populated with more data from participating utility companies, it will become a valuable tool for worker safety assessment in the electrical energy sector. Total case rate reduction factors of 1.5 to 3 have been seen in recent years at the utility companies following the guidance produced from database analyses (Douglas 2008).

Table 5. US occupational injury and illness rates and fatality counts in the electric services industry

Year	Annual Average Employment Count	Injuries and Illnesses		Count of occupational fatalities in the electric power industry	Count of occupational fatalities at nuclear power plants
		Total case rate	Total Lost Workday Case Rates		
1994	416,800	5.7	2.6	35	0
1995	399,800	5.7	2.6	34	0
1996	381,300	5.1	2.4	29	0
1997	369,100	5.7	2.4	34	1
1998	363,300	5.1	2.4	27	1
1999	358,400	4.9	2.2	24	0
2000	359,300	4.8	2.4	28	0
2001	357,000	5.0	2.5	25	0
2002	354,000	3.7	2.0	35	1
2003	575,900	4.1	2.0	22	0
2004	563,900	4.5	2.1	40	0
2005	553,300	4.0	2.0	22	0
2006	547,400	3.8	1.9	39	0

Source: www.bls.gov for illnesses and injuries. Total cases and lost workday cases are per 100 workers per year. The www.bls.gov gave the census of fatal occupational injuries for the overall industry counts. Nuclear power fatalities were counted from the US NRC event notification database at www.nrc.gov – note that only occupational accident events, e.g., electrocution, were counted rather than personal heart attack events, so that the count could be compared to the industry-wide occupational fatalities data collected by the Bureau of Labor Statistics.

Cadwallader (2005) noted that the nuclear fission power plants, which produce on the order of 20% of the nation's electricity, employ, in rough figures, about 30–40% of the electrical utility workers. Nuclear power plant worker lost workday cases are on the order of 0.25 per 100 workers per year (Cadwallader 2007) and are much lower than the electric services total lost workday case values given in Table 5; those average about 2.0 in recent years. Therefore, injury rates in the other types of power plants are higher than in the fission power plants. From Table 5 it is also seen that occupational fatalities in fission power plants are rare. The industry-wide annual worker fatalities are also realized primarily from non-nuclear workers: linemen, electricians, painters, and other groups. As noted above, the roughly 100 nuclear fission plants in the US employ a fairly large percentage of the utility workforce, leaving smaller work crews at the fossil-fueled power plants, where the accident rates and annual fatality counts are the highest.

To date, annual lost workday injury rates at fusion experiments vary between 0.3 and 1.1 per 100 workers, and there have not been any work-related fatalities in fusion research experiment operations (Cadwallader 2007). No industrial fatalities are expected in ITER or NIF operations. A proposed worker safety goal for ITER is an annual value of 0.3 lost workday case rate per 100 workers, which is a low value compared to national standards (Cadwallader 2007). The 0.3 value is comparable to the safety performance of particle accelerators and existing fusion experiments and should be possible for ITER to meet. The longer-term goal for fusion power plants is to meet the intent of the Fusion Safety Standard (DOE 1996a), with the most comparable type of power plant being a nuclear fission reactor.

Noting the radiation safety analyses and use of occupational safety standards, combined with the use of 'lessons learned' from the present generation of machines, ITER and NIF personnel safety is being treated.

6. Environmental Issues

6.1. Radwaste Management

Fusion offers salient safety advantages relative to other sources of energy, but generates a sizable amount of mildly radioactive materials that tend to rapidly fill the low-level waste repositories. Since the early 1970s, the majority of fusion power plant designs have focused on the disposal of radwaste in geological repositories, adopting the preferred fission radwaste management approach of the 1960s. The large volume of radioactive materials that will be generated during fusion plant operation and after decommissioning suggests developing a new framework that takes into account the environmental, political, and present reality in the US and abroad. At present, many US utilities store their radwaste onsite due to the limited and/or expensive offsite disposal option. Because of the limited capacity of existing repositories and the political difficulty of constructing new repositories worldwide, managing the continual stream of radioactive fusion materials cannot be relegated to the back-end as only a disposal issue. Concerns about the environment, radwaste burden for future generations, lack of geological repositories, and high disposal cost directed the attention of many fusion designers to seriously consider more environmentally attractive scenarios, such as:

- Recycling and reuse within the nuclear industry
- Clearance or release to the commercial market, if materials contain traces of radioactivity.

The recycling and clearance options have been investigated by many fusion researchers in the 1980s and 1990s, first focusing on selected materials or components (Baker 1980, Ponti 1988, Cheng 1992, Butterworth 1992, Dolan 1994, Butterworth 1998, Cheng 2000), then examining almost all fusion components in the late 1990s and 2000s (Rocco 1998, Cheng 1998, Rocco 2000, Petti 2000, El-Guebaly 2001, Asaoka 2001, Zucchetti 2001, Tobita 2004, Bartenev 2007, El-Guebaly 2007a). Recycling and clearance became more technically feasible during the 2000s with the development of advanced radiation-resistant remote handling tools that can recycle highly irradiated materials (up to 10,000 Sv/h) and with the introduction of the 2003/2004 clearance category for slightly radioactive materials by the US (NRC 2003), IAEA (IAEA 2004), and other agencies. Encouraged by such advancements, El-Guebaly applied the recycling and clearance approaches to all in-vessel and out-vessel components of the most recent ARIES design that are subject to extreme radiation levels: very high levels near the plasma and very low levels at the bioshield (El-Guebaly 2007a). In addition, the technical elements supporting the future management of fusion radioactive materials were identified along with a list of critical issues to be addressed by a dedicated R&D program, as well as the policy and regulatory concerns for all three options: recycling, clearance, and disposal (El-Guebaly 2008, Zucchetti 2009). Several tasks were also

examined, including the key issues and challenges for each option, the notable discrepancies between the US and IAEA clearance standards (El-Guebaly 2006a), the need for new clearance guidelines for fusion-specific radioisotopes, the structural properties of recycled materials, the need to address the economic aspect before recycling specific components (El-Guebaly 2004), the must requirement of detritiation of fusion components before recycling and/or disposal, the availability of radiation resistant remote handling equipment (El-Guebaly 2007a, Zucchetti 2007), the need for accurate measurements and reduction of impurities that deter the clearance of in-vessel components, the acceptability of the nuclear industry to recyclable/clearable materials, and the status of the worldwide recycling/clearance infrastructure and commercial market (El-Guebaly 2006a, Zucchetti 2009).

All fusion power plants will generate only low-level waste (LLW) that requires near-surface, shallow-land burial as all fusion materials are carefully chosen to minimize the long-lived radioactive products (Steiner 1997, Khater 2003, Petti 2006, El-Guebaly 2007a, Merrill 2008). The vacuum vessel and externals are less radioactive than the in-vessel components, to the extent that they qualify as Class A LLW, the least hazardous type of radwaste according to US classifications (NRC 1982). All fusion components can potentially be recycled using conventional and advanced equipment that can handle 0.01 Sv/h and high doses of 10,000 Sv/h, respectively (El-Guebaly 2007a). Storing the highly irradiated components (blanket and divertor) for several years helps reduce the dose by a few orders of magnitude before recycling. Even though recycling seemed technically feasible and judged, in many cases, a must requirement to control the radwaste stream, the disposal scheme emerged as the preferred option for specific components (target materials of inertial fusion) for economic reasons (El-Guebaly 2004).

The clearance indices for all internal components (blanket, shield, manifolds, and vacuum vessel) exceed unity by a wide margin even after an extended period of 100 y (El-Guebaly 2001). ^{94}Nb is the main contributor to the clearance index of steel-based components after 100 y. Controlling the Nb and Mo impurities in the steel structure helps the clearance index (CI) approach unity for some sizable components. In the absence of impurity control, all in-vessel components should preferably be recycled or disposed of in repositories as LLW. The magnet constituents can be cleared within 100 y, except the Nb_3Sn conductor and polyimide insulator (El-Guebaly 2007b). The 2 m thick external concrete building (bioshield) that surrounds the torus represents the largest single component of the decommissioned radwaste. Fortunately, it is clearable within a few years after shutdown (El-Guebaly 2007b, El-Guebaly 2008). Overall, 70-80% of all fusion radioactive materials, including the bioshield, can be cleared within 100 y after decommissioning, the remaining 20-30% of materials are recyclable, and a minute amount of reprocessing secondary waste may require disposal as LLW.

Advanced fuel cycles offer clear advantages and could be the ultimate response to the safety and environmental requirements for future fusion power plants. The in-vessel components of any $\text{D}-^3\text{He}$ fuelled power plant can easily qualify as Class A LLW and can also be recycled using conventional and advanced remote handling equipment. As for any D-T system, the bioshield contains traces of radioactivity and is clearable from regulatory control in ~10 years after decommissioning (El-Guebaly 2007c).

The integration of the recycling and clearance processes in fusion power plants is at an early stage of development. It is almost impossible to state how long it will take to refabricate components out of radioactive materials. The minimum time that one can expect is one year

temporary storage and two years for fabrication, assembly, inspection, and testing. All processes must be done remotely with no personnel access to fabrication facilities. Figure 5 depicts the essential elements of the recycling/clearance process. Examining the various steps, one could envision the following (El-Guebaly 2008):

1. After extraction from the power core, components are taken to the Hot Cell to disassemble and remove any parts that will be reused, separate into like materials, detritiate, and consolidate into a condensed form. This is one of the most challenging steps.
2. Ship materials to a temporary onsite or centralized facility to store for a period of ~1 year or less.
3. If the CI does not reach unity in less than e.g. 100 y, transfer the materials to a recycling center to refabricate remotely into useful forms. Fresh supply of materials could be added as needed.
4. If the CI can reach unity in less than e.g. 100 y, store the materials for 1-100 y then release to the public sector to reuse without restriction.

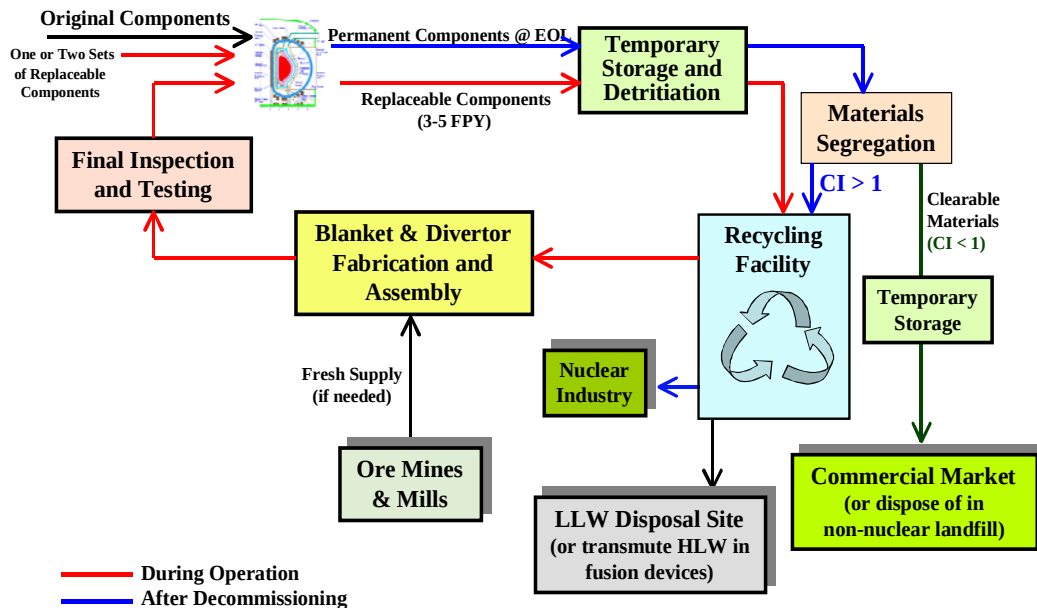


Figure 5. Diagram of recycling and clearance processes.

Clearly, proper handling of fusion radioactive materials is important to the future of fusion energy. The recycling/clearance approach solves fusion's large radwaste problem, frees ample space in repositories for non-fusion non-recyclable radwaste, preserves natural resources, minimizes the radwaste burden for future generations, and promotes fusion as an energy source with minimal environmental impact. At present, the experience with recycling/clearance is limited, but will be augmented significantly by advances in fission spent fuel reprocessing (that deals with highly radioactive materials), fission reactor dismantling, and bioshield clearing before fusion is committed to commercialization in the 21st century and beyond. We are forecasting advanced recycling techniques some 50-100 y in

the future based on current accomplishments and near term developments within the fission industry in support of the Advanced Fuel Cycle Initiative (AFCI), Mixed Oxide (MOX) fuel fabrication, and Partitioning and Transmutation activities. In addition, the US is currently developing guidelines for the unconditional release of clearable materials from fission reactors that could be valuable for fusion. While recycling/clearance is a tense, contentious political situation, there has been some progress. For instance, limited scale recycling within the nuclear industry has been proven feasible in Europe and at several US national laboratories. A clearance market currently exists in Germany, Spain, Sweden, Belgium, and other countries in Europe. In the US, the free release has been performed only on a case-by-case basis during decommissioning projects since the 1990s.

The worldwide fusion development strategy should be set up to accommodate this new radwaste management trend. A dedicated R&D program should optimize the proposed radwaste management scheme further and address the critical issues identified for each option. Seeking a bright future for fusion, the following general recommendations are essential for making sound decisions to restructure the framework of handling fusion radioactive materials (El-Guebaly 2008):

- Fusion designers should:
 - Minimize radwaste volume by clever designs
 - Promote environmentally attractive scenarios such as recycling and clearance, avoiding geological disposal
 - Continue addressing critical issues for all three options
 - Continue developing low-activation materials (specifications could be relaxed for some impurities while more stringent specs will be imposed on others to maximize clearance)
 - Accurately measure and reduce impurities that deter clearance of in-vessel components
 - Address technical and economical aspects before selecting the most suitable radwaste management approach for any fusion component.
- Nuclear industry and regulatory organizations should:
 - Accept recycled materials from dismantled nuclear facilities
 - Continue national and international efforts to convince industrial and environmental groups that clearance can be conducted safely with no risk to public health
 - Continue developing advanced radiation-resistant remote handling equipment capable of handling $> 10,000$ Sv/h that can be adapted for fusion use
 - Consider fusion-specific and advanced nuclear materials and issue official guidelines for unconditional release of clearable materials.

The outcome of the recent radwaste assessment will impact the mission of the Demo device to be built after ITER. All members of the US Fusion Energy Sciences Advisory Committee (FESAC) strongly support the recycling and clearance options. The 2007 FESAC report (Greenwald 2007) states “Beyond the need to avoid the production of high-level waste, there is a need to establish a more complete waste management strategy that examines all the types of waste anticipated for Demo and the anticipated more restricted regulatory environment for disposal of radioactive material. Demo designs should consider recycle and

reuse as much as possible. Development of suitable waste reduction recycling and clearance strategies is required for the expected quantities of power plant relevant materials. Of particular concern over the longer term could also be the need to detritiate some of the waste prior to disposal to prevent tritium from eventually reaching underground water sources. This may require special facilities for the large anticipated fusion components. The fission industry will be developing recycling techniques for the Global Nuclear Energy Partnership (GNEP) and the US Nuclear Regulatory Commission (NRC) is developing guidelines for the release of clearable materials from fission reactor wastes both of which may be of value to fusion.”

6.2. Thermal Pollution

Thermal pollution affects creatures living in nature, wildlife as well as plant life. The heat (or lack thereof) in natural water is always an important parameter of any natural water system. Heat influences all biologic activity – fish and shellfish reproduction and lifetime, and photosynthesis, eutrophication, and degradation of organic materials. This is why all thermal power plants, such as coal, oil, natural gas, nuclear, or any other heat-producing plant, have limits on heat rejection to the environment. The EPA has regulations for once-through water cooling of power plant condensers in 40CFR125 Subparts I and J. The basic rule is not quantitative; instead, each plant at each location must determine what amount of water heating will not significantly affect fish, shellfish, or wildlife in the area as discussed in Section 316 of the Clean Water Act (EPA 2008). Individual states decide what is a tolerable level of heat release at the plant location. Consider a 3000 MW thermal power plant with a $\approx 33\%$ plant thermal efficiency, such as a fission power plant. This plant rejects 2000 MW thermal from its condenser to the environment. If the cooling water from a lake, river, or ocean is flowing on the order of $50 \text{ m}^3/\text{second}$, then the cooling water temperature increase is about 10°C (El-Hinnawi 1982). A coal-fired power plant might have perhaps a 9°C temperature increase (ANL 1990). Eventually the warmed water gives up its heat to the atmosphere. Eichholz (1976) gives good discussions of all thermal-related phenomena, including beneficial uses of warm water for fish hatcheries, etc. An important aspect is that the temperature difference of the thermal discharge must be kept small enough so that if the power plant goes off-line for any reason the marine life acclimatized to the higher temperature are not “cold shocked” by a sudden decrease in heat rejection from the plant (Glasstone 1980). Some power plants use a closed system of cooling water that routes to a cooling tower for discharge to the atmosphere, thus avoiding the issue of heat rejection to a body of water. Of the U.S. nuclear power plants, slightly less than half use a closed cooling water system with a cooling tower (Lobner 1990). The ITER facility will reject $\approx 400 \text{ MW}$ thermal when operating and will use a cooling tower (IAEA 2002). The initial fusion power plants using a steam cycle for electricity production would be regulated for waste heat like any other thermal power plant.

The direct conversion of fusion charged particle energy into electric power would eliminate the costly and thermo-dynamically inefficient “balance of plant” portion of a thermal power plant: the steam piping, the steam turbine and electrical generator, the steam condenser and feedwater heaters. Direct conversion ideas for advanced fuel cycles ($\text{D-}^3\text{He}$, $\text{P-}^{11}\text{B}$, $^3\text{He-}^3\text{He}$) with less neutron emission have been described by Miley (1970) and Santarius (1995). Achieving direct conversion with efficiency exceeding 70% would

eliminate much of the thermal pollution from the power plant. The first fusion power plant in the 21st century is still envisioned to be a thermal power plant that uses steam or gas, a turbine, and a generator for energy conversion to electricity. In the more distant future, fusion power plants using advanced fuels and direct conversion of charged particle energy into electrical power would release only small amounts of waste heat to the environment.

6.3. Other Environmental Concerns

Present fusion experiments pose very little hazard to the environment (DOE 1995a, DOE 1995b, Ono 1999). As fusion research progresses toward construction and operation of a demonstration power plant, the demonstration facility is expected to have many of the same environmental concerns as other thermal power plants. Several of the shared concerns are airborne and liquid radiological releases, and chemical effluent releases. Fusion-specific concerns are magnetic field and radiofrequency energy releases to the environment. The U.S. Environmental Protection Agency (EPA) has regulations for some of these concerns. State governments and local governments also have laws for various environmental effluents so the benefits of electrical power can be enjoyed while minimizing harm to the environment.

6.3.1. Gaseous and Liquid Effluent Releases

Each year, nuclear power plants release small amounts of radioactive gaseous and liquid effluent wastes to the environment. The U.S. Nuclear Regulatory Commission collects and reviews these data for regulatory compliance. The release data from earlier years were presented in a series of reports (Tichler 1995); more recent reports are available at a website (www.reirs.com/effluent/). Harris (2004) has presented a trend analysis of these data and the trend is fairly constant over the reporting period from 1994 through 2001. Most fission power plants release short-lived radioactive gases, such as activated air, at the rate of a few hundred curies per year. Holdup of these gases on charcoal filters and in tanks or delay lines allows time for radioactive decay to low levels so the releases do not pose a hazard to the public or the environment. Cottrell (1974) gives a good description of techniques for air filtration and holdup that are used in gaseous radwaste systems. NIF will release on the order of 26 Ci of Ar-41 and about 150 Ci of the short-lived N-16 (Brereton 2003). ITER will release ≈ 27 Ci of Argon-41 and 0.27 mCi of Carbon-14 each year (IAEA 2002). Fusion power plants will also have some activated air, probably between ITER levels and fission power plant levels.

Tritium, in either gas or water form, is the largest concern for gas or liquid releases from fusion plants. Nuclear fission power plants create a small amount of tritium either as a fission product or as the result of neutron reactions, such as with boron. Much of the tritium created in fission power plants enters into water and becomes liquid. Tichler (1995) showed that most fission power plants release approximately 10–100 Ci (0.001–0.01 g) of tritium as gas per year. This is a small fraction of the 10 mrem annual airborne dose allowed at the plant site boundary. Tichler also showed that fission plants release about 200–23,000 Ci (0.02–2.3 g) of tritium in liquid effluent each year. As noted earlier in this chapter, there are limits for tritium concentrations in drinking water, and these releases are well within the safe limits.

A design goal for an ARIES power plant is to keep the tritium losses to the environment below 4 g/y (El-Guebaly 2009). The ITER project has design provisions to capture as much tritium as possible so that very little is released to the environment (IAEA 2002). Cristescu (2007) describes the ITER project's tritium effluent limits of 10,000 Ci/yr (1 g/yr) of tritium gas and 1,000 Ci/yr (0.1 g/yr) of tritiated water; the ITER estimated annual releases are 0.05 g tritiated water vapor and 0.18 g tritium gas in air and 0.0004 g of tritium in water (IAEA 2002). Note that an annual release of 8 grams of tritiated water vapor from a plant stack would result in a 10 mrem annual dose to any member of the public at the site boundary (Cadwallader 2003). The waste water detritiation system is designed to accept water with tritium concentrations of 1 to 10 Ci/kg and use electrolysis to convert the water into gas and then scavenge the tritium from the hydrogen and oxygen gases. The tritium concentration in the hydrogen gas that is released to the environment is less than 1.9 mCi/m³ of gas, and the tritiated water vapor concentration in the water vapor discharged to the air is less than 0.1 mCi/m³ (Iwai 2002). The NIF project will have a tritium administrative inventory of 500 Curies of tritium on site and routine emissions of tritium are expected to be on the order of 30 Curies per year (0.003 g/y) (Brereton 2003).

6.3.2. Chemical Releases

Existing power plants release some non-radioactive chemicals each year. ANL (1990) and Eichholz (1976) describe some of the chemicals used in power plants: sulfuric acid for regenerating demineralizer resin beds that clean the plant coolant water, sulfuric acid and ammonia for cooling water pH control, hydrazine for free hydrogen control in the coolant water, and corrosion inhibitors, biocides, detergents, etc. A steam-electric fusion power plant would use similar chemicals. A fusion power plant would also have some unique chemical releases. One of the larger foreseen releases would be boiloff gases from the cryogenic plant (helium and nitrogen). The present estimate for ITER is 1 to 3 metric tons of helium gas released each year (IAEA 2002), which is considered to be insignificant and no public hazard. Calculations have shown that even very cold helium gas will rise and disperse in ambient air, and very cold nitrogen will initially seek low areas but as it warms in the ambient air it rises and disperses as well (Abbott 1994). The ITER facility will use beryllium as a coating on the fusion chamber walls to obtain the benefits of beryllium as a plasma facing material. ITER will also release small amounts of particulate beryllium, on the order of 0.1 gram/year (IAEA 2002). That small amount does not pose a hazard; the US emission standard for airborne beryllium in 40CFR61.32 is up to 10 grams/day. Reyes (2003) noted that some potential materials to encapsulate the inertial fusion D-T targets pose a chemical hazard if released. The results of that study showed that due to the decreasing public exposure concentrations to the chemical elements mercury and lead, the toxicological exposure was a greater concern than the radiological exposure. In most cases the reverse is true. If allowable chemical exposure concentrations continue to decrease to protect the public from such highly hazardous chemicals, then chemical release analyses will become more important for protection of the public and the environment (Cadwallader 2003a).

If a plant uses a cooling tower, there may be steam releases from the tower, but many types of thermal power plants currently release steam and it is not found to be a threat even if the steam entrains impurities in the form of chemistry control chemicals. Like other power plants, a fusion plant will have waste oil and grease from lubricating the traditional

components and possibly electrical insulating oil or gas (e.g., sulfur hexafluoride) releases from electrical equipment. The routine releases from the NIF experiment are incidental ozone and nitrogen oxides created by operation of the spark gap switches in the laser systems, and some volatile organic solvents for optical lens cleaning (Brereton 2003). These chemical releases are managed by best industrial practices and release levels are regulated by the EPA. None of these types of chemical releases in these quantities are considered to be a present or future threat to the public.

6.3.3. Electromagnetic Energy Emanations

Fusion power plants will use high field magnets and radiofrequency heating for the plasma. There are exposure limits to these forms of energy. IEEE (2007) gives general public maximum permissible magnetic field exposure levels for the head/torso of 118 milliTesla (mT), and 353 mT for the arms and legs. The constant magnetic fields of fusion magnets, such as the toroidal field coils, are highest in the magnet bore. There are several factors to decrease the field strength, primarily distance. As one moves away from the magnet, the field strength decreases as $1/\text{distance}^3$ (Thome 1982). Away from the magnets but inside the ITER building, the peak value of the magnetic field is 70 mT (Benfatto 2005). The ITER magnetic fields have been calculated to decrease to less than 0.02 mT at 250 m from the plant building. This 0.02 mT value is less than the earth's magnetic field strength, which varies between 0.025 and 0.065 mT (IAEA 2002). Using 0.04 mT as the earth's field at Cadarache, then the total magnetic field strength at 250 m from the ITER building would be the earth's horizontal field and the vertical ITER field summed. Summing vectors is performed by taking the square root of $[(0.02 \text{ mT})^2 + (0.04 \text{ mT})^2]$, which gives 0.045 mT. This magnetic field value is more than three orders of magnitude below the IEEE suggested maximum exposure value of 118 mT. The public exposure to magnetic fields from ITER is well below the IEEE recommended levels. Fusion power plants, whose magnets will not be much more powerful than ITER magnets, are expected to have similar results.

Fusion power plants also use electron cyclotron heating in the microwave energy region at nominally 100 GHz and ion cyclotron heating in the radio wave region around 100 MHz. It is possible that these electromagnetic energies could leak at the generation source or from transmission lines that route the energy to the vacuum vessel. The IEEE Standard C95.1 (IEEE 2005) gives general public maximum permissible exposure (MPE) levels for electromagnetic fields from 3 kHz to 300 GHz. For the ion cyclotron ≈ 100 MHz energy, the basic restriction or action level for public exposure is 80 mW/kg of body weight. For electron cyclotron ≈ 100 GHz energy, the maximum permissible exposure level is found by a formula, $(90 \times \text{frequency in GHz} - 7000)/200$. For the 100 GHz case, the MPE power density value is 10 W/m^2 . Readings taken from the plasma heating systems of existing fusion experiments show that system energy leakage gives worker exposures on the order of 3.5 mW/kg for ion cyclotron and 0.03 mW/kg for electron cyclotron (Wang 2005). The public, being much further from the equipment, would not receive any tangible exposure. Therefore, there is no expected public exposure to radiofrequency heating energy from ITER or a fusion power plant.

7. Conclusion

The promise of fusion power has always been that high technology can sustain modern society without harm to the public, workers, or the environment. We outlined the role of fusion in tackling such pertinent safety and environmental issues and demonstrated the positive impact fusion can make worldwide:

- Public and worker safety that protects individuals and society, ensures hazardous material from the premises is controlled, minimized, and kept below allowable limits, and demonstrates the consequences of frequent events, if any, are minor, the likelihood of accidents is small, and their consequences are bounded, needing no evacuation plan.
- Radwaste reduction schemes that greatly reduce the volume of mildly radioactive materials requiring geological disposal. Supported by clever designs and smart choice of low-activation materials, recycling and clearance are the most environmentally attractive solution toward the goal of radwaste-free fusion energy.

Harnessing the nuclear fusion process to use on the earth is difficult, and the fusion power plants, envisioned to utilize the same power as the core of a sun, are challenging and complicated to build. Nevertheless, the benefits of bountiful power with fuel available to all countries, electric power that is at least as safe as existing plants, and power that has lower environmental impact than existing technologies, are believed to greatly outweigh the challenges and costs of fusion research.

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Chapter 10

LEAK DETECTION TECHNOLOGY

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Abstract

In large tokamak machines such as the TFTR, the JET and the JT-60, a high quality of vacuum integrity is required to achieve impurity-free plasma, so the vacuum leak detection technology is highly important. A combination of a helium (^4He) spraying and a ^4He leak detector is a conventional leak detection method due to its higher detectability, reliability and easier maintainability. However, such tokamak machines are huge, complicated, and inaccessible, so the conventional method is not applicable in many cases. Special leak detection methods were therefore developed; for instance, a sensitivity of the ^4He sniffer method was improved five decades in JT-60, and a lot of experiences on leak events, pinpointing and repairs were accumulated in individual machines. Through these tokamak operations, the nuclear fusion research has considerably advanced, which created the next step for international collaboration — the ITER project. Cadarache in France was eventually selected for the ITER construction site and now, the ITER organization comprising the seven parties is effective for executing the scheduled missions. In this chapter, the ITER leak detection system is given an overview, and a reasonable leak detection strategy is proposed, based on the methods and concepts developed so far. To support the strategy, the essential leak detection technologies are exhibited. The high-resolution quadrupole mass spectrometer (HR-QMS) using the zone II condition in the Mathieu diagram has been developed for the mass spectrometer leak detector (MSLD) for torus, where the 10^{-4} peak ratio of $^4\text{He}^+/\text{D}_2^+$ could be obtained with the 3.58 MHz prototype. An improved method with fully circulating the water has been demonstrated, in which krypton (Kr) was dissolved in water. For 10^{-3} Pa·m³/s of two water leaks, the positional accuracy of 12.6 % was obtained, based on the natural convection flow calculation. Another valuable method with fully circulating the water has been developed, namely, a total pressure gauge or the QMS head was mechanically scanned around the suspected area by using a vacuum compatible manipulator. A unidirectional total pressure gauge could detect the minimum 10^{-8} Pa·m³/s level of air leaks at background

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pressures of 10^{-5} Pa. A special QMS head equipped with a vacuum-tight rf tuning package operable at more than 150 °C was moved around the argon (Ar) leak nozzle, and could detect the Ar leak of 1.9×10^{-5} Pa·m³/s. A noticeable progress has been made in the leak detection technology as mentioned above, and they will be experimentally tested in the ITER.

1. Introduction

In fusion machines, a high quality of vacuum integrity is required to achieve impurity-free plasma, so a vacuum leak detection technology is highly important. In the 1970s–1980s, large tokamak machines such as the Tokamak Fusion Test Reactor (TFTR) [1], the Joint European Torus (JET) [2] and the JAERI Tokamak-60 (JT-60) [3] were successively constructed and initiated operations. In 1991, the JET conducted a limited series of tritium experiments [4], and the TFTR followed an extensive tritium operation in 1993 [5], so that from a safety perspective, the leak detection is increasingly important to secure the integrity of the radiologic containment. The combination of a helium (⁴He) spraying and a ⁴He leak detector is a conventional leak detection method due to its higher detectability, reliability and easier maintainability [6]. However, such tokamak machines are huge, complicated, and inaccessible, so the conventional method is not applicable in many cases. Special leak detection methods were therefore developed; for instance, a lower detectable limit of a ⁴He sniffer method was improved from 10^{-6} to 10^{-11} Pa·m³/s in JT-60, where a liquid nitrogen (N₂) cooled sorption pump was used for the selective pumping of air. The sniffed ⁴He tracer into the leak detector increased drastically and this method was applied to the magnetic limiter coil cans and weld joints [7]. A combination of the sorption pump and the ⁴He leak detector can be replaced to a counterflow ⁴He leak detector [8]. Inner skin leaks of double walls in the DIII-D tokamak could be located with the ⁴He entrainment techniques, where a viscous N₂ flow was introduced to the double-wall channels and the outlet was connected to a counterflow ⁴He leak detector. The ⁴He was sprayed from the inner vacuum vessel [9]. In addition, a lot of experiences on leak events, pinpointing and repairs were accumulated as know-hows in individual machines [10], [11].

Through these large tokamak and the other machine operations, considerable advances have been made in nuclear fusion research, which created the next step for international collaboration. In 1988–1990, the Conceptual Design Activity (CDA) of the International Thermonuclear Experimental Reactor (ITER) was implemented among Japan, the EU, Russia and the USA for aiming to develop the concept of an experimental fusion reactor to demonstrate the scientific and technological feasibility of fusion power [12]. The Engineering Design Activity (EDA) was subsequently conducted from 1992 [13] and came to fruition with the issue of the Final Design Report in 2001 [14]. Following a protracted period of negotiation, the CEA Research Centre in Cadarache, France, was eventually selected for the ITER construction site in 2005, and now, the ITER organization comprising the seven parties is effective for executing the scheduled missions to ignite a first plasma in 2016 [15].

In this chapter, the ITER leak detection system is given an overview, and a reasonable leak detection strategy is proposed, based on the methods and concepts developed so far. To support the strategy, the essential leak detection technologies are exhibited. The ITER leak detection system is a part of the vacuum pumping system, so during the CDA and EDA, a primary effort was devoted to selecting and designing the torus vacuum pumping system.

Although for the leak detection system using ^4He as a tracer, a mechanical pump-based torus vacuum pumping system [16] is desirable because of its continuous pump capacity, a cryosorption pump-based torus vacuum pumping system was finally selected due mainly to a limited pumping space near the plasma [17], and R&Ds on the prototype cryosorption pump have been aggressively implemented for construction [18]. The ITER leak detection system was therefore designed to be compatible with the cryosorption pump [19], where half of the torus cryosorption pumps was warmed to $>30\text{ K}$ to suppress ^4He pumping action therebetween spraying ^4He tracer to the leak suspected object. Once the ^4He build-up by leaks was observed, the accumulated ^4He was promptly evacuated by the rest of the 4.5 K cryosorption pumps. A mass spectrometer leak detector (MSLD) for torus of the leak detection system has to detect a small amount of ^4He in hydrogen isotope atmospheres, so the developed high-resolution quadrupole mass spectrometer (HR-QMS) separable ^4He (4.0026 amu) from D_2 (4.0282 amu) is mentioned.

Although for torus leak detection of in-vessel water cooling channels, a drain and dry out scheme of the water circuits [10] and resultant ^4He leak testing are a base design concept, the drain and dry process is impossible in some cases due to aspects such as the nuclear heating; in this instance, an in-situ leak detection that fully circulates the water will be implemented. The water leak detection using water-soluble gas as a tracer has been developed for roughly localizing the leak, where krypton (Kr) is dissolved into the water. The leak can roughly be localized by evaluating a time delay from the injection of the tracer-dissolved water until the actual detection of the tracer by using the QMS. Leak detection by remote handling is proposed for detecting the water molecules using an in-vacuum movable, total pressure gauge and the QMS head. The TFTR designed and tested a maintenance manipulator operable in $<10^{-6}\text{ Pa}$, to minimize personnel radiation [20]. Also in ITER, the vacuum manipulator for inspection use has been developed [21].

2. Overview of ITER Leak Detection System

The ITER leak detection system is a part of the vacuum pumping system for the torus pumping, cryostat pumping, and other vacuum equipment pumping. The leak detection system overviewed in this section is based on the 1999 ITER design parameters [19]. However since then, the ITER parameters have been updated for construction, where major revised parameters concerning the torus leak detection are, a torus free volume of $\sim 2000\text{ m}^3$ ($\sim 4600\text{ m}^3$ in 1999 design) and a target effective pumping speed of $>60\text{ m}^3/\text{s}$ (ibid $200\text{ m}^3/\text{s}$) [18]. Hence, key design issues and reasonable proposals on a leak detection strategy based on the technological progress in this area are described here.

The ITER leak detection system can be categorized into five subsystems for torus, neutral beam/diagnostic neutral beam (NB/DNB) injector, cryostat, service vacuum and electron cyclotron heating & current drive (EC H&CD), as shown in Table 1, where each integrated leak rate and the number of each unit are listed. Figure 1 shows an overall flow diagram of the leak detection system in which the mass spectrometer leak detectors (MSLDs), residual gas analyzer (RGA) units and calibration units listed in Table 1 are indicated. The RGA unit and MSLD gas sensors are basically QMSs with magnetic shields, which are extensively used in current operating fusion machines. There are two types of cryopumps, i.e. cryopanel with and without charcoal sorbents (see Note in Fig. 1). For helium pumping, charcoal-coated

cryopumps are used. The MSLDs are connected to roughing lines, cryopump forelines and the service vacuum leak detection manifold (SVLDM) lines routed to roughing pumps (RPs) and installed in the vacuum pump room. The MSLD of the EC H&CD system is hooked to the auxiliary pump comprised of a turbomolecular pump and a dry scroll pump, which is placed in the EC H&CD building.

The integrated leak rate of the torus is less than 10^{-7} Pa·m³/s, and allocated to the various in-vessel components that make up the vacuum boundary, i. e. 10^{-8} Pa·m³/s each for vacuum vessel, divertor and blanket. The MSLD type 1 should detect ⁴He tracer in hydrogen isotopes. Another MSLD type 1 is used for leak checking in the NB/DNB injectors, and as a backup for the torus MSLD. Vacuum quality of the torus is monitored by the RGA unit type 1. The torus leak detection subsystem will be used to test all components that form part of the primary vacuum boundary. The MSLD type 1 will be connected to the torus both through the cryopump foreline at the divertor port level and the torus roughing line at the equatorial port level, upstream of the primary roughing pumps in the vacuum pump room.

The MSLD type 1 is equipped with gross and fine leak test modes, which use counterflow and directflow methods, respectively. In the fine leak test mode, the MSLD type 1 has to detect a minimal 10^{-10} Pa·m³/s of ⁴He leakage in a background hydrogen isotope flow of 5×10^{-3} Pa·m³/s. In addition, in-vessel residual tritium produces ³He by β -decay at the maximum rate of approximately 10^{-3} Pa·m³/s, which will add to ⁴He. The primary cryopumps are used for the torus pumping. These cryopumps have 4.5 K cryopanel which are coated with activated charcoal. In order to leak test with ⁴He as a tracer, the half cryopanel of the primary cryopumps are warmed up to 30~80 K to suppress ⁴He pumping while hydrogen isotopes are pumped [22]. The remaining cryopumps are maintained at 4.5 K and the inlet valves are opened to quickly clean-up residual ⁴He.

Table 1. List of the overall leak detection system

Leak detection subsystem	Integrated leak rate (Pa·m ³ /s)	Unit	Quantity
Torus	<1x10 ⁻⁷	MSLD type 1	1
		RGA unit type 1	5
		RGA unit type 4	3
		Calibration unit type 1	1
NB/DNB injector	<1x10 ⁻⁸	MSLD type 1	1
Cryostat	Before operation: <1x10 ⁻⁵	MSLD type 2	1
	During operation: <0.1 (Air)	RGA unit type 2	1
	<1x10 ⁻³ (Helium)	Calibration unit type 2	1
Service Vacuum	<1x10 ⁻⁸ for each component	MSLD type 3	3
		RGA unit type 3	12
		Calibration unit type 2	3
EC H&CD	<1x10 ⁻⁸	MSLD type 3	2
		Calibration unit type 2	2

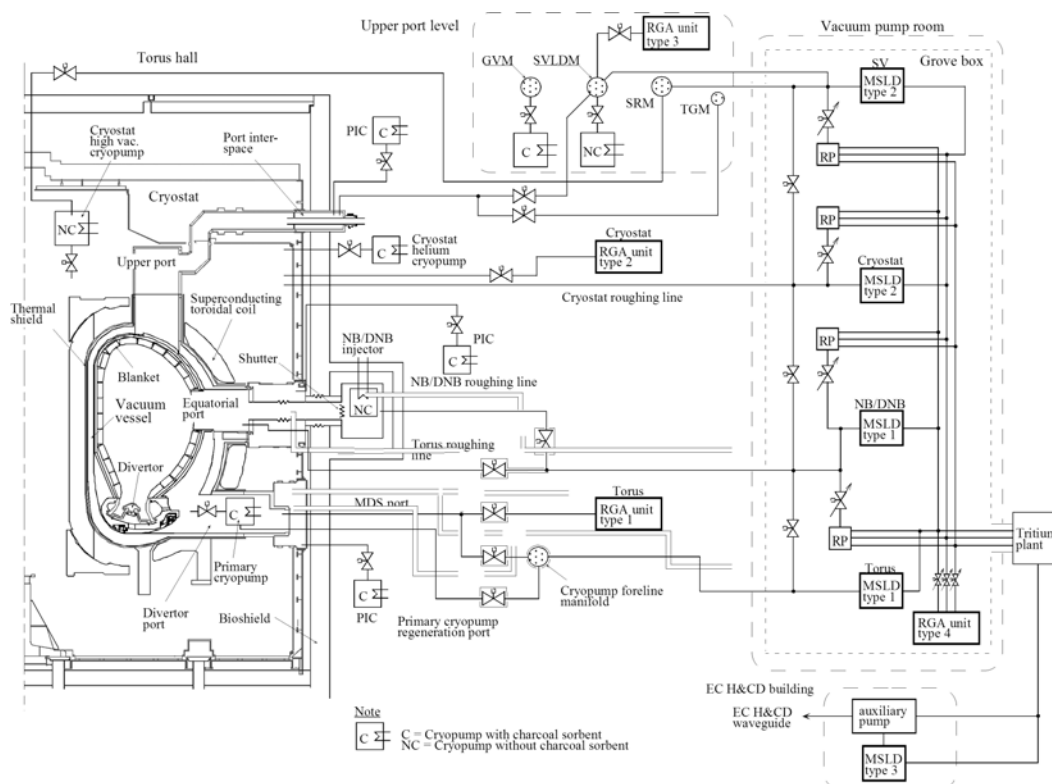


Figure 1. Overall flow diagram of ITER leak detection system. MSLD: Mass Spectrometer Leak Detector, RGA: Residual Gas Analyzer, RP: Roughing Pump, SRM: Service Roughing Manifold, GVM: Guard Vacuum Manifold, SVLDM: Service Vacuum Leak Detection Manifold, TGM: Tracer Gas Manifold, PIC: Port Interspace Cryopump, MDS: Maintenance Detritiation System.

The HR-QMS described in the next section, Sec. 3, with a closed ion source provides a slightly higher operating pressure by a few Pa, which is suitable for the MSLD type 1. Although 30~80 K primary cryopumps in the torus will evacuate hydrogen isotopes, a large amount of hydrogen isotopes are still adsorbed on inner surfaces of the roughing and maintenance detritiation system (MDS) lines, so these should be eliminated by using the 10 K, because of its easy maintenance. ^4He is further separated from hydrogen isotopes and ^3He in the HR-QMS. The exhaust gas is compressed to atmospheric pressure by a dry scroll pump and transferred to the tritium plant.

Four RGA type 1 units are connected to the MDS ports at ~90 degrees apart for toroidal direction, and one RGA type 1 unit is connected to the torus roughing line. The MDS ports and the cryopump regeneration ports are connected to the cryopump foreline ring manifold. The RGA unit type 1 will monitor the vacuum quality in the primary vacuum vessel not only during tokamak discharges but also in maintenance high vacuum pumping, ranging from 10 to 10^{-10} Pa. A recently developed miniature RGA can even work maximally at 10 Pa without differential pumping, although the minimum detection level is rather poor at 10^{-7} Pa in a Faraday cup detector. The miniature RGA is very attractive for magnetic shielding protecting against fields of 0.3 T because the sensor head occupies only about 15 mm in diameter and 25 mm in length. To cover the remaining $10^{-7}\sim 10^{-10}$ Pa, a conventional QMS with a Faraday cup

detector is also attached to the RGA unit type 1. The magnetic shield enclosures will ensure a double containment for tritium confinement, with a combination of permalloy and mild steels.

Three integrated leak rates for the cryostat are defined as shown in Table 1. Prior to commencement of operation, the integrated leak rate should be less than $10^{-5} \text{ Pa}\cdot\text{m}^3/\text{s}$. During operation with coils cooled to cryogenic temperatures, leaks from cryogenic ^4He channels of less than $10^{-3} \text{ Pa}\cdot\text{m}^3/\text{s}$ will be unavoidable. Charcoal coated cryopumps will be used to keep the cryostat ^4He base pressure less than 10^{-4} Pa . When the coils are cooled to cryogenic temperatures, a pumping speed of the coil enclosures is very large, roughly estimated at $10^5 \sim 10^6 \text{ m}^3/\text{s}$. Thus smaller air leakages of the initial $10^{-5} \text{ Pa}\cdot\text{m}^3/\text{s}$ levels are hard to detect. So another integrated air leak rate of less than $0.1 \text{ Pa}\cdot\text{m}^3/\text{s}$ during operation has been defined, which was determined by the maximum tolerable ozone production rate. The excessive cryostat air and ^4He leaks can be monitored by the RGA unit type 2.

The service vacuum (SV) leak detection subsystem targets miscellaneous vacuum enclosures which are connected to a guard vacuum manifold (GVM) and a service roughing manifold (SRM), with an integrated leak rate of less than $10^{-8} \text{ Pa}\cdot\text{m}^3/\text{s}$ for each component level. In Fig. 1, the GVM, SRM, SVLDM, and a tracer gas manifold (TGM) are indicated, and four RGA type 3 units are attached to the SVLDM which are ~ 90 degree apart for toroidal direction, to which the MSLD type 2 is linked. A port interspace is pumped by a port interspace cryopump (PIC) and connected to the SVLDM for leak checking. The SVLDM doesn't use charcoal coated cryopumps to suppress helium pumping. To analyze atmospheric gases from the RP sets for leak checking, three type 4 RGA units are attached to the exhaust lines routed to the tritium plant. The MSLD type 3 of the EC H&CD system is used for leak checking the waveguides.

During the torus roughing pump down of air or N_2 from 0.1 MPa to 1 Pa , the gross leak test will be implemented. The torus is pumped through the torus roughing line by using a RP set [23]. The torus pump down equation can be written as

$$V_0 \frac{dP_0}{dt} = -S_0 \cdot P_0 + Q_l + Q_o, \quad (1)$$

where V_0 is a torus volume of 4600 m^3 and dP_0/dt is a rate of change of the torus pressure with respect to time. The S_0 , Q_l and Q_o represent an effective pumping speed, an air leak rate and an outgassing rate, respectively. If the sum of Q_l and Q_o is as constant as Q during a short period of Δt from P_1 to P_2 , the solution becomes

$$\Delta t = \frac{V_0}{S_0} \ln \frac{P_1 - Q/S_0}{P_2 - Q/S_0}. \quad (2)$$

Figure 2 shows the estimated torus pump down curves for various air leakages. The outgassing rate is assumed to be $5 \times 10^{-6} \text{ Pa}\cdot\text{m}^3/(\text{s}\cdot\text{m}^2)$ for a total surface area of 11400 m^2 , which only affects the pump down curves to a final pumping stage. The $260 \text{ Pa}\cdot\text{m}^3/\text{s}$ air leakage (equivalent to $\sim 1100 \mu\text{m}$ leak hole in 50 mm path) occupies an ultimate torus pressure of 235 Pa , and 13 Pa for $11.3 \text{ Pa}\cdot\text{m}^3/\text{s}$ leakage. Thus from the ultimate pressures, very large leaks of more than about $10 \text{ Pa}\cdot\text{m}^3/\text{s}$ can be identified. At this stage, leak detection for localizing large leaks will be done by using the gross leak test mode of the MSLD type 1

and by using ^4He as a tracer. The detection range of the leak detection system Q_{range} is increased to

$$Q_{\text{range}} = \frac{S'_0 + S'_1}{S'_1} Q_{\text{Mrange}}, \quad (3)$$

where S'_1 is a foreline pumping speed at the MSLD inlet, and $Q_{\text{Mrange}} = 1 \sim 1 \times 10^{-8} \text{ Pa} \cdot \text{m}^3/\text{s}$ is the detection range of the MSLD. The S'_1 can be estimated at $21 \text{ m}^3/\text{h}$ for 100 Pa , this means that the Q_{range} is calculated at $170 \sim 1.7 \times 10^{-6} \text{ Pa} \cdot \text{m}^3/\text{s}$ for the torus pressure of 235 Pa . In a torus pressure of 10 Pa , the S'_0 and S'_1 are estimated at $0.7 \text{ m}^3/\text{s}$ and $10 \text{ m}^3/\text{h}$ for air, respectively, therefore the Q_{range} will be $250 \sim 2.5 \times 10^{-6} \text{ Pa} \cdot \text{m}^3/\text{s}$ assuming the same S'_0 and S'_1 for ^4He . Hence, the detection range of the torus leak detection subsystem is marginally increased to 2~3 orders of the magnitude of the MSLD itself. The scroll pump shows a zero pumping speed at 1 Pa , so the gross leak test mode is valid for torus pressure greater than 1 Pa . An acoustic method that detects ultrasonic waves generated by a gas being sucked into a leak hole, will be applicable for locating large leaks of more than $10 \text{ Pa} \cdot \text{m}^3/\text{s}$ [24].

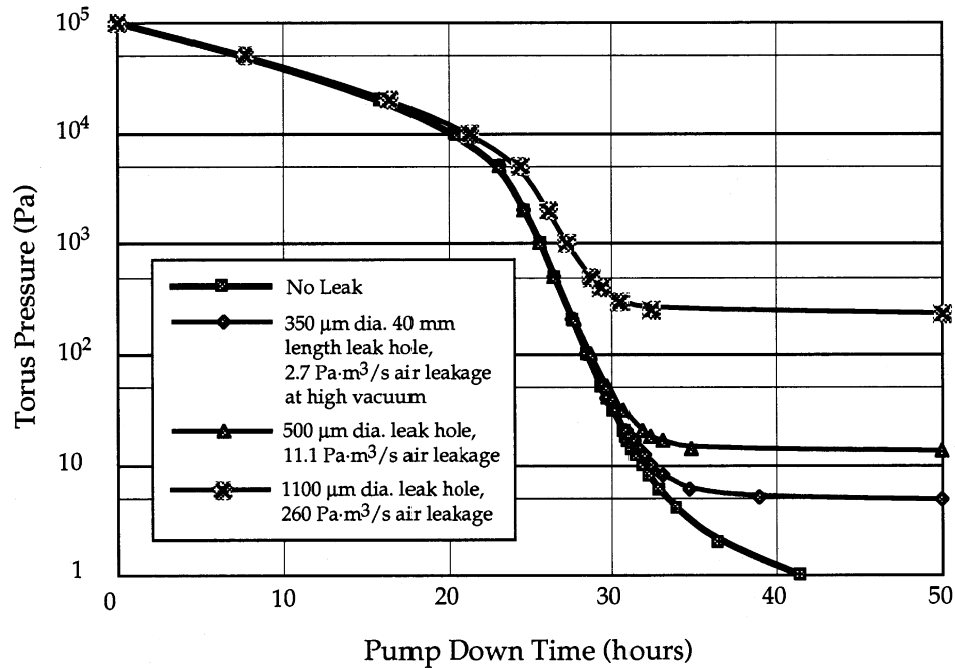


Figure 2. Influence of leaks on torus rough pumping characteristics.

When the torus pressure reaches $10 \sim 1 \text{ Pa}$, a pressure rise method will be implemented. Although detectability of the method closely depends on the torus outgassing rate, the outgassing rate per unit area q for the pressure rise method can be represented as [25]

$$q_1 = a \cdot e^{-\frac{t}{b}} t < t_1 \text{ hours}, \quad (4)$$

$$q_2 = c \cdot t^{-d} \quad t \geq t_1 \text{ hours}, \quad (5)$$

with $a=1.9 \times 10^{-4}$, $b=4$, $c=5.0 \times 10^{-5}$, $d=0.5$ and $t_1=10$, based on the assumptions that $q \sim 10^{-4}$, $\sim 10^{-5}$ and $\sim 5 \times 10^{-6} \text{ Pa} \cdot \text{m}^3/(\text{s} \cdot \text{m}^2)$ after 1, 10 and 100 hours of testing, respectively. Figure 3 shows the pressure rise curves of a $0.5 \text{ Pa} \cdot \text{m}^3/\text{s}$ air leakage after closing all pump inlet valves. The gross torus pressure of Fig. 3 can be measured by a capacitance manometer and its curve (dP_0/dt) is plotted as a torus gassing rate that is indicated in Fig. 4. From Figs. 3 and 4 it can be estimated that the gross leakages of more than $0.5 \text{ Pa} \cdot \text{m}^3/\text{s}$ can be clearly identified within about a day's buildup.

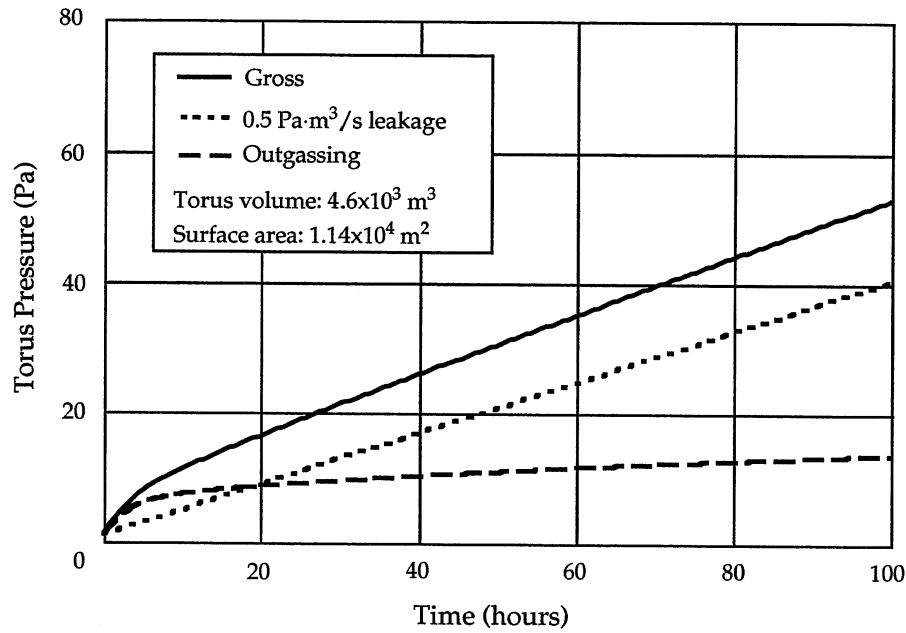


Figure 3. Example of torus pressures as a function of time for $0.5 \text{ Pa} \cdot \text{m}^3/\text{s}$ air leakage by pressure rise method.

During torus high vacuum pumping, the vacuum quality is monitored by the RGA type 1, using primary cryopumps from 1 to 10^{-7} Pa . If the unit outgassing rate is $5 \times 10^{-6} \text{ Pa} \cdot \text{m}^3/(\text{s} \cdot \text{m}^2)$, the ultimate torus pressure is only at a level of 10^{-3} Pa . This means that a baking of whole in-vessel components and a wall conditioning procedure [26] is indispensable in order to achieve an average outgassing rate of $\sim 10^{-9} \text{ Pa} \cdot \text{m}^3/(\text{s} \cdot \text{m}^2)$. This introduces the projected base pressure of 10^{-7} Pa for impurities. After these procedures the tokamak operation will start. The baking procedure of maximum $200\text{--}250^\circ \text{C}$ will be done by circulating pressurized hot water into in-vessel cooling channels. Once the water is introduced into the cooling channels, dominant leak species in the torus will become water vapor, so the residual water pressure is continuously monitored by the RGA type 1. During tokamak discharges, the level of oxygen impurities relating to water vapor can also be monitored by plasma spectroscopy diagnostics. According to current operating tokamak machines such as JT-60U, the maximum acceptable

water vapor pressure for normal tokamak operations is about 10^{-5} Pa, so if and when water vapor pressures in the torus exceed this level, the leak checking procedure will be activated.

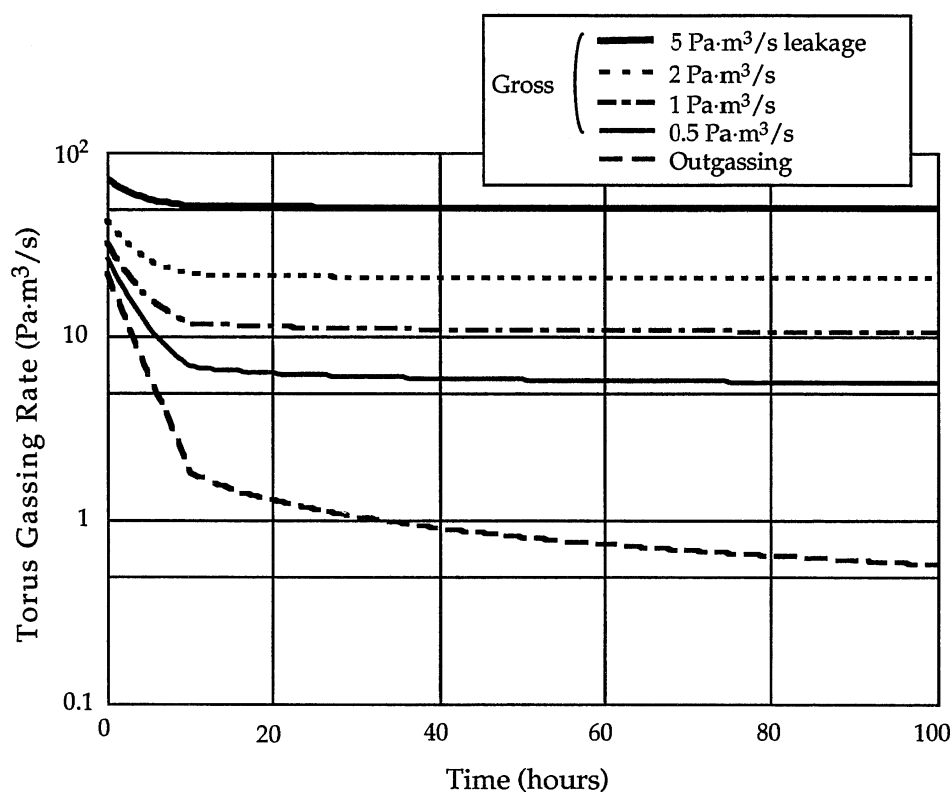


Figure 4. Example of torus gassing rates as a function of time for various leakages by pressure rise method.

From deviations of water vapor pressures measured by the RGA type 1, the leak can be localized within a quadrant area of toroidal direction. In-vessel water cooling channels are segmented by a minimum number of water valves for the primary vacuum vessel, the blanket and the divertor. The inlet water pressures of cooling channels belonging to a suspect quadrant area are sequentially changed by opening and closing water valves, and any resultant change in water vapor pressures are monitored by the RGA type 1. The suspect water channels are drained and dried for ^4He leak testing, according to the following scheme.

Waters are drained by gravity head and by circulating a ~ 2 MPa N_2 gas. The channels are further dried by circulating $\sim 150^\circ\text{C}$ with ~ 2 MPa N_2 gas through a water condenser until the partial steam pressure reaches ~ 0.001 MPa at the channel outlet. After that, pressurized ^4He is pumped into the suspect cooling circuits. Any ^4He that has leaked into the torus is detected by the MSLD type 1. If necessary, the suspect circuits are cut off even further and temporary valves are attached in order to isolate leaks on a subcomponent level, for example, in a divertor cassette which is also filled with ^4He . The drain and dry out procedure for water cooling channels prior to commencement of ^4He leak detection is a base design concept. However if the heat of radiation decay of in-vessel components such as the blanket and

divertor is too high to reduce or stop the cooling water flows for leak testing, long component cool down times are required in order to start leak testing. In addition, the ^4He leak test is not applicable during D-T plasma discharges, and when there are excess ^4He background levels. Therefore, an alternative method for leak testing in-situ with fully circulating water in the cooling channels should be developed.

A new leak detection concept for water loops described in Sec. 4 is proposed, in which water soluble materials are added to the circulating water as a tracer and any effused tracer emitted through leaks into the torus would be detected by the RGA type 1. Analyzing a time delay from a start of dissolving tracer till the relevant increase in the tracer peak, a further suspect area can be well localized. Another method of an in-vacuum side leak detection described in Sec. 5 will be implemented, in which a unidirectional total pressure gauge or the QMS head is mechanically scanned around the suspected area using a vacuum compatible manipulator. When the water leak is well localized using these methods, the torus is further N_2 purged to an atmospheric pressure for repair. After the leak relating water channels are drained and dried, the leak-localized component is remotely dismantled and then, transferred to the hot cell for further pinpoint leak checking and repair. The repaired components are reassembled and confirmed no leaks using the MSLD.

The other candidate options using an in-vessel manipulator that is no need of vacuum compatible are a sniffer method and a helium entrainment method. In the sniffer method, ^4He is introduced into the suspected channels after the drain and dry of the in-vessel water channels, and the torus is N_2 purged to 0.1 MPa. A sorption pump equipped sniffer tip [7] is scanned to the suspected inner area by using the in-vessel manipulator. The leaked ^4He gas with large amount of N_2 is sniffed, and the N_2 is effectively trapped to the liquid N_2 cooled sorption pump. A conventional leak detector can detect the no trapped ^4He . In this case, the sorption pump and the leak detector can be replaced by the counterflow helium leak detector. Another sniffer method is applicable with a combination of the pressurized N_2 in suspected channel and the atmospheric ^4He as the cover gas in torus [27]. The leaked N_2 into the torus can be retained with high concentration due to a lower diffusion coefficient of N_2 in atmospheric ^4He . The highly concentrated N_2 can be detected through the scanned sniffer tip and a differentially pumped mass spectrometer. In the helium entrainment method [9], the N_2 is introduced from the upstream of the suspected channel and the counterflow helium leak detector is connected to the downstream. From the in-vessel side, the ^4He spraying with mechanical scan is implemented and the leaked ^4He into the N_2 flow can effectively be transferred to the leak detector.

3. Development of High-Resolution Quadrupole Mass Spectrometer

The HR-QMS capable of discriminating ^4He (4.0026 amu) from D_2 (4.0282 amu) has been demanded for the leak detector in fusion machines. Another demand for the $^4\text{He}/\text{D}_2$ separation is the fuel and ash measurement, since the ^4He ash (α -particles) is generated by a deuterium-tritium (D-T) reaction. A principle of the QMS is based on the filtration of ions in a quadrupole field where the electric condition for stable ion orbits is mathematically determined by the Mathieu diagram. While the diagram has an infinite multitude of stability

zones, only zone I lying on the nearest position to the original point is adopted for conventional QMSs. Analysis and an experiment for the zone II condition were made, where zone II was denoted by a small rectangular zone having a center of $a=2.8$ and $q=3.0$ in the (a, q) plane. The peak on mass spectra using the zone II condition showed a sharp cutoff, suggesting the possibility of obtaining high resolution. Based on the results, the HR-QMS adopting the zones I and II conditions were developed, where the dc and rf voltages for each condition could be supplied by a common circuit. The two conditions can be easily chosen by pushing a switch to control some relays. When the zone II condition was selected, the QMS worked in a high-resolution mode in which a slope of the mass scan line was set near to the upper tip. The QMS could be also worked in a normal mode conditioned to zone I, whereby the application for QMS's was extended [28].

3.1. Specification of the HR-QMS

The dc (U) and rf (V) voltages derived from the upper tip values $a=3.164$ and $q=3.234$ are expressed as follows:

$$U \text{ (volts)} = 0.162[m(\text{amu})] \times [f(\text{MHz})]^2 \times [r_0(\text{mm})]^2, \quad (6)$$

$$V \text{ (volts)} = 0.331[m(\text{amu})] \times [f(\text{MHz})]^2 \times [r_0(\text{mm})]^2, \quad (7)$$

where m , f , and r_0 are the unit mass of ion, frequency, and an inner radius of the quadrupole, respectively. The combined voltage ($U - V \cos 2\pi ft$) is added to a pair of rods and $-(U - V \cos 2\pi ft)$ is added to another pair of rods. The analyzer head used was equipped with a Bayard-Alpert (B-A) type ion source, an ideal hyperbolic quadrupole, and a copper-beryllium type secondary electron multiplier (SEM). The quadrupole is 200 mm in length and $r_0=4$ mm. The gain of the SEM is 10^6 at -3 kV. The value of f is a key to high resolution, since the increase in f gives, in principle, both higher resolution and sensitivity. However, as expressed in Eqs. (6) and (7), the increase in f reduces the maximum amu at limited U and V ; we must hence make a compromise with f . When the value of f was increased from the previous 2.5 to 3.58 MHz, the resolution was much improved.

Figure 5 shows a main block diagram, where the four relays RL_1 - RL_4 are equipped with variable resistors. The role of each resistor for zone II is illustrated in Fig. 6, where the typical zones I and II are seen on the converted (U, V) plane. From Figs. 5 and 6 the roles of the resistors are explained as follows:

- 1) The variable resistors relating to RL_1 adjust λ (U/V ratio), i.e., the resolution.
- 2) RL_2 resistors control the bias voltage by which the λ line moves in parallel with the solid one (Fig. 6).
- 3) RL_3 resistor for zone II controls the initial U_0 to cancel out the interference of zone I's peak.
- 4) RL_4 resistors preset the displays of mass numbers. In reality, RL_1 and RL_2 resistors are simultaneously tuned to obtain the same width for each peak.

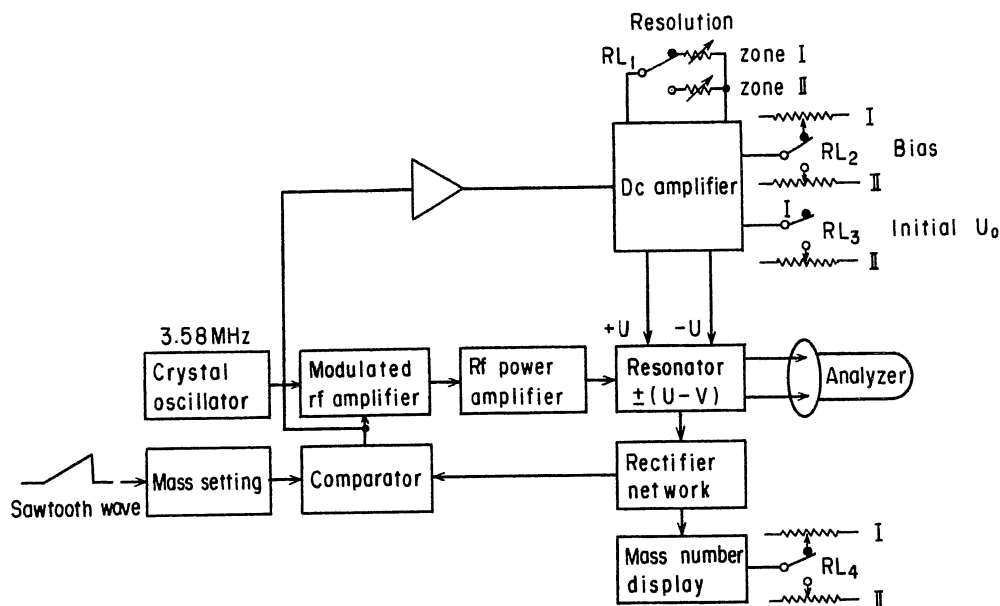


Figure 5. Block diagram of the HR-QMS.

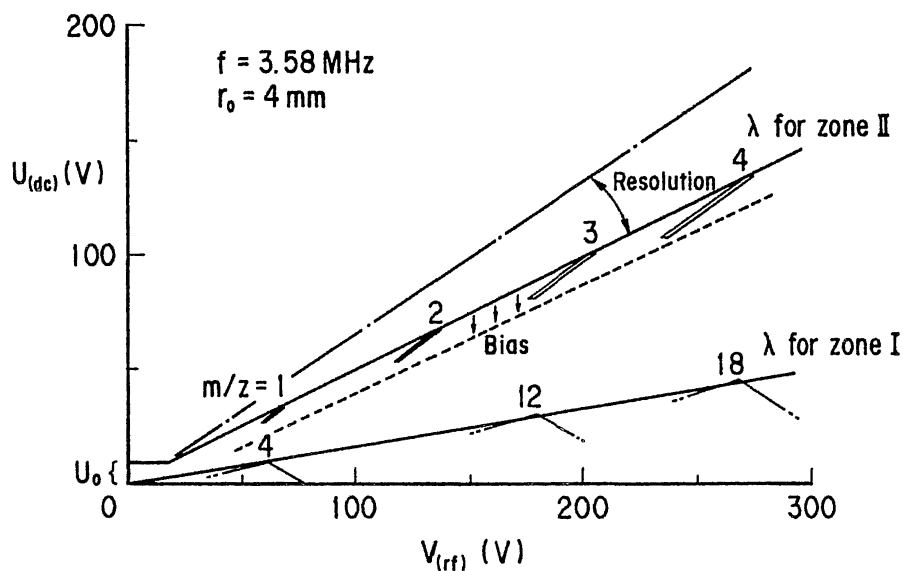
Figure 6. Stability diagram converted on the (U,V) plane for the zone I and II conditions; m/z , mass to charge number ratio; λ , slope of a mass scan line.

Figure 7 shows a photograph of the QMS we developed, where the QMS is named "HIRES0M-2SM" and is divided into three parts; a main controller, an analyzer head, and a drive unit. The main controller generates a sawtooth wave corresponding to a scan speed, first and last mass settings. The other settings are a change in the high-resolution mode, a gain of current amplifier, SEM voltage, etc. The drive unit contains most of the circuits shown in Fig. 5 and power supplies for the ion source and SEM.

Table 2. Main specifications of HIRESOM-2SM.

Items	Mode	
	High-resolution	Normal
Analyzable range (m/z)	1–9 amu	1–60 amu
Resolution ^a (m/ Δ m)	Up to 50m	1m
Lower detectable limit	10^{-8} Pa ^b	10^{-12} Pa ^c
Sensitivity	$10^{-4} - 10^{-5}$ A/Pa ^b	0.1 A/Pa ^c
Common specifications		
Scan speed	0.1–2000 s/scan	
Range of current amplifier	$10^{-5} - 10^{-11}$ A (full scale)	
Emission current	1 mA	
Weight	Analyzer head: 2.3 kg (without envelope) Controller: 3.2 kg Drive unit: 15 kg	

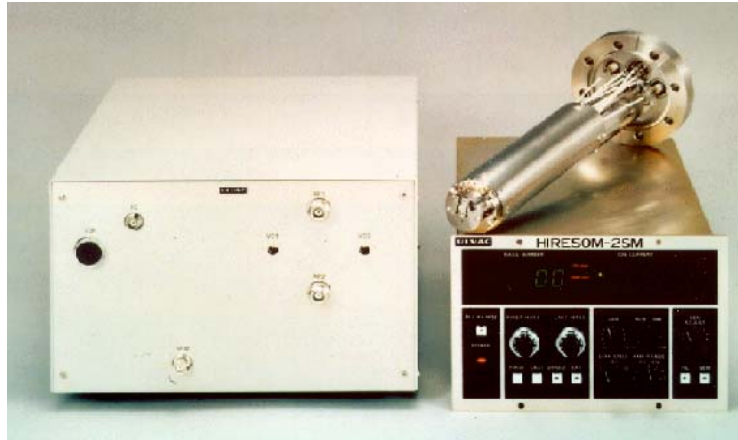
^aAt 10% peak height.^bFor ^4He .^cFor N_2 .

Figure 7. Photograph of the QMS called as HIRESOM-2SM. The main controller and an analyzer head without an envelope are on the right, and the drive unit is on the left.

The variable resistors to adjust resolution and bias in the high-resolution mode were installed on the back panel. Tuning the two resistors, we can obtain the best-resolved $^3\text{He}/\text{HD}$ and $^4\text{He}/\text{D}_2$ peaks. A toroidal core was used for the resonator (in Fig. 5) and also for the rf transformer, whereby the influence of external magnetic fields was reduced in tokamak experiments. The main specifications of HIRESOM-2SM are summarized in Table 2 in which the values of the upper four items are measured in a preliminary experiment. The substitution of the parameters m , r_0 , and f into Eqs. (6) and (7) yields $U=299$ V and $V=611$ V at $m/z=9$ amu with the high-resolution mode, and $U=149$ V and $V=890$ V at $m/z=60$ amu with the normal mode. Hence, the dc and rf amplifiers can supply the upper limits of $U=299$ V and

$V=890$ V, respectively. As will be shown in Sec. III, the full width at half-maximum (FWHM) resolution of up to 1080 ($m/z=4$ amu) is obtained with a sacrifice of sensitivity.

3.2. Experimental Results and Discussion

The analyzer head was installed on a test chamber into which the ^3He , ^4He , and D_2 gases were independently introduced through variable leak valves. Typical mass spectra observed in the high-resolution mode and in the normal one are seen in Figs. 8(a) and 8(b), respectively. In the figures, a mixture of ^4He , D_2 , and air is leaked into the chamber where the total pressure is 3.7×10^{-4} Pa. The outlines of saturated peaks are cut off on the tops due to a linear scale of the current amplifier. The peaks of $m/z = 1-3$ amu are predicted as H^+ , H_2^+/D^+ , and H_3^+/HD^+ , respectively. Besides, the peak of $m/z=4$ amu in Fig. 8(a) is divided into $^4\text{He}^+$ and D_2^+ . Figure 8(b) shows the normal spectra for $m/z = 1-60$ amu.

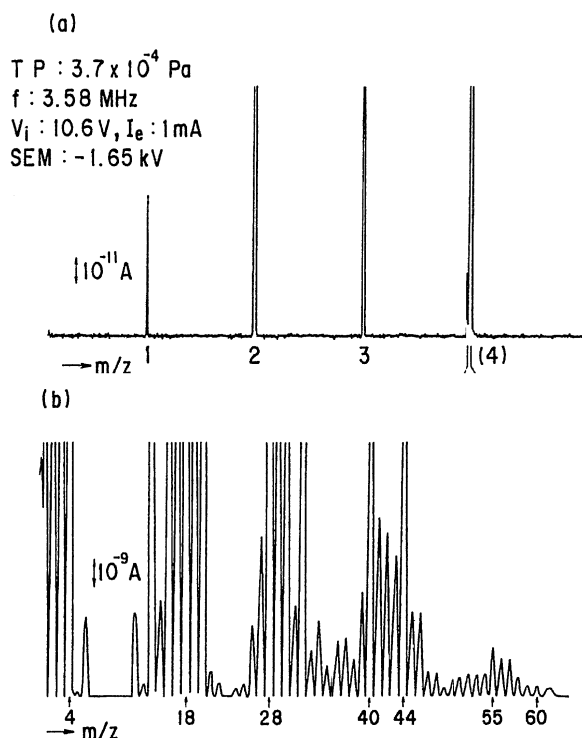


Figure 8. (a) Typical mass spectra in the high-resolution mode at 10^{-11} A/div, and (b) in the normal one at 10^{-9} A/div. The leaked gas is the mixture of ^4He , D_2 , and air. TP, total pressure; V_i , ion accelerating voltage; I_e , emission current; SEM, applied voltage to the SEM.

Figures 9(a)-9(e) indicate a series of separated $^4\text{He}^+/\text{D}_2^+$, where the ^4He pressure is much smaller than that of D_2 . The peak heights are, from the left, sequentially multiplied by a factor of 10. From the peaks in Figs. 9(a) and 9(e), the ratio of $^4\text{He}^+/\text{D}_2^+$ is estimated at roughly 10^{-4} . The resolution for the D_2 peak in Fig. 9(a) is also estimated at 360 (FWHM). The D_2 peaks in Figs. 9(c)-9(e) are wider than those in Figs. 9(a) and 9(b) due to longer time constants for the gains of $10^{-10} - 10^{-12}$ A/div. Furthermore, the resolution for the separated D_2 peak is increased

to 1080 (FWHM) as shown in Fig. 10 where the assumed $^3\text{He}^+$ is replaced with a dashed peak and D_2^+ is regarded as HD^+ . While the Δm between $^3\text{He}^+$ and HD^+ decreases to 1/4.3 of that between $^4\text{He}^+$ and D_2^+ , we can conclude that the QMS we developed has the ability to separate H_3^+ from HD^+ .

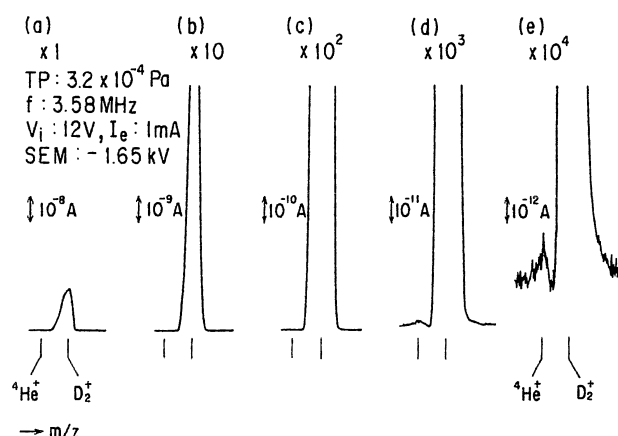


Figure 9. (a) Separated $^4\text{He}^+/\text{D}_2^+$ at 10^{-8} A/div; (b) at 10^{-9} A/div; (c) at 10^{-10} A/div, (d) at 10^{-11} A/div, and (e) at 10^{-12} A/div.

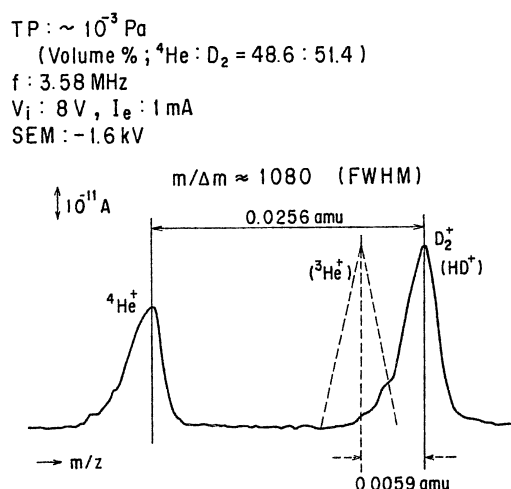


Figure 10. Well-resolved $^4\text{He}^+/\text{D}_2^+$ for supporting the ability to separate $^3\text{He}^+$ from HD^+ . The dashed peak and D_2^+ are regarded as $^3\text{He}^+$ from HD^+ , respectively, and $m/\Delta m$ means resolution.

To check this ability, a small amount of ^3He gas was leaked into the D_2 -filled chamber, and the QMS was carefully tuned to observe the double peak of $^3\text{He}/\text{HD}$ at $m/z=3$ amu. Figures 11 (a) and 11(b) show the separated $^3\text{He}^+/\text{HD}^+$ in which HD^+ contains, exactly speaking, H_3^+ on the high mass side. Although the D_2 pressure was much increased, little increase in the HD^+ height was observed due to the lack of H^+ or decomposed H that was to form HD^+ in the ion source. Hence, a higher D_2 pressure is set in Figs. 11(a) and 11(b) where the $^3\text{He}^+/\text{HD}^+$ ratio of roughly 0.1 is obtained.

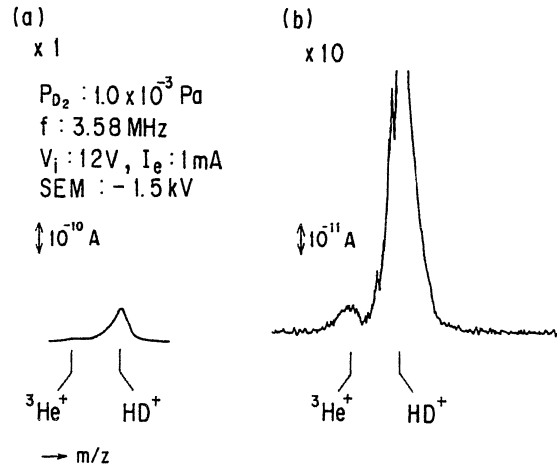


Figure 11. (a) Separated ${}^3\text{He}^+/\text{HD}^+$ at 10^{-10} A/div , and (b) at 10^{-11} A/div .

The reason for the high resolving power on the zone II condition can be explained as follows: The amplitude of stable ions in zone II is larger than that in zone I. Also the amplitude of unstable ions increases rapidly with increasing time and the unstable ions collide with the rods in smaller rf cycles. The rf cycle means a number of rf periods during which ions oscillate harmonically with the rf field, so that the increase in amplitude per cycle for the zone II unstable ion is larger than that for the zone I ion. The filtering effect for ions on the zone II condition is stronger, and the peak tailing due to transmission of unstable ions is much reduced. Hence, the decrease in peak tailing gives high resolution. The high-resolution means, however, low sensitivity caused by a large ion amplitude, so that a compromise for both resolution and sensitivity is needed. The best way to improve the two factors simultaneously is to increase f as much as possible. First, the relation between f and the resolution is discussed. As explained in the foregoing paragraph, higher resolution is derived from larger rf cycles. If the rf cycle is denoted as n_i then,

$$n_i = f \cdot l_0 \sqrt{\frac{m}{2eV_i}}, \quad (8)$$

where l_0 and e are a quadrupole length and charge of ion, respectively. In Eq. (8), n_i is increased with increasing f and l_0 , and with decreasing V_i for same ions. However, a large l_0 causes machining problems and a small V_i causes large ion oscillations in a fringing field. The rest is f . If the upper limit of applicable V is estimated at 1 kV, so $V = 1 \text{ kV}$, $m = 4 \text{ amu}$, and $r_0 = 4 \text{ mm}$ are substituted in Eq. (7), then $f = 6.87 \text{ MHz}$ is obtained. Also when $f = 6.87 \text{ MHz}$ and the same are substituted into Eq. (6), $U = 489 \text{ V}$ is derived. Hence, advancing our rf technology, we will be able to fabricate a controller adopting up to 7 MHz with Fig. 7's layout. Second, we mention the effect of f upon sensitivity. The sensitivity can be related to transmitted ions and if the ion current is denoted as I , then,

$$I \propto \frac{r_0^2 f}{(m/\Delta m)} \quad (9)$$

is given [29], since the maximum acceptable angle for the ions of oblique incidence is increased with increasing f . To increase I , r_0 is more effective than f ; however, a larger r_0 has no ability to restrain the peak tailing. Consequently, the increase in f improves both resolution and sensitivity, and also the lower limits of $^3\text{He}^+/\text{HD}^+$ and $^4\text{He}^+/\text{D}_2^+$ ratios.

4. Development of Water Leak Detection Methods Using Water-Soluble Materials

In-vacuum vessel components of fusion reactors, such as blanket and divertor, include many water-cooling channels, which must endure a high heat flux and a high radiation dose during plasma operations. In addition, the torus vacuum vessel comprises a double wall structure for water-cooling. The water leaks into the primary boundary will not only have a detrimental affect on plasma performance but can result in the loss of coolant accident (LOCA). When a water leak accident into the torus occurs, a human access for leak detection and repair is impossible because of the high radiation field. Development of a remote leak detection method applicable to the in-vacuum vessel water leakage is therefore, a vital issue in fusion reactors. Therefore, an improved leak detection method for the water channels is proposed,

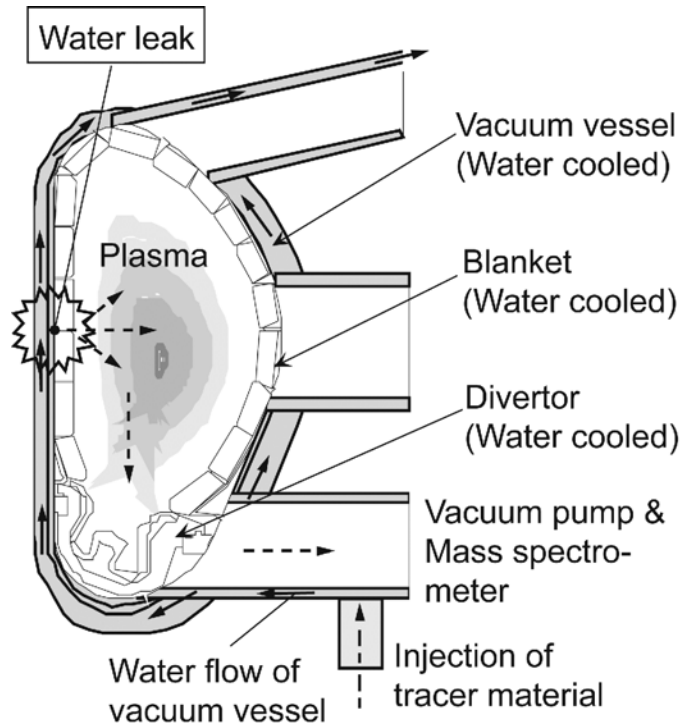


Figure 12. Concept of water soluble tracer method for water leak detection.

where the leak detection can be done with fully circulating the cooling water as shown conceptually in Fig. 12. A tracer material is dissolved into one circulating water circuit at a time and the tracer-dissolved water is effused into the vacuum vessel through a water leak passage. The leak point can roughly be localized by evaluating a time delay from the injection of the tracer-dissolved water until the actual detection of the tracer by using a mass spectrometer [30].

4.1. Experimental Apparatus

Among some candidate tracer materials, namely, radio isotopes, alcohol and rare gases, the rare gases were selected for the tracer materials, due to its lower neutron absorption cross section, although it shows poor solubility. In addition, considering the ITER parameters [13], the targeted water leaks of 10^{-2} Pa·m³/s levels and krypton (Kr) as the tracer were selected.

A water leak valve with the projected leak rate of less than 10^{-2} Pa·m³/s was designed and fabricated. Figure 13 shows a simplified leak model for design, where a crack with a rectangular cross section is assumed. When the water leak occurs in this model, a pressure loss for the z-direction balances to a shearing stress τ , then, the following equation is given,

$$A \cdot \frac{dP}{dz} = \ell_p \cdot \tau, \quad (10)$$

where A ; cross area of the crack, P ; representative water pressure, $\ell_p = 2 \cdot (\ell + H)$.

The water flowing through this small crack can be regarded as a laminar flow, so,

$$\tau = \mu \frac{\partial u}{\partial y} \approx 6 \frac{\nu \cdot G}{g \cdot \delta} \quad (11)$$

is obtained, where ν ; kinematic viscosity, g ; gravitational acceleration, G ; weight velocity per unit area. Eq. (11) is substituted into Eq. (10) and integrated for the z direction, then,

$$A \cdot \Delta P = 6 \cdot \ell_p \frac{\nu \cdot G}{g \cdot \delta} \cdot z \quad (12)$$

is obtained, where $\Delta P = P_1 - P_2$. From Eq. (13), G is

$$G = \frac{A \cdot \delta \cdot g}{6 \cdot \ell_p \cdot z \cdot \nu} \cdot \Delta P \approx \frac{\delta^2 \cdot g}{12 \cdot z \cdot \nu} \cdot \Delta P, \quad (13)$$

where $\ell_p \approx 2 \cdot \ell$. Hence, the water leak rate W in kg/s is

$$W = A \cdot G = A \frac{\delta^2 \cdot g}{12 \cdot z \cdot \nu} \cdot \Delta P. \quad (14)$$

Based on Eq. (14), a needle type, two leak valves were fabricated as indicated in Table 3 and Fig. 14. The valve sheet material was Viton, so the valve could be fully closed, which secured the helium leak rate of below 10^{-10} Pa·m³/s.

Table 3. Specification of water leak valves

Type	A	B
Inner diameter D_1 (μm)	250	250
Outer diameter D_2 (μm)	244	242
Clearance δ (μm)	3	4
Flow length z (μm)	50	50
* water leak late Q_w (Pa·m ³ /s)	2.0×10^{-3}	4.7×10^{-3}

*100 °C and 0.1 MPa.

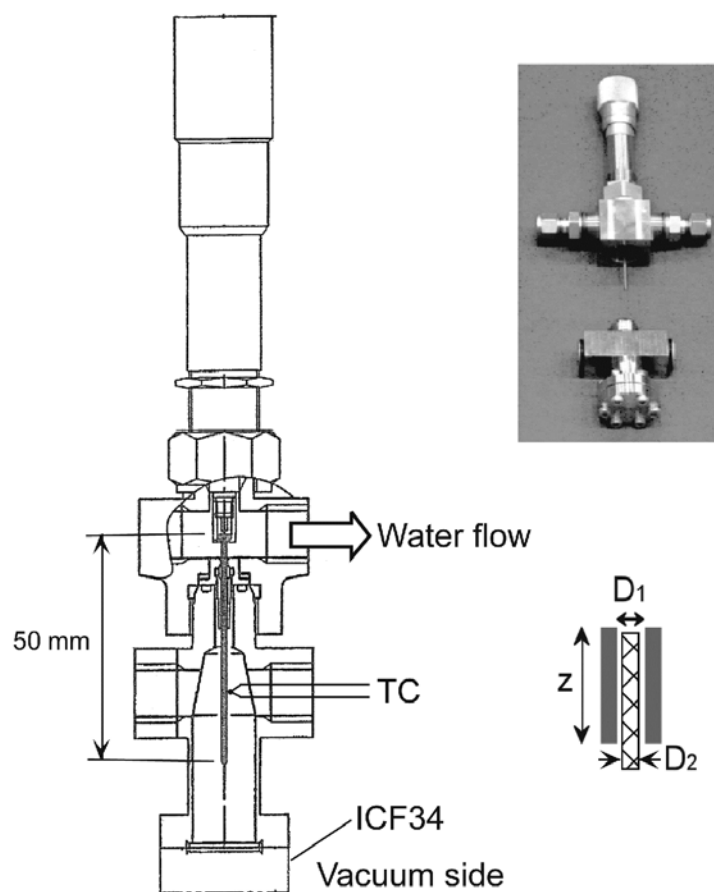


Figure 14. Fabricated water leak valve A. The valve comprises a fixed cylinder of D_1 in inner diameter, $z=50$ mm in length and a movable needle of D_2 in diameter. The needle can plunge in the cylinder with the upper linear motion feedthrough. TC: Thermo-couple.

Figure 15 shows a schematic view of the experimental apparatus comprising a water flow loop and a vacuum system. The SUS304 water loop of 20A was adopted, where the minimum constant flow rate of $6.0 \times 10^{-6} \text{ m}^3/\text{s}$ could be maintained stably with the combination of a circulation pump and a flow meter. The water temperature and pressure were maintained to $<100^\circ\text{C}$ and $<0.5 \text{ MPa}$ in order to be excluded from the domestic regulation of the high-pressure vessel. Two test pipes of 100A (Test pipe I) and 20A (ibid. II) were prepared, where inlets of two water leak valves could be connected. The outlets of the leak valves were connected to a vacuum vessel through flexible bellow tubes and evacuated by the $4 \text{ m}^3/\text{s}$ (for H_2O) cryopump. The TMP and the rotary pump (RP) were also used for the roughing pumping and the regeneration of the cryopump. The QMS equipped with a SEM was used for the Kr detection, and tuned to the m/e of 84. Kr is comprised of six stable isotopes (^{78}Kr , ^{80}Kr , ^{82}Kr , ^{83}Kr , ^{84}Kr and ^{86}Kr) and ^{84}Kr indicates the highest abundance ratio of 56.9 %. The emission current and the SEM voltage were fixed to 2 am and -1kV, respectively throughout this experiment. A calibrated Kr leak of $3.0 \times 10^{-8} \text{ Pa} \cdot \text{m}^3/\text{s}$ was attached to the vacuum vessel for the QMS calibration. Two 20A auxiliary test pipes of 1 m and 5 m in length were also used to confirm the pipe length variance. A photo of the assembled apparatus is also shown in Fig. 16.

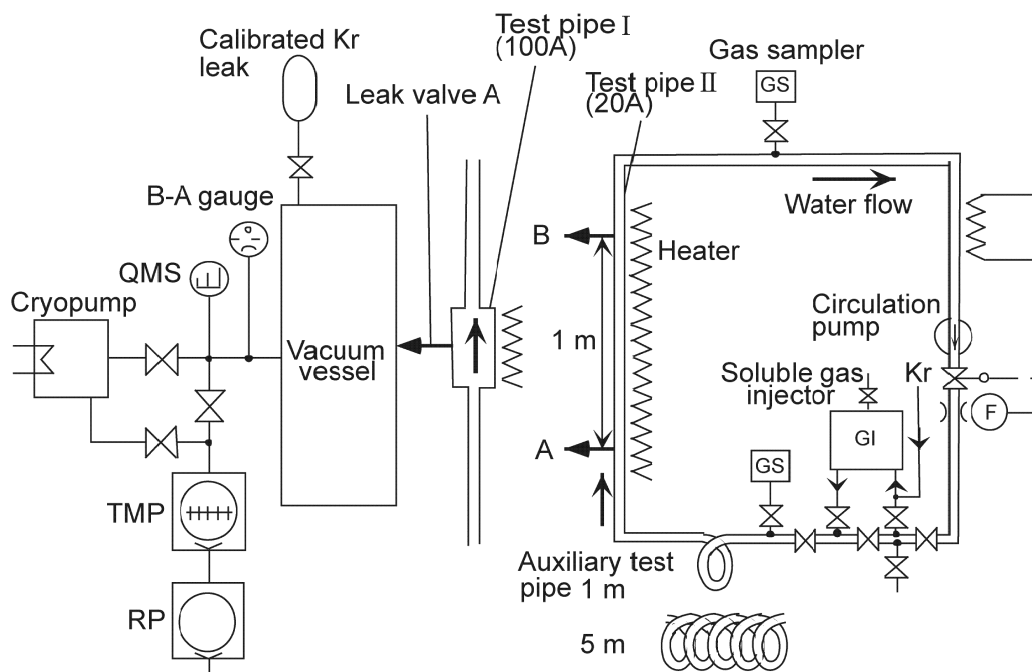


Figure 15. Schematic view of experimental apparatus. A nominal pipe diameter (ND) of the main loop is 20A whose inner diameter (ID) is 23.0mm. The test pipe I is 100A and 108.3mm in ID, to which the leak valve A is attached. The test pipe II is 20A and the leak valves A and B are attached by the distance of 1m. The water in the test pipes flows from the antigravitational direction.

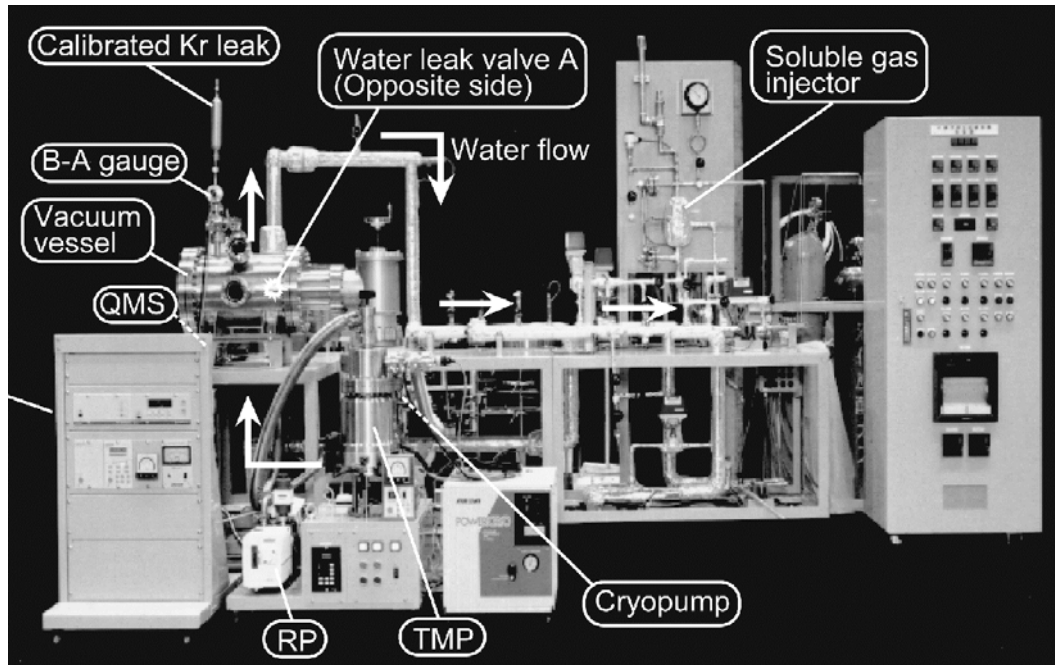


Figure 16. Photo of the assembled apparatus.

In a soluble gas injector (GI), Kr was bubbled into water at a projected water temperature and pressure to dissolve Kr to a saturation level. The Kr dissolved water was sampled by cylinders at gas samplers (GS) that were attached both the inlet and outlet of the test pipe. The sampled water was analyzed by a gas chromatography mass spectrometer (GC-MS) and the measured solubilities of Kr at two GS's were confirmed to nearly same as the theoretical value. This means that the Kr was dissolved into the circulating water at a saturation level.

4.2. Experimental Results and Discussion

An effective pumping speed of the cryopump system for water (S_w) was estimated to $0.6 \text{ m}^3/\text{s}$, which brought to an ultimate pressure of $2 \times 10^{-5} \text{ Pa}$ in the vacuum vessel without baking. Before the water dissolved Kr was effused into the vacuum vessel, the calibrated Kr leak and the water leak were independently introduced to check the QMS delectability for the $m/e=84$ peak. The valve A was attached to the test pipe I (see Fig. 15) at circulating water conditions of 0.15 MPa and 30°C , which could estimate the water leak rate of $9.2 \times 10^{-4} \text{ Pa}\cdot\text{m}^3/\text{s}$ using Eq. (14). Figure 17 indicates trend curves of the $m/e=84$ peak current and the vacuum vessel pressure when the leak valve A is continuously opened, whereas the valve of the Kr calibrated leak is periodically opened and closed. Although in the B-A gauge reading, there was a range of the coefficient in the relative sensitivity for water, such as 1.25 ± 0.44 due to the adsorptive property, we estimated the water pressure rise (ΔP_w) of $2.8 \times 10^{-3} \text{ Pa}$ (at the coefficient of 1.25). Then, the water leak rate ($Q_w = \Delta P_w \times S_w$) was given as $1.7 \times 10^{-3} \text{ Pa}\cdot\text{m}^3/\text{s}$ that was 1.8 times larger than that of the above designed value. The difference is mainly caused by the dimensional tolerance of the valve clearance ($\pm 4 \mu\text{m}$). In this experimental condition, we observed around the outlet of the needle valve in vacuum through a glass view port, and

confirmed that the flake of ice has formed but instantly sublimed. These phenomena were continuously observed but the water leak not stopped, so the actual leak passage was further narrowed by the flake of the ice. If we denote the QMS detection sensitivity for Kr as A_{Kr} that is a ratio of the $m/e=84$ peak current rise ΔI_{Kr} to the Kr leak rate Q_{Kr} , then, $A_{Kr}=1.9 \times 10^{-4}$ A/(Pa·m³/s) is given at the condition of Fig. 17.

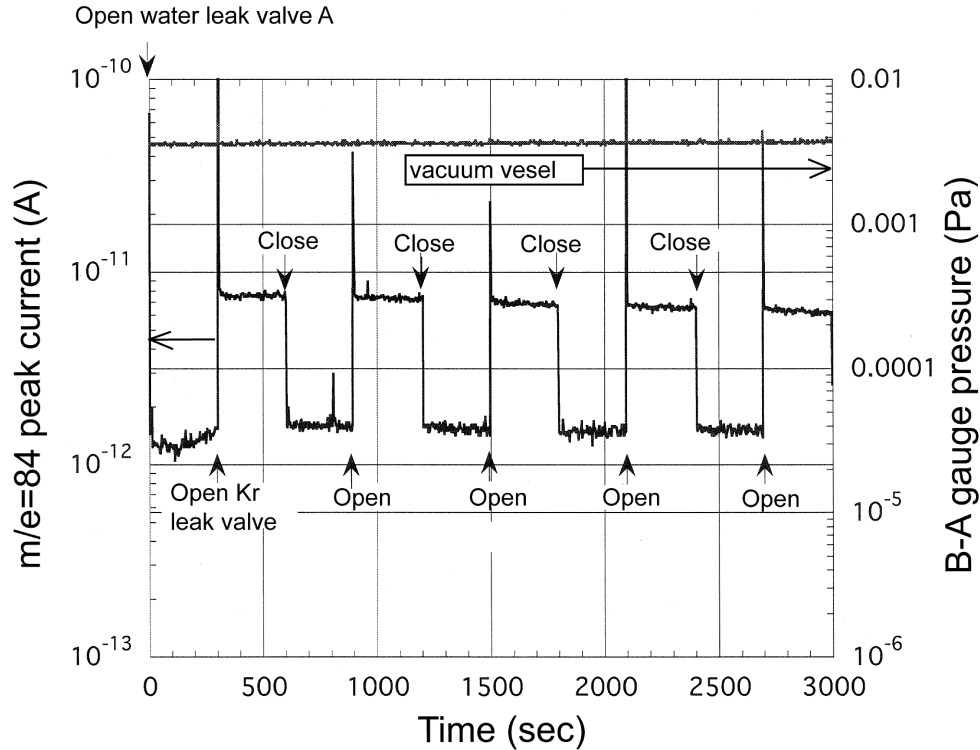


Figure 17. Trend curves of $m/e=84$ peak current and B-A gauge pressure. The calibrated Kr leak of 3.0×10^{-8} Pa·m³/s and the water leak were independently effused into the vacuum vessel. The water pressure and temperature were 0.15 MPa, 30 °C, respectively, and the flow rate of 6.0×10^{-6} m³/sec. The water leak valve A was attached to the 100A test pipe I and the auxiliary 20A test pipe with 1 m in length was used.

When the Kr dissolved water is introduced into the vacuum vessel at Fig. 17's same water condition, $\Delta I_{Kr}=2.5 \times 10^{-12}$ A is obtained as shown in Fig. 18. The ΔP_w was 3.6×10^{-3} Pa, so $Q_w=2.2 \times 10^{-3}$ Pa·m³/s. The solubility of Kr was estimated to 6.1×10^{-5} , then $Q_{Kr}=1.3 \times 10^{-7}$ Pa·m³/s, so $A_{Kr}=1.9 \times 10^{-5}$ A/(Pa·m³/s) was obtained. The reduction in the A_{Kr} is mainly due to the degradation of the SEM, because the background current in Fig. 18 during the water leaks is 2.6 times smaller than that in Fig. 17. In Fig. 18, a time delay from the introduction of the Kr dissolved water till the detection of the Kr is estimated as 694.3 sec at an auxiliary test pipe length of 5 m. Although not shown, another same experiment possessing only the auxiliary test pipe length was different as 1 m, indicated the time delay of 432.9 sec. The time variance between above two experiments was 261.4 sec for the pipe length difference of 4 m. The water flow velocity in the 20A pipe was 0.0144 m/sec, so the traveling time of the 4 m

length was calculated to 277.8 sec that could conclude to the positional accuracy of 5.9 % (0.24 m). In this case, the auxiliary test pipes were only covered with insulating materials, so an effect of the natural convection flow on the test pipes could be excluded.

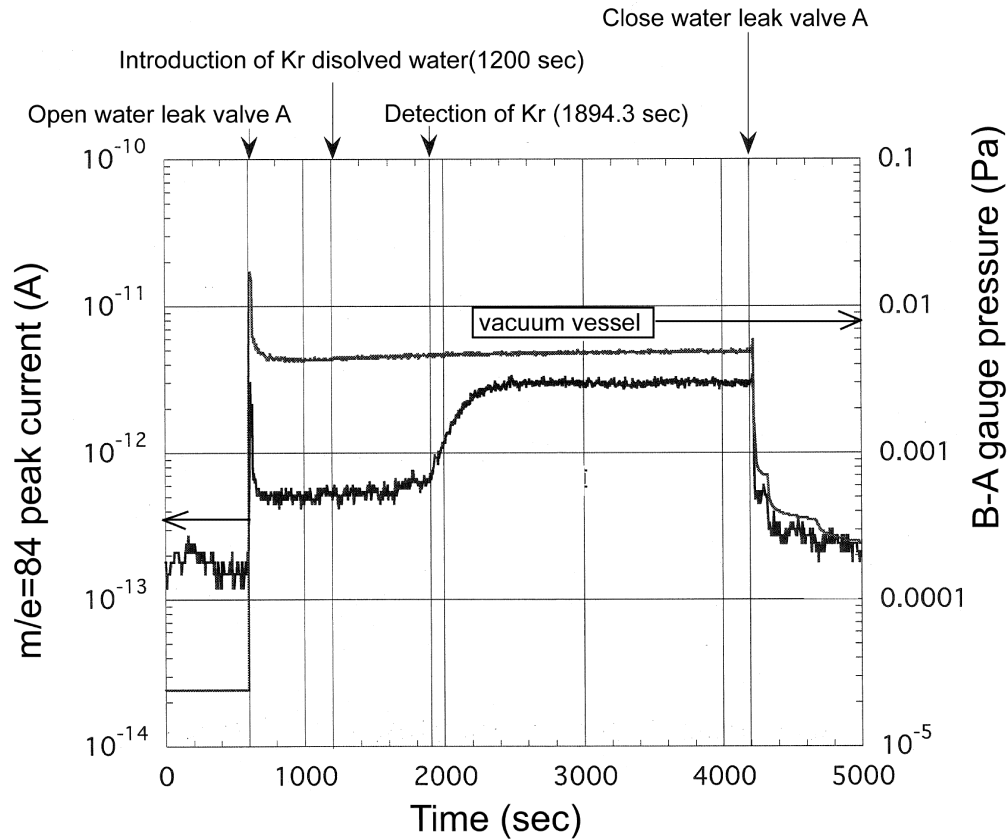


Figure 18. Trend curves of $m/e=84$ peak current and B-A gauge pressure. The Kr dissolved water was introduced into the vacuum vessel through the leak valve A. The water flow conditions were same as those of Fig. 17. The leak valve A was attached to the 100A test pipe I and the auxiliary 20A test pipe with 5 m length was used.

On the other hand, two leak points detection is experimented to verify an influence of the natural convection flow on the accuracy of leak points as indicated in Fig. 19, where the Kr dissolved water is introduced at 900 sec. At 1170 sec and 1209 sec, two distinct current rises are observed, so this time domain is further expanded as shown in Fig. 20, where the vertical axis is a linear scale. The bold curve indicated its regression one, in which the current rises of the valve A and valve B were 6.0×10^{-13} A and 1.1×10^{-12} A, respectively. The current rise of the valve B was 1.9 times larger than that of the valve A, so these current rises were directly proportional to the water leak rates. The time delay from detection A to detection B was 39 sec, while the time delay only considering the main flow velocity of 0.0144 m/sec between the 1 m valve distance was calculated to 69.4 sec. The observed time delay was 0.56 times of the calculated value, because the 20A test pipe II was heated to 30 °C and the natural convection increased the actual flow velocity.

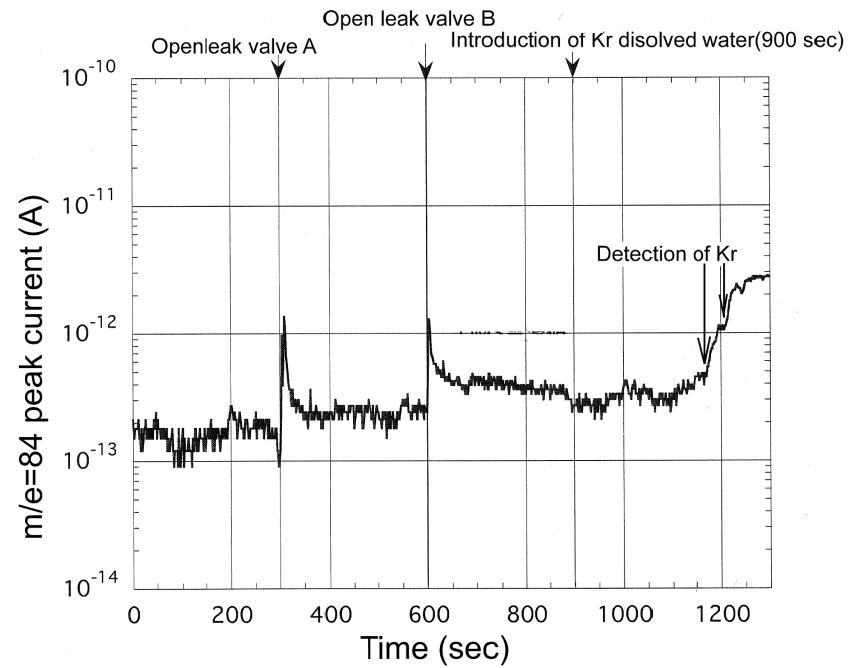


Figure 19. Trend curve of $m/e=84$ peak current for two water leak points. The water flow conditions were same as those of Fig. 17. The leak valves A and B were attached to the 20A test pipe II with 1 m distance and the auxiliary 20A test pipe with 1 m length was used.

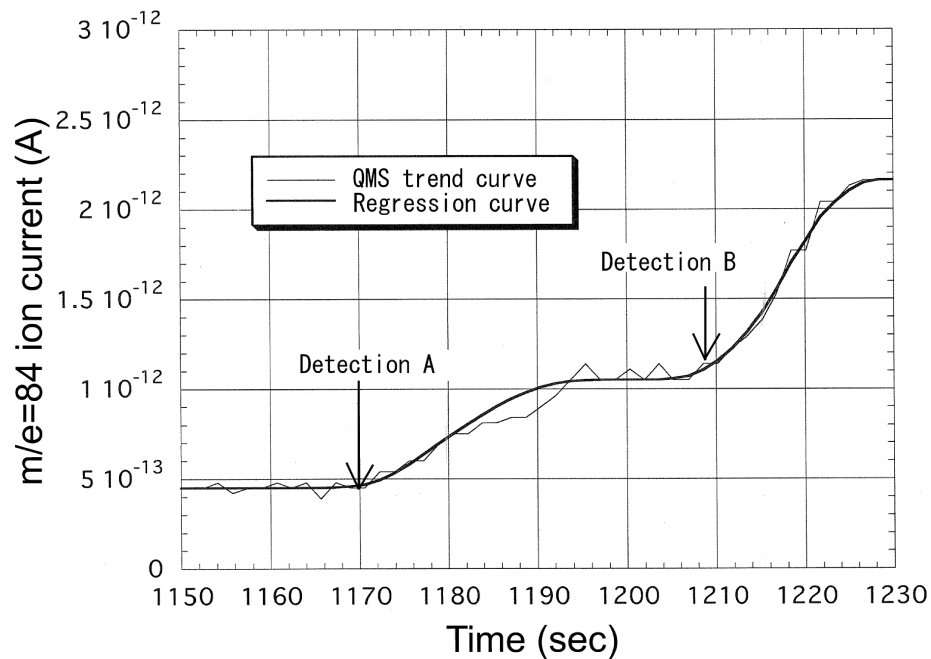


Figure 20. Regression curve of $m/e=84$ peak current for two water leak points.

A heat transfer model for a vertical uniform area can be applied to this 20A test pipe II, in which the maximum velocity of a natural convection u' at a representative length x can be written by the following;

$$\frac{\frac{u' \cdot x}{\nu}}{\sqrt{2 \cdot Gr_x}} = K_1, \quad (15)$$

where Gr_x ; Grashof number and K_1 ; constant number relating to Prandtl one. The Gr_x is

$$Gr_x = g \cdot \beta \cdot x^3 \frac{\Delta T}{\nu^2}, \quad (16)$$

where β ; coefficient of water volume change, ΔT ; temperature difference between outer pipe and inner water. The ΔT can be obtained by the following,

$$Nu = \frac{4}{3} K_1 \left(\frac{\nu_\infty}{\nu_w} \right)^{0.21} \cdot (Gr_x \cdot Pr)^{0.25}, \quad (17)$$

where Nu ; Nusselt number ($= \alpha \cdot \frac{x}{\lambda}$), λ ; heat conduction coefficient, ν_∞ ; kinematic viscosity at infinite distance, ν_w ; kinematic viscosity at wall surface, Pr ; Prandtl number, $\alpha = q / \Delta T$; heat transfer coefficient, q ; heat flux, μ ; viscosity. The K_1 is represented as the following;

$$K_1 = \frac{3}{4} \left[\frac{2 \cdot P_r}{5 \cdot (1 + 2 \cdot \sqrt{P_r} + 2 \cdot P_r)} \right]^{0.25}. \quad (18)$$

The temperature difference between two water leak valves A and B has actually been measured as 0.2 °C which represents $q=58.4 \text{ W/m}^2$ for the 20A test pipe II. Eqs. (16) and (18) are substituted into Eq. (17) and also the q value is assigned to Eq. (17), then the ΔT as a function of x is obtained. Further the ΔT - x characteristic is substituted into Eq. (16), and the Gr_x is assigned to Eq. (15), the resultant u' - x curve is obtained. The u' is superimposed to the main flow velocity of 0.0144 m/sec at Δx , so the accessing length considering u' as a function of traveling time is calculated as in Fig. 21. From Fig. 21, the traveling times of 7.7 sec and 52.3 sec at the valve A (accessing length of 0.125 m) and at the valve B (1.125 m) are obtained, respectively, hence, the time delay is 44.6 sec. The difference between observed and calculated time delays is 5.6 sec, which can define the positional accuracy of 12.6 % for the 1 m distance (0.126 m), based on the calculated one.

The influence of the dissolved Kr diffusion on the flow velocity could be ignored. We calculated one order dilution length and time of the Kr concentration using the first-diffusion equation and the diffusion coefficient (D) of $1.84 \times 10^{-9} \text{ m}^2/\text{s}$ at 25 °C. The D is inversely

proportion to the water temperature whereas no dependent on the pressure. The diffusion velocity was estimated as 6.6×10^{-6} m/s even at 100 °C that was enough small comparing with the main flow velocity of 0.0144 m/s. Hence, the influence of the natural convection by heating is the main limiting factor in this method. As demonstrated in this experiment, the positional accuracy depends strongly on the anticipated natural convection, so this method will be effective based on the detailed and accurate flow simulation.

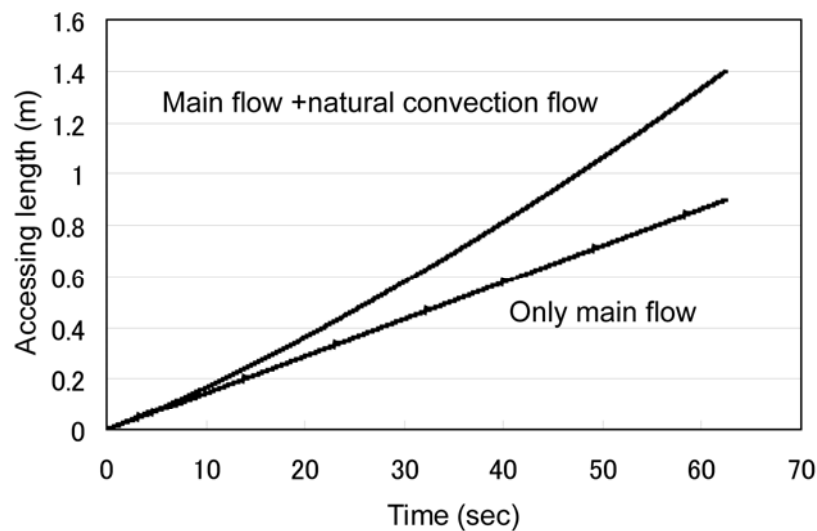


Figure 21. Calculated accessing length as a function of time.

Disadvantage of using Kr as the tracer is that the radioisotope of ^{85}Kr is produced by the $^{84}\text{Kr}(n, \gamma)$ reaction, although activations of other candidate tracers by the ITER neutron flux are inevitable. An efficient Kr cleanup in a water channel should be developed, so we propose a He purge method where He is bubbled into water in stead of Kr at GI of Fig. 15. The dissolved Kr is effectively replaced by He and gas-phase Kr is deposited at upside of the GI and recovered. This procedure is extensively used in a liquid chromatography (LC) area for degassing dissolved air in solvent materials.

In conclusion, for the target water leak rate of less than 10^{-2} Pa·m³/s, 10^{-3} Pa·m³/s of the single water leak could be detected and located with the positional accuracy of 5.9 % (0.24 m for 4 m difference) for 20A auxiliary test pipes without heating. For 10^{-3} Pa·m³/s of two water leak valves with 30 °C heating, the positional accuracy of 12.6 % (0.126 m for 1 m distance) was obtained, based on the natural convection flow calculation.

5. Development of in-Vacuum Side Leak Detection Methods

Another valuable water leak detection method applicable during fully circulating the water is the one from in-vacuum side, in which a total pressure gauge [31] or a partial pressure gauge [32] is mechanically scanned around the suspected area by using a vacuum compatible manipulator. Also in ITER, the vacuum manipulator for inspection use has been developed.

5.1. Total Pressure Measurement

A unidirectional total pressure gauge comprising a cylindrical tantalum covered Bayard-Alpert type gauge is scanned three-dimensionally around a simulated leak nozzle at some leak rate Q_1 as shown schematically in Fig. 22. A cross section of the collimator characterizing the unidirectional gauge is also indicated in Fig. 23, where the diameter D and length L of the nozzle determine the leaked gas entrainment solid angles θ_1 and θ_2 . A dry air was effused from the 0.5 mm in diameter of orifice of the simulated leak nozzle through a variable leak valve from a gas reservoir, and its leak rate Q_1 was measured with a conductance method.

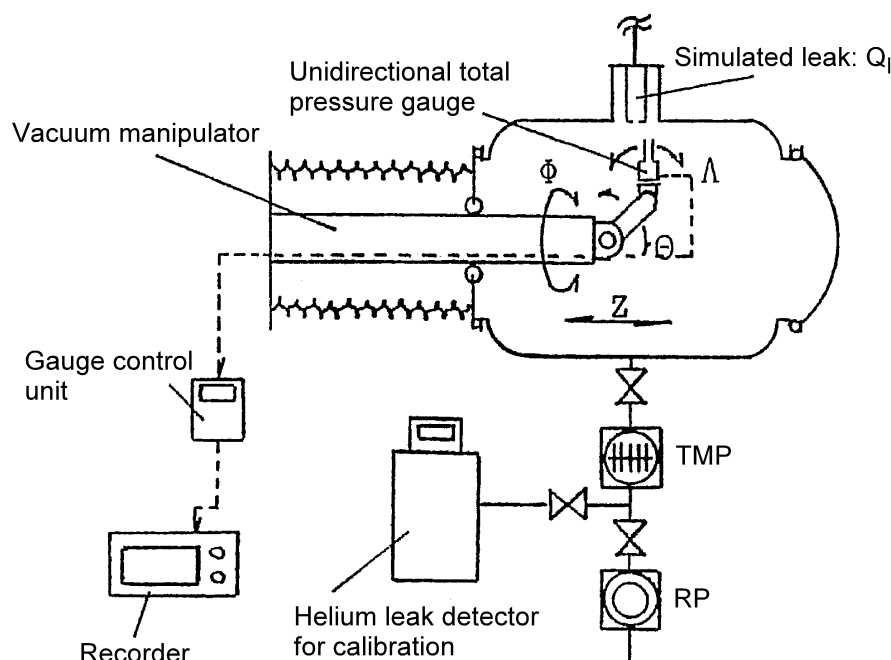


Figure 22. Schematic layout of the experimental setup. The vacuum manipulator can move the pressure gauge for three-dimensional direction with Φ , Λ , Θ and Z axes. Q_1 ; leak rate of simulated leak, TMP; turbomolecular pump, RP; rotary pump.

The vacuum manipulator mainly adopted a gear wheel drive mechanism with special solid lubricants, and the chamber could be baked out to more than 300 °C, so that the ultimate chamber pressure reached to 10^{-5} Pa. The leak nozzle orifice was located on the identical inner surface of the chamber, and the unidirectional gauge was mechanically scanned for z -direction, holding a constant distance between the inlet nozzle and the chamber surface Δ_1 . Examples of raw data on P_s for various scan speeds V_z 's are shown in Fig. 24, where Q_1 , L/D and Δ_1 are 3.1×10^{-7} Pa·m³/s, 32mm/8mm and 5 mm, respectively. Definitive pressure peaks were identified and the amount of the measured pressure change ΔP_s^* for $V_z=175$ mm/min was 6.3×10^{-6} Pa. In addition, Fig. 25 plots ΔP_s^* as a function of Δ_1 , and a calculated pressure change ΔP_s is contrastively plotted. A substantial reason of the ΔP_s curve is the following.

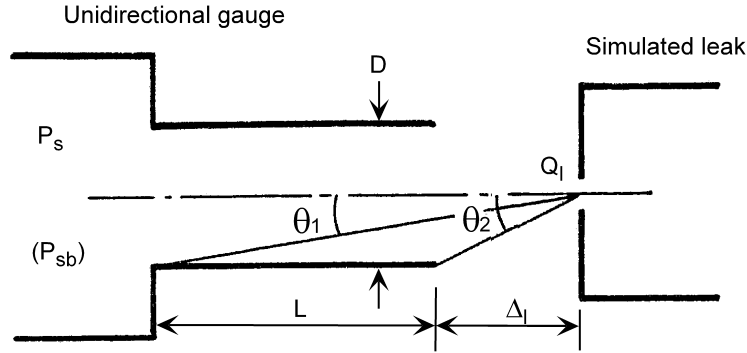


Figure 23. Cross-sectional view of the collimator on the same axis of the simulated leak nozzle. P_s ; total pressure in the gauge, P_{sb} ; background pressure, D and L ; diameter and length of the gauge inlet nozzle, Δl ; distance between the inlet nozzle and simulated leak, θ_1 and θ_2 ; back and front of the inlet nozzle solid angles.

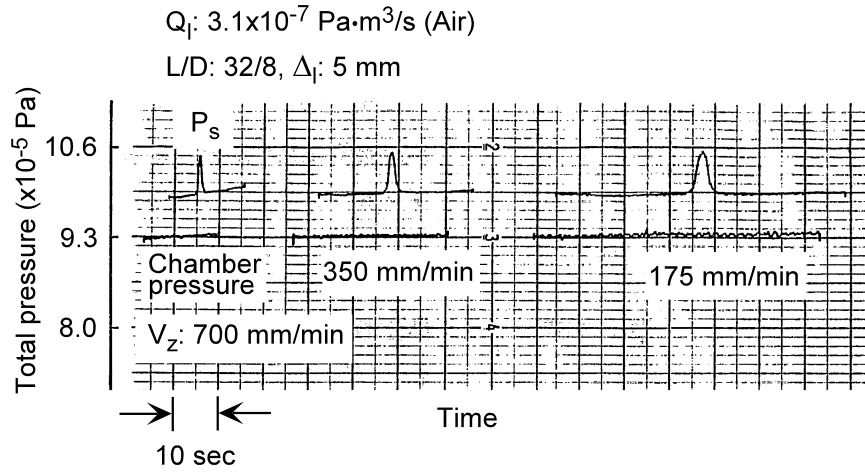


Figure 24. Trend chart of the total pressures (upper: gauge pressure and lower: chamber pressure) at three gauge scan speeds for Z axis (V_z 's).

A background pressure far from the leak point, P_{sb} can be written as,

$$P_{sb} = \frac{Q_l + Q_o}{S_{eff}}, \quad (19)$$

where Q_o and S_{eff} are an outgassing rate of the chamber and an effective pumping speed, respectively. When we assume the effused air distribution in vacuum as the cosine law, an efficiency of the leaked air entrainment through the collimator, E is (see Fig. 23)

$$E = \int_0^{\theta_1} 2 \sin \theta \cdot \cos \theta \cdot d\theta + K \int_{\theta_1}^{\theta_2} 2 \sin \theta \cdot \cos \theta \cdot d\theta, \quad (20)$$

where K is the Clausing factor. So that the following equations are derived,

$$C(P_s - P_{sb}) = E \cdot Q_l, \quad (21)$$

$$\Delta P_s = P_s - P_{sb} = \frac{E}{C} Q_l, \quad (22)$$

where C is a molecular flow conductance of the collimator. The experimental parameters are substituted into Eq. (22), then the ΔP_s is obtained and plotted as in Fig. 25. Although the measured ΔP_s^* is faintly smaller than the calculated ΔP_s , almost same decay curves are obtained. Also the 10^{-8} Pa·m³/s level of air leaks could be detected at the background pressures of 10^{-5} Pa bring the unidirectional gauge close to the leak source to <10 mm. Extending this method, a pressure rise by water leaks will also be detectable.

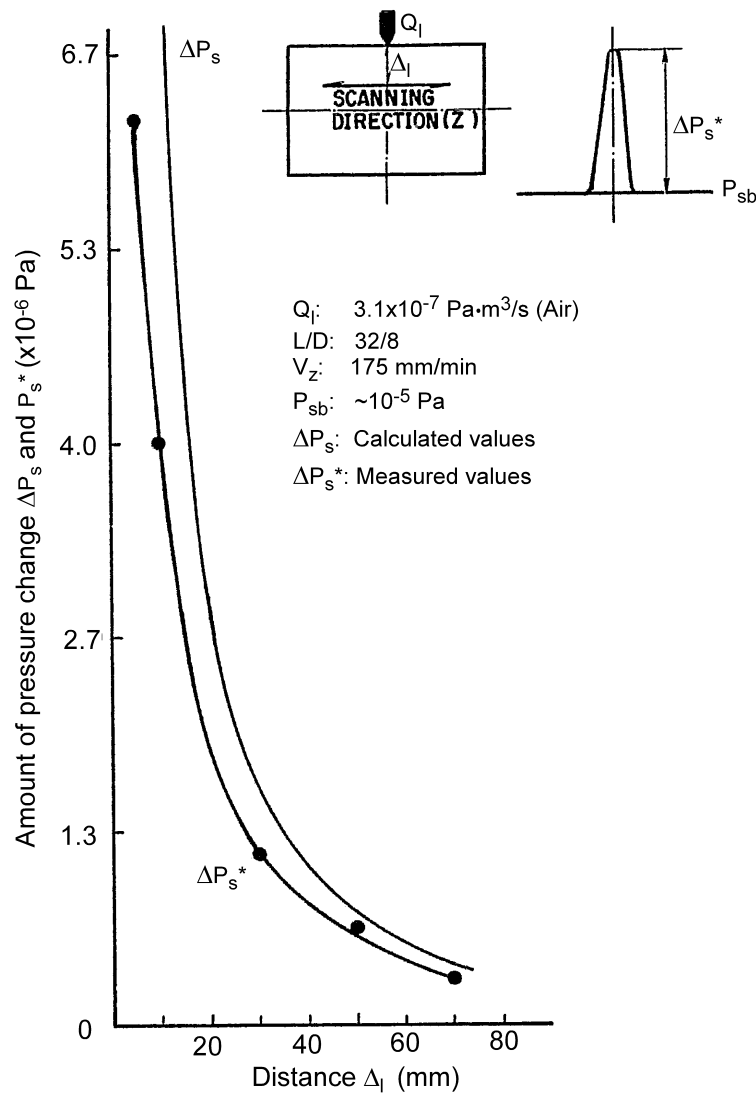


Figure 25. Calculated and measured pressure changes ΔP_s and ΔP_s^* as a function of the distance Δ_l .

5.2. Partial Pressure Measurement

The drawing of the analyzing head is shown in Fig. 26. This head was mainly made of stainless steel casing with an outer diameter of 43 mm, 145 mm long, and 1.6 kg in weight, and comprised a tubular collimator with a inner diameter (D) of 10 mm and 5 mm long (L) on the top. The Channeltron®, an ion source and a quadrupole electrode were commercially available. The electronic circuit consisting of an rf tuning and an rf detection subcircuit was packed in a vacuum flange (ICF 70) sealed cylindrical casing which was directly connected to the head. This circuit diagram is shown in Fig. 2. This was composed of a ferrite toroidal core, a rectifier tube, solid resisters, ceramic and dipped mica capacitors, and the polytetrafluoroethylene (PTFE) insulated electric wires. These components normally fulfilled their functions up to 150 °C, and were fixed with an inorganic bond after being soldered together. This circuit should be used below the melting point of the solder (183 °C), hence the maximum working temperature of the head and the maximum baking temperature of the vessel were determined to 150 °C. The resonance frequency of the rf circuit giving a parallel resonance condition depends weakly on environmental temperature, and an ordinal way of mechanically adjusting a trimmer capacitor to an optimum value is not practicable after the circuit was packed in, therefore, we have tried to tune the circuit by supplying variable frequency of rf voltage. This resonance frequency was designed roughly on 2.2 MHz at 25 °C and realized by winding up the diameter of 0.4 mm PTFE insulated wires on the toroidal core with 4 turns for primary and 19 turns for secondary (Fig. 27). Figure 28 shows an example of a resonance characteristic at 25 °C and Fig.29 shows a temperature dependence of the resonance frequency. The frequency at the minimum current was approximately 2.25 MHz (Fig. 28) and the deviation of the resonance frequency in elevated temperature was, at the most, less than 0.1 MHz from 25 to 150 °C (Fig. 29). Under these conditions, the maximum rf and dc voltage applied to per a half of the quadrupole electrode at 50 amu (atomic mass unit) is 183.3 and ± 31.6 V, respectively. Thus, this circuit would need the breakdown voltage of more than 220 V and its voltage was practically attainable to 150 °C.

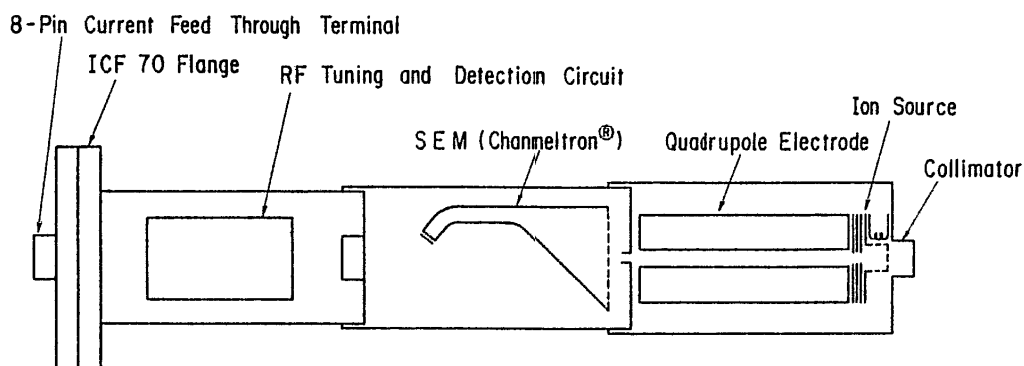


Figure 26. Drawing of the analyzing head. The head possesses a tubular collimator with an inner diameter (D) of 10 mm and a length (L) of 5 mm.

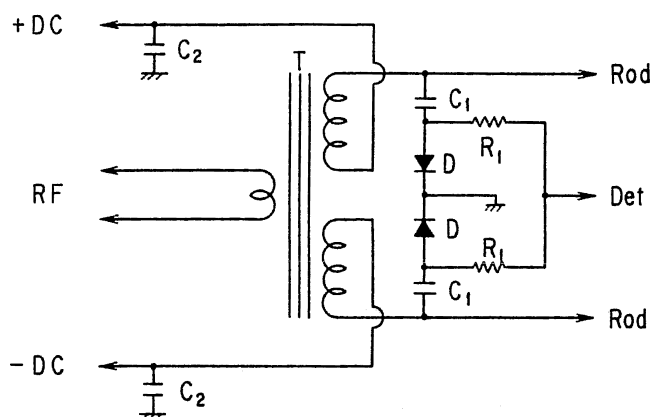


Figure 27. Circuit diagram of the rf tuning and detection packed in an airtight casing. T: Ferrite toroidal core, FT-82#61 (Amidon), D: Rectifier tube, 6AL5 (Toshiba), C_1 : Dipped mica capacitor (100 pF), C_2 : Ceramic capacitor (1000 pF), R: Solid resistor (500 k Ω).

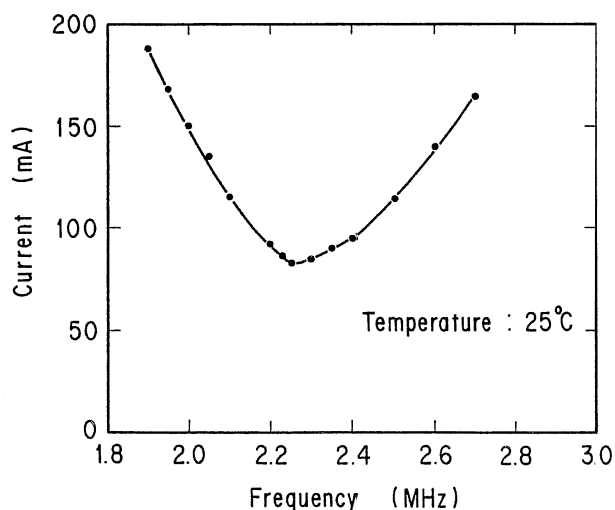


Figure 28. Example of the resonance characteristic of the rf tuning and detection circuit at 25 °C. The current is from the power supply flowing into the rf amp and the frequency is the oscillator reading.

This head was fitted on a center of JVX-I (JAERI Vacuum Experiment-I) cylindrical vacuum vessel made of Inconel 625 with an inner diameter of 0.36 m and 1.5 m long. Figure 30 shows the experimental apparatus. The head was horizontally rotatable with a rotary motion feedthrough, denoted as θ in Fig. 30. A test gas was effused to the vessel through a stainless steel tube with an inner diameter of 3 mm and 500 mm long. The conductance (C) of the tube is $6.5 \times 10^{-6} \text{ m}^3/\text{s}$ for air equivalent. Pressures of the gas supply side (P_1) and the vessel side (P_2) were measured by a Schulz-Phelps and a B-A gauge, respectively. These gauges were calibrated by a spinning rotor gauge, then the flow rate (Q) of the effusing gas was estimated from the equation $Q = C(P_1 - P_2)$. The outlet of this straight nozzle faced the collimator of the head. The rotatable angle of θ around the rotor axis of the feedthrough is within $\pm 30^\circ$, then $\theta = 0^\circ$ represents that the center axis of the collimator corresponds to that of

the nozzle and the distance between them (Δl) is 125 mm. About 2 m long PTFE insulated coaxial cables with an outer diameter of 0.9 mm was used in vacuum to connect from the current feedthrough to the packed rf circuit and to the SEM. Moreover, ceramics insulated aluminum wires with a diameter of 1 mm and 2 m long were tentatively used from the feedthrough to the ion source. Thus, the attainable pressure of the vessel was 1×10^{-5} Pa after the baking of 150 °C for 6h.

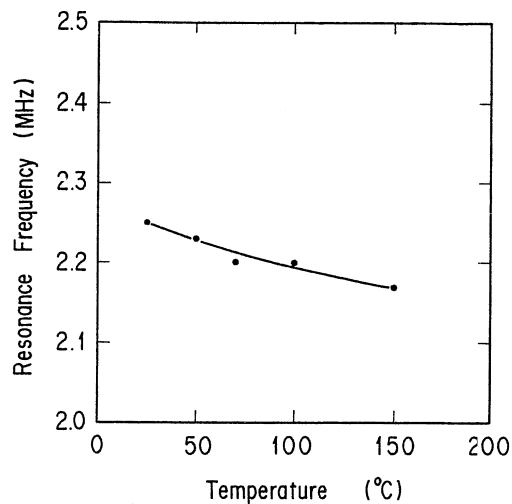


Figure 29. Example of the temperature dependence of the resonance frequency. The rf current is minimized at the frequency.

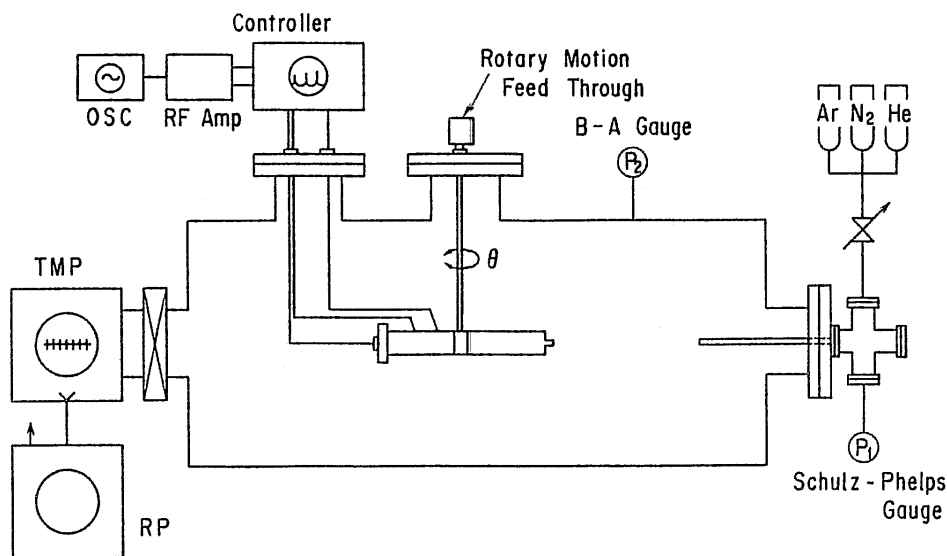


Figure 30. Schematic diagram of the experimental apparatus.

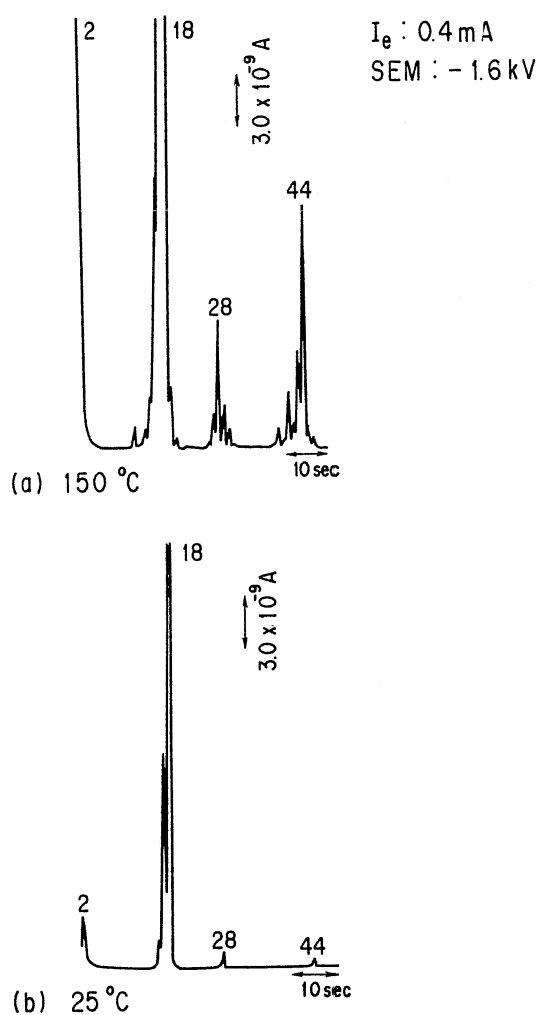


Figure 31. Mass spectrum obtained from this QMS, (a) when the vessel was baked to 150°C and (b) to 25°C . I_e and SEM show an emission current and a voltage applied to the SEM, respectively.

A normal mass spectrum from 1 to 50 amu was obtained when the vessel was baked to 150°C and kept to 25°C as shown in Figs. 31(a) and 31(b), respectively. The frequency of the supplied rf voltage was adjusted to give a minimum rf current, namely that was 2.18 MHz at 150°C and 2.25 MHz at 25°C . The frequency at the parallel resonance condition (f) is denoted as

$$\frac{1}{2\pi\sqrt{LC}}, \quad (23)$$

where L and C are an equivalent inductance and a capacitance, respectively. The frequency f depends negatively on the temperature (see Fig. 28). Because a temperature coefficient of L for the ferrite coil is much larger than that of C for the rf circuit and the coefficient of L is

passive, then the term $\sqrt{LC}$ in Eq. (23) increases as the temperature rise, that causes to decrease of the frequency f . The frequency of the rf voltage should not excessively modify after the fixing of the working condition, since the modification of the frequency causes to change a transmissivity of ions in a quadrupole electrode. A resolution ($m/\Delta m$) measured as a full width at a half-maximum amplitude for $m = 44$ in the spectrum of 150°C 's [Fig. 31(a)] was 77 and this value is adequate as compared with a commercialized QMS. A higher resolution will be obtainable by adjusting a ratio of an rf voltage to a dc with a sacrifice of a signal gain.

For the performance test of this QMS, a molecular flow of argon gas was effused from the nozzle. The fixed Ar^+ (40 amu) and the other background signals were measured with slowly rotating the head from $\theta = -30^\circ$ to $+30^\circ$, and the flow rate (Q_{Ar}) and the vessel pressure (P_2) were $1.9 \times 10^{-5} \text{ Pa} \cdot \text{m}^3/\text{s}$ and $1.2 \times 10^{-4} \text{ Pa}$, respectively, as shown in Fig. 32. The Ar^+ ratio of 1.09 and 1.07 were obtained with the θ ratio of $0^\circ/-30^\circ$ and $0^\circ/+30^\circ$, respectively, whereas the ratio of CO^+/N_2^+ , H_2O^+ and H_2^+ were almost unity. The Ar^+ signal at $\theta = -30^\circ$ was $1.0 \times 10^{-10} \text{ A}$ and assuming this value as the vessel pressure, then the sensitivity of this QMS is $8.3 \times 10^{-7} \text{ A/Pa}$ and the measured pressure rise (ΔP_s) in the head is $1.08 \times 10^{-5} \text{ Pa}$.

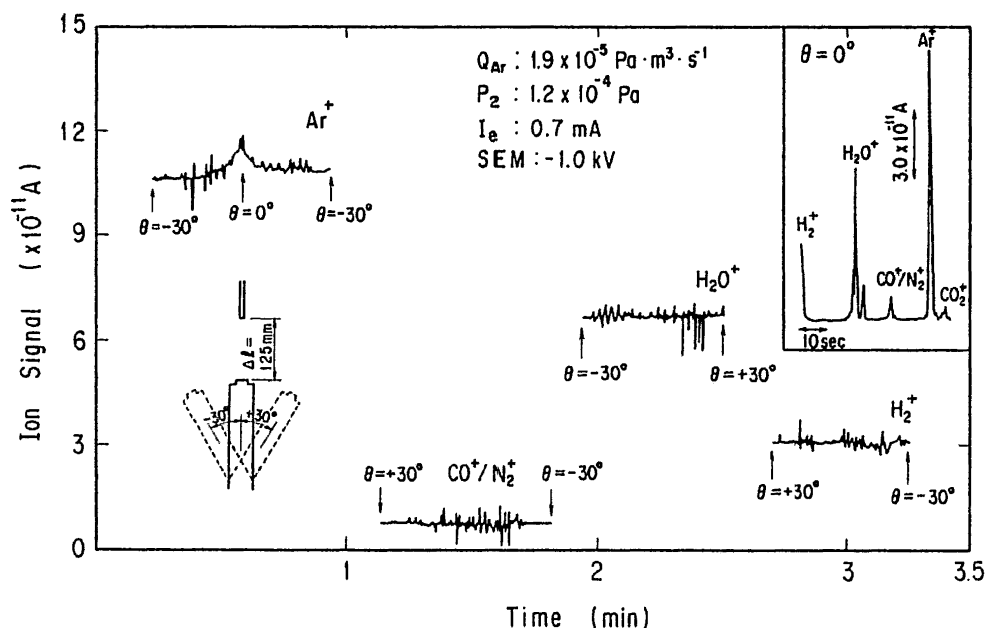


Figure 32. Example of fixed Ar^+ and the other signal changes with moving the head round the effusing nozzle. The effusing gas is argon and Q_{Ar} shows the flow rate. The mass spectrum at $\theta = 0^\circ$ is also shown on the upside of the right-hand side.

Figure 33 shows the calculation of the ΔP_s using Eq. (22) at various Δl with the same Q_{Ar} and P_2 as in Fig. 7. The calculated ΔP_s at $\Delta l = 125 \text{ mm}$ was $5.2 \times 10^{-6} \text{ Pa}$, which was a half of the measured ΔP_s . The existence of the beam flow component would be much more than considering. Spike noises were piled on the signals while the head were moving, since the

signal currents are extremely low as the order of nA range and static charges are generated by a friction of bending the signal cable.

This head should be as compact and light as possible when it is mounted on a vacuum manipulator. Furthermore, a use of a shorter quadrupole electrode though it would bring about a lower resolution, and a use of the microchannelplate would be effective for miniaturizing. A use of the ceramics insulated coaxial cable that is intrinsically damage free is feasible.

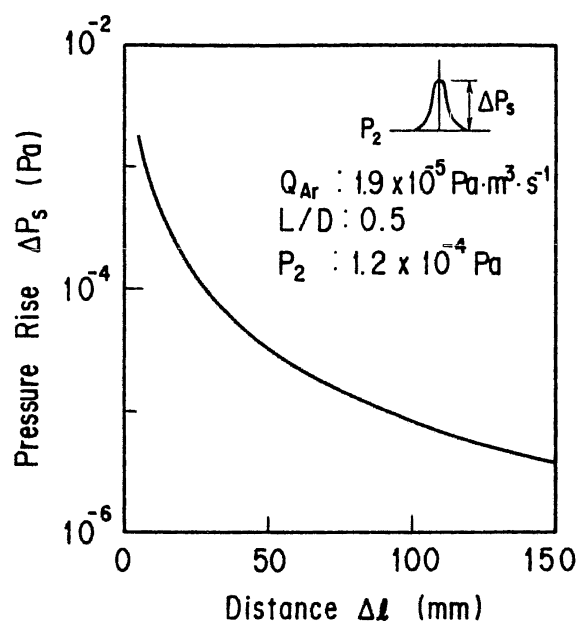


Figure 33. Calculated Ar pressure rise (ΔP_s) as a function of a distance ΔL . L/D shows a size ratio of the collimator.

6. Conclusions

Design of the ITER leak detection system was overviewed and a reasonable leak detection strategy was proposed, in which the leak detection system is comprised of MSLDs, RGA units and calibration units with different specifications. The MSLD has both gross and fine leak test modes that can be applied during roughing and high vacuum pumping, respectively. The MSLD type 1 has to detect a minimal $10^{-10} \text{ Pa} \cdot \text{m}^3/\text{s}$ of ^4He leakage in a background hydrogen isotope atmosphere. For leak detection of in-vessel water-cooling channels, a drain and dry out scheme of water circuits and resultant ^4He leak testing are a base design concept. When the localized drain and dry scheme is impossible due to aspects such as the nuclear heating, an in-situ leak detection with fully circulating water should be implemented, where a water-soluble tracer is introduced into the suspected water channel and the leaked tracer into the torus can be detected by the RGA unit. The in-vacuum side water leak detection is also implemented, where a unidirectional total pressure gauge or QMS head is scanned using a vacuum compatible manipulator. After the drain and dry of the water channel, a sniffer

method and a helium entrainment method with no vacuum compatible manipulator are other options.

The HR-QMS using the zone II condition in the Mathieu diagram has been developed for use in the MSLD, where the 10^{-4} peak ratio of $^4\text{He}^+/\text{D}_2^+$ and the 0.1 of $^3\text{He}^+/\text{HD}^+$ could be obtained with the 3.58 MHz prototype. The filtering effect for ions on the zone II condition is stronger, and the peak tailing due to transmission of unstable ions is much reduced. Hence, the decrease in peak tailing brings a high resolution.

An improved water leak detection method with fully circulating the water has been demonstrated, in which Kr was dissolved in water and the leaked Kr in vacuum was detected by the QMS. For the target water leak rate of less than $10^{-2} \text{ Pa}\cdot\text{m}^3/\text{s}$, $10^{-3} \text{ Pa}\cdot\text{m}^3/\text{s}$ of the single water leak could be detected and located with the positional accuracy of 5.9 % (0.24 m for 4 m difference) for 20A auxiliary test pipes without heating. For $10^{-3} \text{ Pa}\cdot\text{m}^3/\text{s}$ of two water leak valves with 30 °C heating, the positional accuracy of 12.6 % (0.126 m for 1 m distance) was obtained, based on the natural convection flow calculation.

Another valuable water leak detection method with fully circulating water has been developed, namely, a total pressure gauge or the QMS head was mechanically scanned around the suspected area by using a vacuum compatible manipulator. A unidirectional total pressure gauge was scanned near the simulated leak nozzle and the minimum $10^{-8} \text{ Pa}\cdot\text{m}^3/\text{s}$ level of air leaks could be detected at the background pressures of 10^{-5} Pa . A special QMS head equipped with a vacuum tight rf tuning package operable at more than 150 °C was moved around the Ar leak nozzle, and could detect the measured ΔP_s of $1.08 \times 10^{-5} \text{ Pa}$ at $Q_{\text{Ar}} = 1.9 \times 10^{-5} \text{ Pa}\cdot\text{m}^3/\text{s}$.

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Chapter 11

A D-³He SPHERICAL TOKAMAK REACTOR WITH THE PLASMA CURRENT RAMP-UP BY VERTICAL FIELD

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Abstract

We propose the D-³He spherical tokamak (ST) reactor with the major radius of $R_0=5.6$ m, the minor radius of $a=3.4$ m, the aspect ratio of $A=1.65$, the elongation of $\kappa=3$, and the toroidal field of $B_{t0}=4.4$ T with the beta value of $\langle\beta_t\rangle=40\%$. The large plasma current of ~ 90 MA is ramped up by the external heating (200~300 MW) and fusion product heating power, and the vertical field effect. This large plasma current provides the wider operational regime. The required confinement enhancement factor over IPB98(y,2) scaling is 1.8~2.5 for the density profile of $\alpha_n=0.5$ and parabolic temperature profiles ($\alpha_T=1.0$). The minimum neutron power is as low as 52.5 MW for fuel ratio D : ³He = 0.40 : 0.60, if the particle to energy confinement time ratio of 2 by the powerful pumping and the large wall reflectivity $R_{eff}=0.99$ are achieved without spin polarization. The bremsstrahlung and synchrotron radiation losses to the first wall (heat flux $\sim 1\text{MW/m}^2$) could be converted to an electric power of 1 GW through the super-critical pressurized water coolant or the organic coolant together with the divertor heat load. New operation scenario of "quasi-continuous cyclic DC operation" is proposed for steady state electric power output in a D-³He ST reactor when the bootstrap current fraction is less than 100 %.

1. Introduction

Recently the high beta plasma of $\langle\beta_t\rangle\sim 40\%$ and high normalized beta of $\beta_N\sim 6.8$ have been achieved in NSTX spherical tokamak (ST) (Here $\langle\beta_t\rangle$ is defined by $\langle\beta_t\rangle=2\mu_0\langle p\rangle/B_{t0}^2$, $\beta_N=\langle\beta_t\rangle/(I_p/aB_{t0})$ with $\langle p\rangle$ being the volume averaged plasma pressure and B_{t0} being the vacuum magnetic field at the center, which is used throughout this paper) [1]. This results is encouraging a design of a deuterium and helium 3 fuel (D-³He) ST reactor because of the

small synchrotron radiation loss due to the low toroidal field. On the other hand, plasma current ramp-up in a ST has been considered to be a difficult [2][3] due to no space for the central solenoid to induce the plasma current. To overcome this problem we have proposed a current ramp-up scenario in a tokamak using the large flux available from the outer vertical field coils together with the heating power [4][5][6].

In a high temperature D-³He tokamak reactor, the non-inductive plasma current ramp-up without central solenoid needs a prohibitively long time due to a large resistive decay time (~several hours). Even when the central solenoid can be equipped, its flux is not enough to induce the full plasma current larger than 80 MA. To solve the plasma current ramp-up problem in a D-³He spherical tokamak reactor, it was first pointed out by author [7] that the flux from the vertical field Φ_{BV} is the same order of the inductive flux for the plasma current ramp-up Φ_p , and then the study of the plasma current ramp-up by the vertical field and heating power had been started. However, theoretical works had been limited to only D-T reactors [4][5][6] due to complication of D-³He reactor analysis.

So far pioneering work on ignition study had been conducted in a D-³He spherical tokamak with the aspect ratio of $A=1.2$, the minor radius of $a=2.0$ m, the toroidal field of $B_{t0} = 2$ T and the fusion power of 2 GW range for the steady state in terms of the wall reflectivity, beta and so on [8]. The D-³He spherical tokamak reactor with the major radius of $R_0 = 5.25$ m, the minor radius of $a=3.75$ m, the toroidal field of $B_{t0} = 2.7$ T, and the plasma current of $I_p=73$ MA was also discussed as an extension of D-T spherical tokamak studies [9]. Although the plasma current ramp-up is crucially important to a D-³He spherical tokamak reactor, detailed studies on temporal evolution of ignition including the plasma current ramp-up have not yet been conducted. Recent progress of experimental and numerical studies on the plasma current ramp-up by the heating power and vertical field in various tokamaks without central solenoid, such as JT-60U [5][10]-[12], TST-2 [13], and MAST [14][15], also encourage the further study on the current ramp-up and detailed studies on an ignition in a D-³He ST reactor using recent experimental results.

In this work, we survey the parameter regime for D-³He ignition in a proposed ST reactor using the zero-dimensional power and particle equations. Machine parameters are the major radius of $R_0 = 5.6$ m, the minor radius $a = 3.4$ m, the aspect ratio $A = 1.65$, the toroidal field $B_{t0} = 4.4$ T and the elongation of $\kappa = 3$. Although the proposed ST reactor has no central solenoid, the plasma current is ramped up by the heating power and the vertical field up to ~100 MA enough for D-³He ignition.

With application of 200~300 MW heating power and good confinement factor of $\gamma_{HH} = 2.5$, the plasma temperature is initially increased to ~ 100 keV and the plasma current is ramped up to ~ 50 MA. After switching off the heating power in the ignition phase, the plasma current is further ramped up to 80 ~ 100 MA at 250 s with the fusion power. This large plasma current induced by the heating power and the vertical field provides the wide operation regime, such as larger ³He/D fuel ratio to decrease the neutron wall loading and allow flatter density profile, etc. As a result, the confinement enhancement factor of 1.8~2.5 is required over the IPB98(y,2) scaling [16]. The first wall heat flux is ~1 MW/m² due to the wide plasma surface area in a ST configuration. The low wall reflectivity down to 0.35 for synchrotron radiation loss is allowed due to the large beta available in a ST. The superconducting toroidal coil can be used together with the blanket and shield thickness of ~80 cm for the maximum toroidal field of 20.5 T corresponding to the central toroidal field of

4.4 T. The sum of proton and helium ash fractions is 7 to 15 % with the particle confinement to energy confinement time ratio up to the presently achieved value of 4. The bremsstrahlung and synchrotron radiation losses to the first wall could be converted to an electrical power through the super-critical pressurized water coolant or the organic coolant together with the divertor heat load.

In addition, we can reduce the neutron power by achieving the small particle to energy confinement time ratio for fuel and ash particles of $\tau_D^*/\tau_E = \tau_{HE3}^*/\tau_E = \tau_T^*/\tau_E = \tau_p^*/\tau_E = \tau_\alpha^*/\tau_E = 2$, and the large wall reflectivity of $R_{eff}=0.99$. If D-³He reaction is further enhanced by 50 % and D-D reaction is suppressed by nuclear spin polarization, we can further reduce the total neutron power down to $P_n \sim 1.8$ MW in principle. In this sense, we call this ST reactor as “Final (Fusion Ignitor with Neutron Alleviated) ST”.

This paper is organized as follows. In section 2, formalisms are presented for the zero-dimensional particle and power balance equations for D-³He fuels and ignition control algorithm. In section 3, calculated results on the temporal evolution of the plasma current ramp-up by the heating power and vertical field with ignition access and various operation modes are presented. In section 4, various reactor issues of a D-³He ST are discussed. In sections 5, summary is given. In the appendix, we present the equivalent circuit equation employed in this study.

2. Formalism for Ignition Control

We have combined the circuit equation with the zero-dimensional particle and power balance equations using the IPB98(y,2) confinement time scaling law as presented in the appendix. We have used the H-mode power threshold scaling and the detailed control algorithm of the fueling and heating/current drive power as shown here.

2.1. Zero-Dimensional Particle Balance Equations

The particle balance equations for D-³He fuel fusion are given for deuterium, helium-3, tritium, proton and helium-4 as presented in the paper [17]. The D particles are lost by the D-T, D-D and D-³He reactions, where D-D reactions lose two deuterons. The helium-3 is produced by D-D and lost by D-³He reactions. The tritium is created by D-D and lost by D-T reactions. Alpha ash (⁴He) is created by D-T and D-³He reactions and proton ash is produced by D-D and D-³He reactions. In D-D reactions, identical particles are taken into account.

Deuterium particle balance equation is

$$\begin{aligned} \frac{dn_D(0)}{dt} = & (1 + \alpha_n)S_D(t) - \frac{n_D(0)}{\tau_D^*} \\ & - (1 + \alpha_n) \left[n_D(0)n_T(0)\overline{\langle \sigma v \rangle}_{DT}(x) + 2 \frac{n_D(0)^2}{2} \left\{ \overline{\langle \sigma v \rangle}_{DDPT}(x) + \overline{\langle \sigma v \rangle}_{DDHE3N}(x) \right\} + n_D(0)n_{HE3}(0)\overline{\langle \sigma v \rangle}_{DHE3}(x) \right] \end{aligned} \quad (2.1)$$

^3He particle balance equation is

$$\frac{dn_{\text{HE3}}(0)}{dt} = (1 + \alpha_n)S_{\text{HE3}}(t) + (1 + \alpha_n) \left\{ \frac{n_D(0)^2}{2} \overline{\langle \sigma v \rangle_{\text{DDHE3N}}(x)} - n_D(0)n_{\text{HE3}}(0) \overline{\langle \sigma v \rangle_{\text{DHE3}}(x)} \right\} - \frac{n_{\text{HE3}}(0)}{\tau_{\text{HE3}}^*} \quad (2.2)$$

The tritium particle balance equation is

$$\frac{dn_T(0)}{dt} = (1 + \alpha_n) \left\{ \frac{n_D(0)^2}{2} \overline{\langle \sigma v \rangle_{\text{DDPT}}(x)} - n_D(0)n_T(0) \overline{\langle \sigma v \rangle_{\text{DT}}(x)} \right\} - \frac{n_T(0)}{\tau_T^*} \quad (2.3)$$

He ash particle balance equation is

$$\frac{dn_\alpha(0)}{dt} = (1 + \alpha_n) \left\{ n_D(0)n_T(0) \overline{\langle \sigma v \rangle_{\text{DT}}(x)} + n_D(0)n_{\text{HE3}}(0) \overline{\langle \sigma v \rangle_{\text{DHE3}}(x)} \right\} - \frac{n_\alpha(0)}{\tau_\alpha^*} \quad (2.4)$$

Proton particle balance equation is

$$\frac{dn_p(0)}{dt} = (1 + \alpha_n) \left\{ \frac{n_D(0)^2}{2} \overline{\langle \sigma v \rangle_{\text{DDPT}}(x)} + n_D(0)n_{\text{HE3}}(0) \overline{\langle \sigma v \rangle_{\text{DHE3}}(x)} \right\} - \frac{n_p(0)}{\tau_p^*} \quad (2.5)$$

where $n_D(0)$, $n_T(0)$, $n_{\text{HE3}}(0)$, $n_\alpha(0)$, and $n_p(0)$ are the deuterium, tritium, Helium-3, Helium-4 (helium ash), and proton ash density, respectively. τ_D^* , τ_{HE3}^* , τ_T^* , τ_p^* and τ_α^* are the effective particle confinement time including the recycling and pumping effect for each species, and $S_D(t)$ and $S_{\text{HE3}}(t)$ are the deuterium and ^3He fueling rate externally supplied, respectively. $\overline{\langle \sigma v \rangle_{\text{DT}}(x)}$ is the volume averaged D-T fusion rate, $\overline{\langle \sigma v \rangle_{\text{DHE3}}(x)}$ is the volume averaged D- ^3He fusion rate, $\overline{\langle \sigma v \rangle_{\text{DDPT}}(x)}$ is the volume averaged D-D fusion rate producing proton and tritium, and $\overline{\langle \sigma v \rangle_{\text{DDHE3N}}(x)}$ is the volume averaged D-D fusion rate producing helium-3 and neutron. We note that fusion reaction has a factor of 1/2 due to one fused particle from same kinds of deuterons and the loss term has a factor of 1 due to disappearance of two deuterons. Here, the temperature and density profiles are assumed as $T_i(x)/T_i(0) = T_e(x)/T_e(0) = (1 - x^2)^{\alpha_T}$, and $n_e(x)/n_e(0) = n_D(x)/n_D(0) = n_{\text{HE3}}(x)/n_{\text{HE3}}(0) = n_T(x)/n_T(0) = n_p(x)/n_p(0) = n_\alpha(x)/n_\alpha(0) = (1 - x^2)^{\alpha_n}$. The charge neutrality condition is used for the electron density and the peak value is obtained by the volume averaged formula as

$$n_e(0) = n_D(0) + n_T(0) + 2n_{\text{HE3}}(0) + 2n_\alpha(0) + n_p(0) + (1 + \alpha_n)Zn_{\text{imp}} \quad (2.6)$$

Impurity density n_{imp} is assumed to be spatially constant with charge Z . This is the global charge neutral condition and not the local charge neutral condition, which is inherent to 0-dimensional analysis.

2.2. Zero-Dimensional Power Balance Equations

In this study the ion and electron power balance equation were separately treated to take the hot ion mode effect into accounts. The sum of the ion ($\bar{P}_{Li}$) and electron ($\bar{P}_{Le}$) conduction loss should be equal to the total conduction loss $\bar{P}_L$

$$\bar{P}_{Li} + \bar{P}_{Le} = \bar{P}_L \quad (2.7)$$

The total conduction loss $\bar{P}_L$ is calculated by the total confinement time such as IPB98(y,2) scaling. The ion conduction loss is defined by

$$\bar{P}_{Li} = \frac{\frac{3}{2}k(n_D + n_{HE3} + n_T + n_p + n_\alpha)T_i}{\tau_{Ei}} \quad (2.8)$$

and the electron conduction loss by

$$\bar{P}_{Le} = \frac{\frac{3}{2}k(n_e)T_e}{\tau_{Ee}} \quad (2.9)$$

Using $\tau_{Ei} = \gamma_{ci} \tau_{Ee}$ and the profile effect, Eq.(2-7) can be written as

$$\frac{1.5e(f_D + f_{HE3} + f_T + f_p + f_\alpha)n(0)T_i(0)}{\gamma_{ci}\tau_{Ee}(1 + \alpha_n + \alpha_T)} + \frac{1.5en(0)T_e(0)}{\tau_{Ee}(1 + \alpha_n + \alpha_T)} = \bar{P}_L \quad (2.7')$$

The electron confinement time τ_{Ee} is now obtained, and then the ion confinement time τ_{Ei} can be calculated. Finally, the ion conduction loss is given by

$$\bar{P}_{Li}[M / m^3] = \frac{(f_D + f_{HE3} + f_T + f_p + f_\alpha)}{\gamma_{ci} \left\{ \frac{(f_D + f_{HE3} + f_T + f_p + f_\alpha)}{\gamma_{ci}} + \frac{T_e(0)}{T_i(0)} \right\}} \bar{P}_L \quad (2.8')$$

and the electron conduction loss by

$$\bar{P}_{Le} [W / m^3] = \left[1 - \frac{(f_D + f_{HE3} + f_p + f_\alpha)}{\gamma_{ci} \left\{ \frac{(f_D + f_{HE3} + f_p + f_\alpha)}{\gamma_{ci}} + \frac{T_e(0)}{T_i(0)} \right\}} \right] \bar{P}_L \quad (2.9')$$

We have used the ion to the electron confinement time ratio $\gamma_{ci} = \tau_{Ei} / \tau_{Ee} = 6$ in this study unless otherwise noted.

The ion power balance is given by

$$\frac{dW_i}{dt} = \bar{P}_{FP} F_{ion} + P_{EXT} F_{Pi} / V_o - \bar{P}_{ie} - \bar{P}_{Li} \quad (2.10)$$

and the electron power balance by

$$\frac{dW_e}{dt} = \bar{P}_{FP} (1 - F_{ion}) + P_{EXT} (1 - F_{Pi}) / V_o + \bar{P}_{oh} + \bar{P}_{ie} - \bar{P}_b - \bar{P}_s - \bar{P}_{Le} \quad (2.11)$$

where $\bar{P}_{FP}$ is the fusion product heating power per unit volume, F_{ion} is the energy transfer fraction of the fusion power to ion, P_{EXT} is the external heating power, F_{Pi} is the power fraction to ions from the external heating power, V_o is the plasma volume, $\bar{P}_{oh}$ is the Ohmic heating power, $\bar{P}_b$ is the bremsstrahlung loss including the relativistic effect for electron-electron and electron-ion interactions per unit volume [21], $\bar{P}_s$ is the synchrotron radiation loss including the torus effect per unit volume [22] as given in Appendix A.1, $\bar{P}_{ie}$ is the transferred power by collisions due to the ion and electron temperature difference. This term is expressed by

$$\bar{P}_{ie} [W / m^3] = \frac{2.4 \times 10^5 (n_e(0) [m^{-3}] / 10^{20})^2}{1 + 2\alpha_n - 0.5\alpha_T} \left(\frac{f_D}{A_D} + \frac{4f_{HE3}}{A_{HE3}} + \frac{f_p}{A_p} + \frac{4f_\alpha}{A_\alpha} + \frac{f_T}{A_T} \right) \ln \Lambda_j \frac{\left(\frac{T_i(0)}{T_e(0)} - 1 \right)}{T_e(0) [keV]^{0.5}} \quad (2.12)$$

where A_D , A_{HE3} , A_p , A_α and A_T are the mass number of deuteron, Helium-3, proton, Helium-4 and triton, respectively, $\ln \Lambda = 20$ and the profile effects are taken into account. We have used the fixed parameters of the power fraction to ions from the external heating power of $F_{Pi} = 0.5$, and the fusion power fraction to ions of $F_{ion} = 0.4$ which is based on the nuclear elastic scattering [23], unless otherwise noted.

In Eqs.(2-10) and (2-11) the ion energy W_i and electron energy W_e are defined by

$$\begin{aligned}\overline{W}_i &= 1.5k \overline{(n_D + n_{HE3} + n_p + n_\alpha + n_T)} T_i \\ &= \frac{1.5 \times 1.6 \times 10^{-19} (n_D(0) + n_{HE3}(0) + n_p(0) + n_\alpha(0) + n_T(0)) T_i(0) [eV]}{(1 + \alpha_n + \alpha_T)}\end{aligned}\quad (2.13)$$

and

$$\begin{aligned}\overline{W}_e &= 1.5k n_e \overline{T_e} \\ &= \frac{1.5 \times 1.6 \times 10^{-19} n_e(0) T_e(0) [eV]}{(1 + \alpha_n + \alpha_T)}\end{aligned}\quad (2.14)$$

Therefore, the ion temperature and electron temperature are calculated by, respectively,

$$\begin{aligned}\frac{dT_i(0)}{dt} &= \frac{(1 + \alpha_n + \alpha_T)}{1.5 \times 1.6 \times 10^{-19} (f_D(0) + f_{HE3}(0) + f_p(0) + f_\alpha(0) + f_T(0)) n_e(0)} [\overline{P}_{PF} F_{ion} + P_{EXT} F_{Pi} / V_o - \overline{P}_{ie} - \overline{P}_{Li}] \\ &\quad - \frac{T_i(0)}{(f_D(0) + f_{HE3}(0) + f_p(0) + f_\alpha(0) + f_T(0)) n_e(0)} \left[\frac{dn_D(0)}{dt} + \frac{dn_{HE3}(0)}{dt} + \frac{dn_p(0)}{dt} + \frac{dn_\alpha(0)}{dt} + \frac{dn_T(0)}{dt} \right]\end{aligned}\quad (2.10')$$

$$\begin{aligned}\frac{dT_e(0)}{dt} &= \frac{(1 + \alpha_n + \alpha_T)}{1.5 \times 1.6 \times 10^{-19} n_e(0)} [\overline{P}_{FP} (1 - F_{ion}) + P_{EXT} (1 - F_{Pi}) / V_o + \overline{P}_{oh} + \overline{P}_{ie} - \overline{P}_s - \overline{P}_b - \overline{P}_{Le}] \\ &\quad - \frac{T_e(0)}{(1 - (1 + \alpha_n) Z f_{imp}) n_e(0)} \left[\frac{dn_D(0)}{dt} + 2 \frac{dn_{HE3}(0)}{dt} + \frac{dn_p(0)}{dt} + 2 \frac{dn_\alpha(0)}{dt} + \frac{dn_T(0)}{dt} \right]\end{aligned}\quad (2.11')$$

where V_o is the plasma volume. $[dn_D(0)/dt]$ to $[dn_T(0)/dt]$ terms in Eqs.(2-10') and (2-11') are given by the particle balance equations (2-1) to (2-5). The fusion product heating power per unit volume is $\overline{P}_{FP} = \overline{P}_{DHE3} + \overline{P}_{DDPT} + \overline{P}_{DDHE3N} + \overline{P}_{DT}$ where $\overline{P}_{DHE3} = n_D(0) n_{HE3}(0) \langle \sigma v \rangle_{DHE3}(x) (3.7 + 14.7) \times 10^6 \times 1.6 \times 10^{-19}$ is the fusion product heating power from D-³He reaction, $\overline{P}_{DDPT} = \{n_D(0)^2 / 2\} \langle \sigma v \rangle_{DDPT}(x) (1.02 + 3.01) \times 10^6 \times 1.6 \times 10^{-19}$ is the fusion product heating power from D-D reaction producing proton and tritons, $\overline{P}_{DDHE3N} = \{n_D(0)^2 / 2\} \langle \sigma v \rangle_{DDHE3N}(x) 0.82 \times 10^6 \times 1.6 \times 10^{-19}$ is the fusion product heating power from D-D reaction producing ³He and 2.45 MeV neutrons, and $\overline{P}_{DT} = n_D(0) n_T(0) \langle \sigma v \rangle_{DT}(x) 3.52 \times 10^6 \times 1.6 \times 10^{-19}$ is the fusion product heating power from D-T reaction. The total fusion power including the neutron power is $\overline{P}_f = \{\overline{P}_{DHE3} + \overline{P}_{DDPT} + \overline{P}_{DDHE3N} (0.82 + 2.45) / 0.82 + \overline{P}_{DT} (3.52 + 14.06) / 3.52\} V_o$.

Here $f_D = n_D(0)/n_e(0)$, $f_{HE3} = n_{HE3}(0)/n_e(0)$, $f_T = n_T(0)/n_e(0)$, $f_p = n_p(0)/n_e(0)$, $f_\alpha = n_\alpha(x)/n_\alpha(0)$ and $f_{imp} = n_{imp}/n_e(0)$.

The particle confinement times are set as $\tau_D^*/\tau_E = \tau_{HE3}^*/\tau_E = \tau_T^*/\tau_E = \tau_p^*/\tau_E = \tau_\alpha^*/\tau_E = 2 \sim 4$ relative to the global energy confinement time τ_E in this study and the prompt loss of fusion products are assumed to be zero. The IPB98(y,2) confinement time scaling has been used as given in Appendix A.2.

2.3. Control Algorithm of the Fusion Power and the Heating Power

The total fueling of D-³He fuels is controlled by the fusion power P_f with the simple proportional-integration-derivative (PID) or PI controller and fuel ratio control algorithm is added in cascade as presented in Appendix A.3 [24]. As our ultimate purpose of D-³He fusion reactor study is to reduce the neutron power as small as possible, D and ³He fuel ratio is important parameter because it determines the neutron power. The fuel ratio is defined by $n_D(0) : n_{HE3}(0) = n_D(0)/(n_D(0) + n_{HE3}(0)) : n_{HE3}(0)/(n_D(0) + n_{HE3}(0))$. The external heating power is applied to keep the H-mode using the H-mode power threshold as given in Appendix A.4 [6][20][25].

2.4. Machine Parameters and Plasma Cross Section in Final ST

Choice of machine parameters is important from the view point of the reactor compatibility. As machine size and plasma parameters are interdependent, integrated approach is necessary. The energy conversion using the hot coolant determines the first wall surface area because the first wall heat flux should be smaller than 1 MW/m² for the present technology. At the same time the neutron wall loading can be reduced by the large surface area. The plasma minor radius also determines the plasma current through the edge safety factor and hence the plasma confinement. The larger minor radius also decreases the plasma self-inductance, helping the plasma current ramp-up.

As shown in Fig. 1, the machine size (the major radius $R_o = 5.6$ m and the minor radius $a = 3.4$ m, and the elongation $\kappa = 3$) is large in the vertical direction. Here the overall plasma cross section of ITER is shown for comparison. Although the elliptic plasma cross-section has been approximately used in this estimation, the actual plasma shape should be a triangular shape as shown in Fig. 1 for higher beta and higher normalized beta [1]. However, as recent NSTX experimental results show that the plasma rotation provides the higher beta [26], it may be concern that such highly elongated triangular plasma shape can have such high rotation velocity. As the high performance plasma may be sensitive to the plasma cross section, more work on reactor design should be conducted if this conceptual trial looks promising.

The high temperature superconducting coil made by the Bi1212/Ag round shaped wire with tape-shaped multi-filaments ($J_{op} = 500\text{MA/m}^2$, $B_{op} = 20 \sim 25$ T, and $T_{op} = 20$ °K) [27], proposed for “Vector” [28], may be a good candidate for this ST reactor because the allowable maximum toroidal field is very high as 23 T. The maximum toroidal field at the radius of the inner toroidal leg of $\Delta_{BT} = 1.2$ m is given by $B_{max} = B_{to}R_o/\Delta_{BT} = 20.5$ T for the toroidal field $B_{to} = 4.4$ T at the major radius of $R_o = 5.6$ m, the minor radius of $a = 3.4$ m, the

blanket shield of $\Delta_{BL}=0.8$ m and the scrape-off layer of 0.2 m. The total coil current of B_T coil is $I_{BT} = 2\pi R_0 B_{t0}/\mu_0 = 123.2$ MA, and then the current density is $J_{BT} = I_{BT}/(\pi \Delta_{BT}^2) = 27.23$ MA/m², which is much smaller than 500 MA/m², allowing the stabilizer. The mechanical stress exerted on the inner toroidal leg is $\sigma_r = \sigma_\theta = B_{max}^2/2\mu_0 = 170$ MPa, which is smaller than the maximum stress of structural material JN1 or JJ1 (700~800 MPa) [28]. Therefore, the aspect ratio is finally $R_0/a = (\Delta_{BT} + \Delta_{BL} + 0.2 + a)/a = 1.65$.

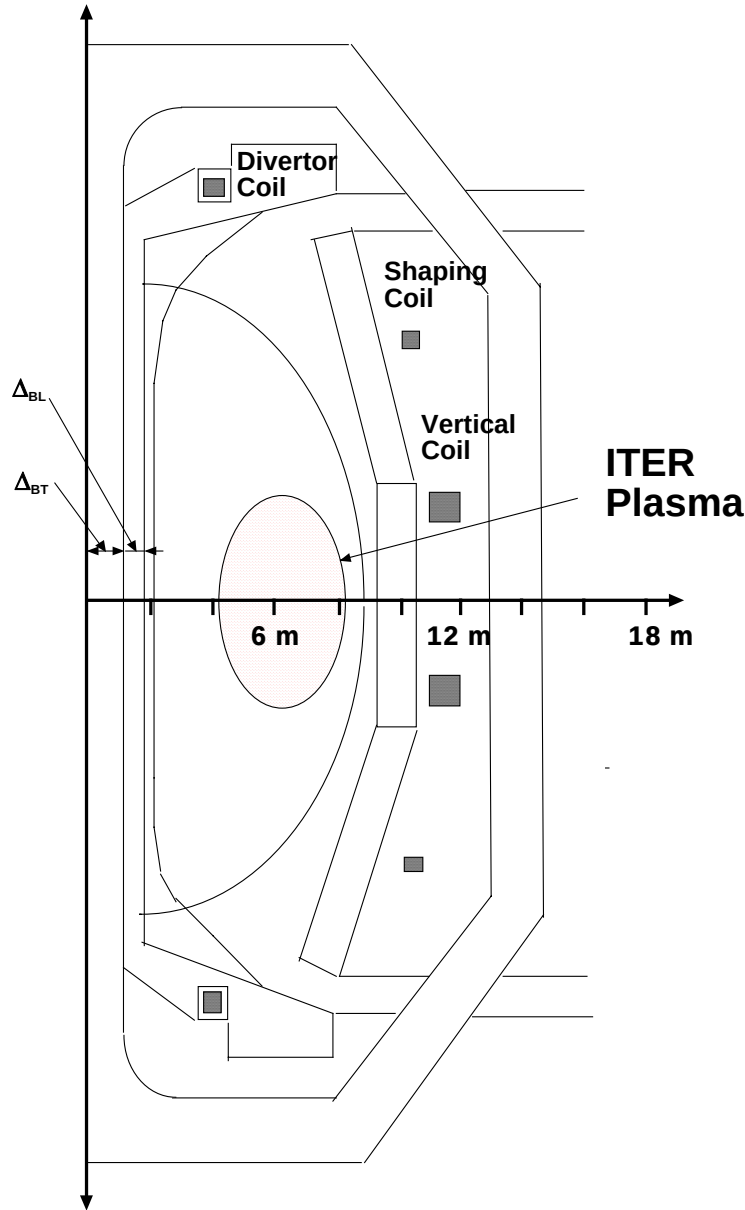


Figure 1. The plasma cross section and poloidal coil arrangement in the Final ST reactor ($R_0 = 5.6$ m, $a = 3.4$ m, $\kappa = 3$ and $B_{t0} = 4.4$ T).

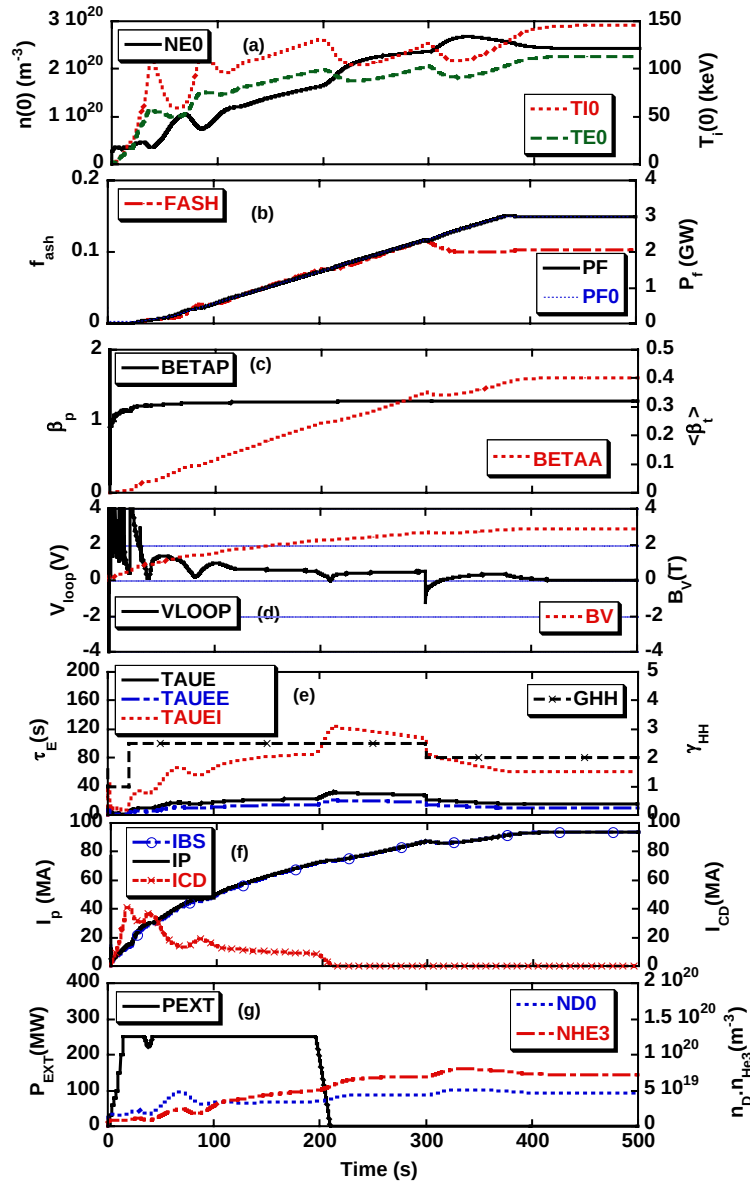


Figure 2. Temporal evolutions of the plasma parameters in the case of $F_{\text{ion}}=0.4$ and $\tau_{\text{Ei}}/\tau_{\text{Ee}}=6$. The coefficient of the bootstrap current is assumed to be $C_{\text{BS}} = 1.0$, and the internal inductance of $\ell_i = 0.42$. Other parameters are $\alpha_n=0.5$, $\alpha_T=1.0$, $\tau_D^*/\tau_E = \tau_{\text{HE3}}^*/\tau_E = \tau_T^*/\tau_E = \tau_p^*/\tau_E = \tau_\alpha^*/\tau_E = 2$, $R_{\text{eff}} = 0.9$, $P_f = 3$ GW, $\tau_{\text{rise}} = 180$ s, $\gamma_{\text{HH}} = 2.5$ to 2.0 , and PID fueling control ($T_{\text{int}} = 10$ s and $T_d = 0.5$ s). In the steady state the fuel ratio is $n_D : n_{\text{HE3}} = 0.42 : 0.58$. (a) the ion temperature $T_i(0)$, electron temperature $T_e(0)$ and density $n(0)$, (c) the poloidal beta β_p and average toroidal beta β_t , (d) the loop voltage V_{loop} by the vertical field term $[dB_v/dt]$ and the vertical equilibrium field B_v , (e) the ion confinement time τ_{Ei} , the electron confinement time τ_{Ee} , the total confinement time τ_{E} , and confinement enhancement factor γ_{HH} . (f) the plasma current I_p , the non-inductively driven current I_{CD} , and the bootstrap current I_{BS} , (g) the heating/current drive power P_{EXT} , and ^3He (NHE3) and D (ND0) ion densities.

2.5. Poloidal Coil Layout

Particle and power balance equations are combined with the plasma circuit equation to calculate the plasma current evolution as given in the Appendix A.5. The plasma cross section with the elongation of $\kappa = 3$ is determined by assumed three single turn plasma current, and the shaping coils, vertical coils and the divertor coil as shown in Fig. 2, where the positions are $(R_{sh}, H_{sh}) = (10.5 \text{ m}, \pm 8.5 \text{ m})$, $(R_v, H_v) = (11.5 \text{ m}, \pm 3.0 \text{ m})$, and $(R_{div}, H_{div}) = (4.0 \text{ m}, \pm 13.0 \text{ m})$, respectively. A set of the mutual inductances between the plasma center and the three types of the poloidal coils, and the vertical fields per unit coil current from each coil are $(M_{psh} = 4.793 \times 10^{-6} \text{ H}, B_{zosh} = 4.848 \times 10^{-8} \text{ T/A})$, $(M_{pv} = 9.521 \times 10^{-6} \text{ H}, B_{zov} = 1.096 \times 10^{-7} \text{ T/A})$, and $(M_{pdiv} = 5.974 \times 10^{-7} \text{ H}, B_{zodiv} = 5.307 \times 10^{-9} \text{ T/A})$, respectively. We should note that the poloidal coil arrangement is not so influential to the plasma current ramp-up, because the effective plasma inductance is not largely altered by the poloidal coil arrangement. Actually, for the ratios of the divertor coil current to plasma current ($\alpha_{div} = I_{div}/I_p = 5/80$) and the shaping coil current to the vertical coil current ($\alpha_{sh} = I_{sh}/I_v = 1/4$) in this case, the plasma inductances given by the tight aspect approximation [6][29] are $L_p = 2.77 \text{ } \mu\text{H}$ and $L_{peff} = 2.778 \text{ } \mu\text{H}$ for $\ell_i = 0.42$. The inductance difference between these values is small because the current ramp-up process is mainly determined by the vertical field effect, not by the poloidal coil arrangement. More accurate calculations of the plasma current evolution using the optimized poloidal coil layout, plasma shape and the equilibrium calculation code are necessary, which is beyond the scope of this study.

If the Ohmic (OH) transformer solenoid is placed on the center leg of the toroidal coil $R_{OH} = 1.2 \text{ m}$ with the maximum solenoid field $B_{OH} = 10 \text{ T}$, the OH flux is $\Phi_{OH} = 2(\pi R_{OH}^2)B_{OH} = 90 \text{ Vs}$ which is much smaller than the required value of the inductive flux $L_p I_p \sim 210 \text{ Vs}$. For $B_{OH} = 20 \text{ T}$, we have still lower flux $\Phi_{OH} = 180 \text{ Vs}$.

3. Calculated Results on D-³He Ignition

We present calculated results on the ignition and plasma current ramp-up to $\sim 100 \text{ MA}$ by the vertical field effect.

3.1. Reference Ignition Scenarios with the Plasma Current Ramp-Up

The evolution of the plasma parameter is calculated using the equivalent circuit equation (A-18) and particle and power balance equations. As shown in Fig. 2-(g), the heating/current drive power is applied up to 250 MW for 200 s , and then automatically switched off by the ignition control algorithm when ignition is reached. The plasma current is initially ramped-up to 70 MA by the loop voltage V_{loop} induced by the vertical field B_v , which is due to the increase in the plasma energy by the external heating power [30]. The non-inductive driven current appears during the application of the heating power, and becomes zero after 200 s because it is switched off in ignition. The bootstrap current (described by x) is comparable to the plasma current (solid line) for the bootstrap

coefficient of $C_{BS}=1.0$ in Eq.(A-15), and the total plasma current reaches $I_p \sim 93.6$ MA at 500 s.

Here, the ion to the electron confinement time ratio is $\tau_{Ei}/\tau_{Ee} = 6$ and the particle to energy confinement time ratio is assumed to be $\tau_D^*/\tau_E = \tau_{HE3}^*/\tau_E = \tau_T^*/\tau_E = \tau_p^*/\tau_E = \tau_\alpha^*/\tau_E = 2$ and the internal inductance of $\ell_i = 0.42$. We call this case the reference case. The confinement factor is assumed to be $\gamma_{HH} = 2.5$ at 20 s in the initial current ramp-up phase, and reduced to 2.0 at 300 s in the flat top phase to decrease fusion product ashes as shown in Fig. 2-(e). As the confinement factor set here is artificial, actual control of the confinement time itself should be developed somehow by, for example, the plasma shape control etc in a D-³He reactor. The square root density profile of $\alpha_n = 0.5$, parabolic temperature profile of $\alpha_T = 1.0$, and the wall reflectivity of $R_{eff} = 0.9$ are assumed. The fuel ratio of $n_D : n_{HE3} = 2/3 : 1/3$ has been used to increase the fusion reactivity during the initial low fusion power phase, and then fuel ratio feedback control is switched on at $t = 100$ s to have D ion density $n_D(0)$ and ³He ion density $n_{HE3}(0)$ with the ratio of $n_D : n_{HE3} = 0.42 : 0.58$ as seen in Fig. 2-(g). The electron density slowly increases to the steady state with $2.43 \times 10^{20} \text{ m}^{-3}$ (Fig. 2-(a)), which is smaller than the Greenwald density limit (converted to the peaked value) $n_{GW}(0) = (4/\pi) (I_p(\text{MA})/\pi a^2) \times 10^{20} \text{ m}^{-3} = 3.28 \times 10^{20} \text{ m}^{-3}$. The ion temperature is also increased to 145 keV, the fusion power reaches 3 GW and the sum of the proton and alpha fraction becomes 10.3 %. In the steady state, the toroidal beta reaches $\langle \beta_t \rangle = 40.3$ %, the beta poloidal $\beta_p = 1.28$, and the neutron power is 60.4 MW and neutron wall loading is $\Gamma_n = 0.034 \text{ MW/m}^2$. Detailed values are listed in Table 1.

For the lower internal inductance of $\ell_i = 0.3$, the larger plasma current more than 100 MA is induced due to a lower plasma inductance. Thus, the plasma current is sensitive to the internal inductance. As the heating power is applied from the initial phase, the current penetration becomes slower, and then the internal inductance might be low as in this operation. Although the self-consistent treatment is necessary including the bootstrap current, we use $\ell_i = 0.42$ in the following analysis for simplicity.

However, the initially increased plasma current is not mainly by the non-inductive current with the current drive efficiency $\eta_{CD} = 0.25 \times 10^{20} [\text{Am}^{-2}\text{W}^{-1}]$. Because even if we decrease the current drive efficiency to one-tenth of this value, the induced current is almost the same for the external heating power of 300 MW case. Even if the bootstrap current fraction is changed, the plasma current ramp-up is almost the same. Thus we understand that the plasma current ramp-up is mainly induced by the vertical field effect. We note here that the current drive method and heating equipments are not yet specified in this study.

When the confinement factor is artificially increased from 2.0 to 2.2 during 400 and 600 s (Fig. 3-(e)) after establishment of the steady state, the plasma current is also increased from 92 MA to ~ 96 MA due to the increase in the plasma energy (Fig. 3-(g)) together with the bootstrap current. When the confinement factor is decreased, both the plasma current and the bootstrap current are simultaneously decreased. Thus we see that even if the confinement time is changed, the bootstrap current fraction is still 100 % in this case.

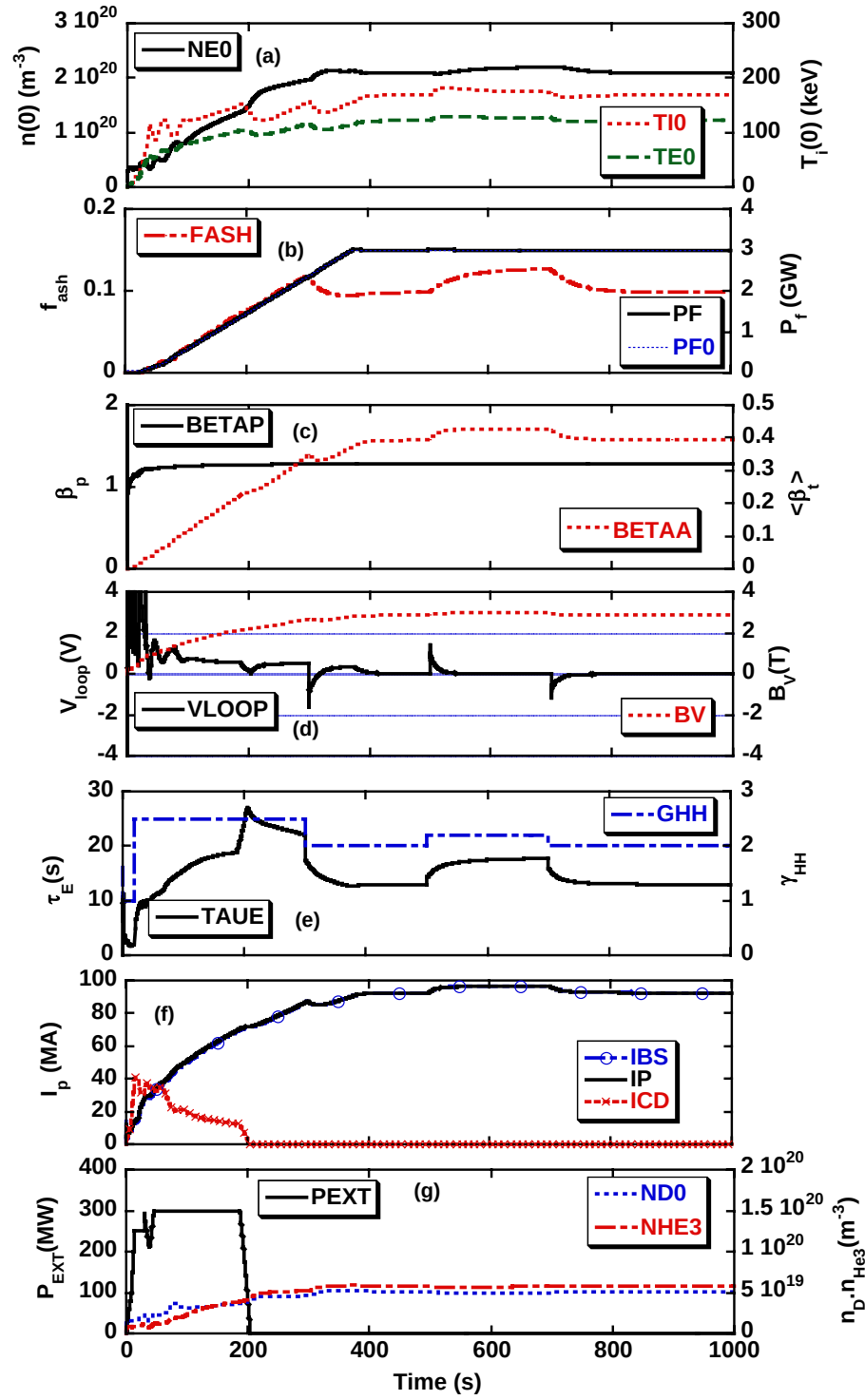


Figure 3. Plasma current changes when the confinement factor is changed from 2.0 to 2.2 at $t=500$ s and back to 2.0 at $t=700$ s. (a)-(g) are the same in Fig. 2 except for (e) τ_E and γ_{HH} .

Table 1. Machine and Plasma parameters of Final ST

		Reference case	Low reflectivity	Long particle confinement	Lowest neutron power
Major radius:	R_o	5.6 m	5.6	5.6	5.6
Minor radius:	a	3.4 m	3.4	3.4	3.4
Toroidal field:	B_{to}	4.4 T	4.4	4.4	4.4
Maximum field:	B_{max}	20.6 T	20.6	20.6	20.6
Radius B_t coil :	Δ_{BT}	1.2 m	1.2	1.2	1.2
Plasma Current:	I_p	93.6 MA	87.3	88.5	93.6
Safety factor:	Q_{MHD}	4.9	5.3	5.3	4.9
Internal inductance:	ℓ_i	0.42	0.42	0.42	0.42
Plasma inductance:	L_p	2.78 μ H	2.78	2.78	2.78
Heating power:	P_{EXT}	250 MW	300	300	250
Confinement factor					
over IPB98(y,2) scaling:	γ_{HH}	2.5 to 2.0	2.5 to 2.0	2.5 to 1.8	2.5 to 2.0
Confinement time:	τ_E	15.8 s	16.3	12.3	16.9
Ash density fraction:	f_{ash}	10.3 %	11.0	15.1	10.8
Be impurity fraction:	f_{Be}	1 %	1	1	1
Effective charge:	Z_{eff}	1.79	1.71	1.66	1.87
Particle confinement					
time ratios:	$\tau_D^*/\tau_E = \tau_{HE3}^*/\tau_E = \dots$	2.0	2	4	2
Fuel ratio:	$n_D:n_{He3}$	0.42:0.58	0.53:0.47	0.61:0.39	0.30:0.70
Fusion product heating					
efficiencies:	$\eta_\alpha = \eta_p = \dots$	1.0	1	1	1
Wall reflectivity:	R_{eff}	0.9	0.35	0.9	0.9
Hole fraction:	f_H	0.1	0.1	0.1	0.1
Density profile:	α_n	0.5	0.5	0.5	0.5
Temperature profile:	α_T	1.0	1.0	1.0	1.0
D- ³ He spin polarization:	$\sigma_{spin}(D-^3He)$	1.0	1.0	1.0	1.5
D-D spin polarization:	$\sigma_{spin}(D-D)$	1.0	1.0	1.0	1/10
Electron density:	$n(0)$	$2.43 \times 10^{20} \text{ m}^{-3}$	2.33×10^{20}	2.54×10^{20}	2.5×10^{20}
Greenwald factor:	$n(0)/n(0)_{GW}$	0.74	0.76	0.82	0.76
Power transfer ratio to ions:	F_{ion}	0.4	0.4	0.4	0.4
Ion to electron confinement					
time ratio:	τ_{Ei}/τ_{Ee}	6.0	6.0	6.0	6.0
Ion temperature:	$T_i(0)$	145 keV	128	115	145
Temperature ratio:	$T_i(0)/T_e(0)$	1.28	1.29	1.23	1.27
Toroidal beta value:	$\langle \beta_t \rangle$	40.3 %	35.0	36.0	40.2
Poloidal beta value:	β_p	1.28	1.28	1.28	1.28
Normalized beta value:	β_N	6.44	5.98	6.08	6.4
Fusion power:	P_f	3000 MW	3000	3000	3000
Neutron power:	P_n	60.4 MW	112	187.7	1.8
Bremsstrahlung loss:	P_b	1369 MW	1081	1181	1496
Synchrotron radiation loss					
to the wall:	P_{sw}	328 MW	713	247	335
to the hole:	P_{sh}	115 MW	145	87	118

Table 1. Continued

		Reference case	Low reflectivity	Long particle confinement	Lowest neutron power
Heat exchange power:	P_{ie}	1049 MW	1041	969	1089
Total plasma conduction loss:	P_L	1129 MW	950	1291	1051
Ion conduction loss:	P_{Li}	126 MW	114	155	110
Electron conduction loss:	P_{Le}	1002 MW	836	1136	940
Electric power ($\eta_c=40\%$)	P_e	1139 MW	1098	1091	1151
Average neutron wall loading:	Γ_n	0.034 MW/m ²	0.063	0.11	0.001
Average heat flux:	Γ_h	1.0 MW/m ²	1.09	0.86	1.09
Divertor heat load:	Γ_r	16.0 MW/m ²	13.5	18.3	14.9

3.2. The Relation of the Ion to Electron Temperature Ratio, Ion and Electron Confinement Time, and Fusion Power Fraction to Ions

So far, we have used the fixed parameters of the fusion power fraction to ions $F_{ion}=0.4$ and the ion to the electron confinement time ratio $\tau_{Ei}/\tau_{Ee}=6$. Here we study more in detail the relationship of the ion to the electron temperature ratio $T_i(0)/T_e(0)$, the fusion power fraction to ions F_{ion} , and the ion to the electron confinement time ratio τ_{Ei}/τ_{Ee} . In Fig. 4, these relationships are shown. When τ_{Ei}/τ_{Ee} is increased for a constant $F_{ion}=0.4$, the ion to electron temperature ratio $T_i(0)/T_e(0)$ increases from 1.27 ($\tau_{Ei}/\tau_{Ee}=4.5$) to 1.30 ($\tau_{Ei}/\tau_{Ee}=10.0$). Even for larger τ_{Ei}/τ_{Ee} value, $T_i(0)/T_e(0)$ is saturated and does not increase any more. When the fusion power fraction to ions F_{ion} is increased to 0.7, the ion to electron temperature ratio increases furthermore from 1.50 ($\tau_{Ei}/\tau_{Ee}=1.5$) to 1.62 ($\tau_{Ei}/\tau_{Ee}=6.0$). The fusion power fraction to ions F_{ion} determines the ion to the electron temperature ratio at first, and then the ion to the electron confinement time ratio does next.

This fact could be understood from the power balance equations. The ion power balance in the steady state is given by

$$\bar{P}_{FP} F_{ion} = \bar{P}_{ie} + \bar{P}_{Li} \quad (2.10'')$$

and the electron power balance in the steady state is

$$\bar{P}_{FP} (1 - F_{ion}) + \bar{P}_{ie} = \bar{P}_b + \bar{P}_s + \bar{P}_{Le} \quad (2.11'')$$

In the rough limit of $\bar{P}_{Li}=0$ with a neoclassical ion confinement, the inequality

$$\bar{P}_{FP} F_{ion} > \bar{P}_{ie}$$

must be satisfied to transfer a power from the fusion product to electrons.

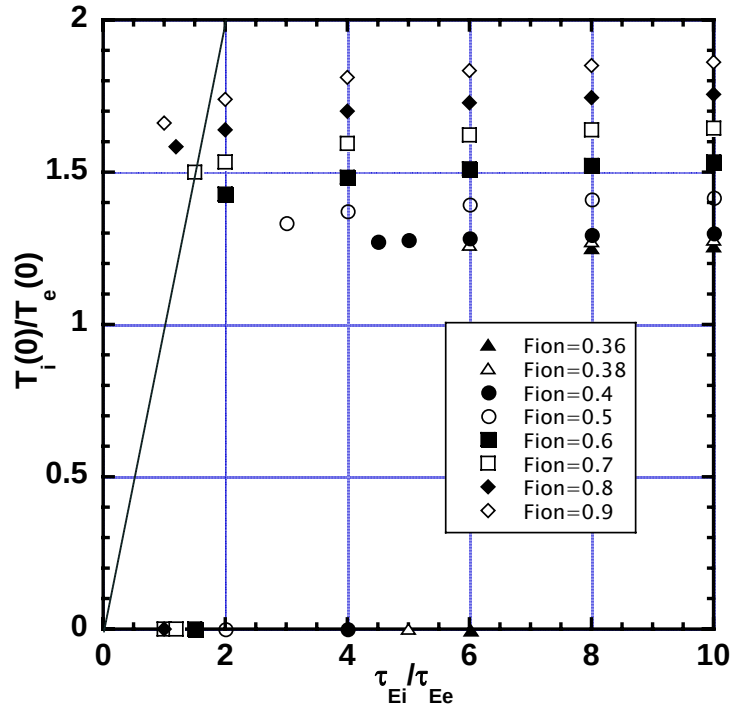


Figure 4. The ion to electron temperature ratio $T_i(0)/T_e(0)$ vs the ion to electron energy confinement time ratio τ_{Ei}/τ_{Ee} with respect to the ion power fraction from the fusion power of $F_{ion}=0.36\sim 0.9$. Other parameters are the same as in Fig. 2 except for F_{ion} and τ_{Ei}/τ_{Ee} .

Therefore, at first F_{ion} determines the input power ratio to ions and electrons, and then confinement time ratio τ_{Ei}/τ_{Ee} determines the plasma conduction loss, yielding the temperature ratio. In general, in the high-density regime ions and electrons couple strongly by the heat exchange power by collisions $\bar{P}_{ie}$. Therefore, the operating density is lower than $\sim 3 \times 10^{20} \text{ m}^{-3}$ as seen in this study.

3.3. Low Wall Reflectivity

So far the wall reflectivity R_{eff} is assumed as high as 0.99 or 0.9. Although the high wall reflectivity is desirable for achieving ignition, the wall reflectivity may be reduced during the discharge due to the particle sputtering to the first wall. Therefore, we have checked the lower bound of the reflectivity. As shown in Fig. 5, the wall reflectivity is set to $R_{eff}=0.7$ until $t=250$ s, and then dropped to $R_{eff}=0.35$ almost corresponding to silicon carbide (SiC) [31]. Ignition is achievable in this case. However, to expand the ignition regime, a larger quantity of deuterium is used as $n_D : n_{HE3} = 0.53 : 0.47$, and then the total neutron power is as large as $P_n \sim 112$ MW for $\tau_D^*/\tau_E = \tau_{HE3}^*/\tau_E = \tau_1^*/\tau_E = \tau_p^*/\tau_E = \tau_\alpha^*/\tau_E = 2$, and the ion to the electron confinement time ratio $\tau_{Ei}/\tau_{Ee} = 6$. This is because the toroidal field is reduced due to high beta value, and then the synchrotron radiation loss to the first wall is itself large.

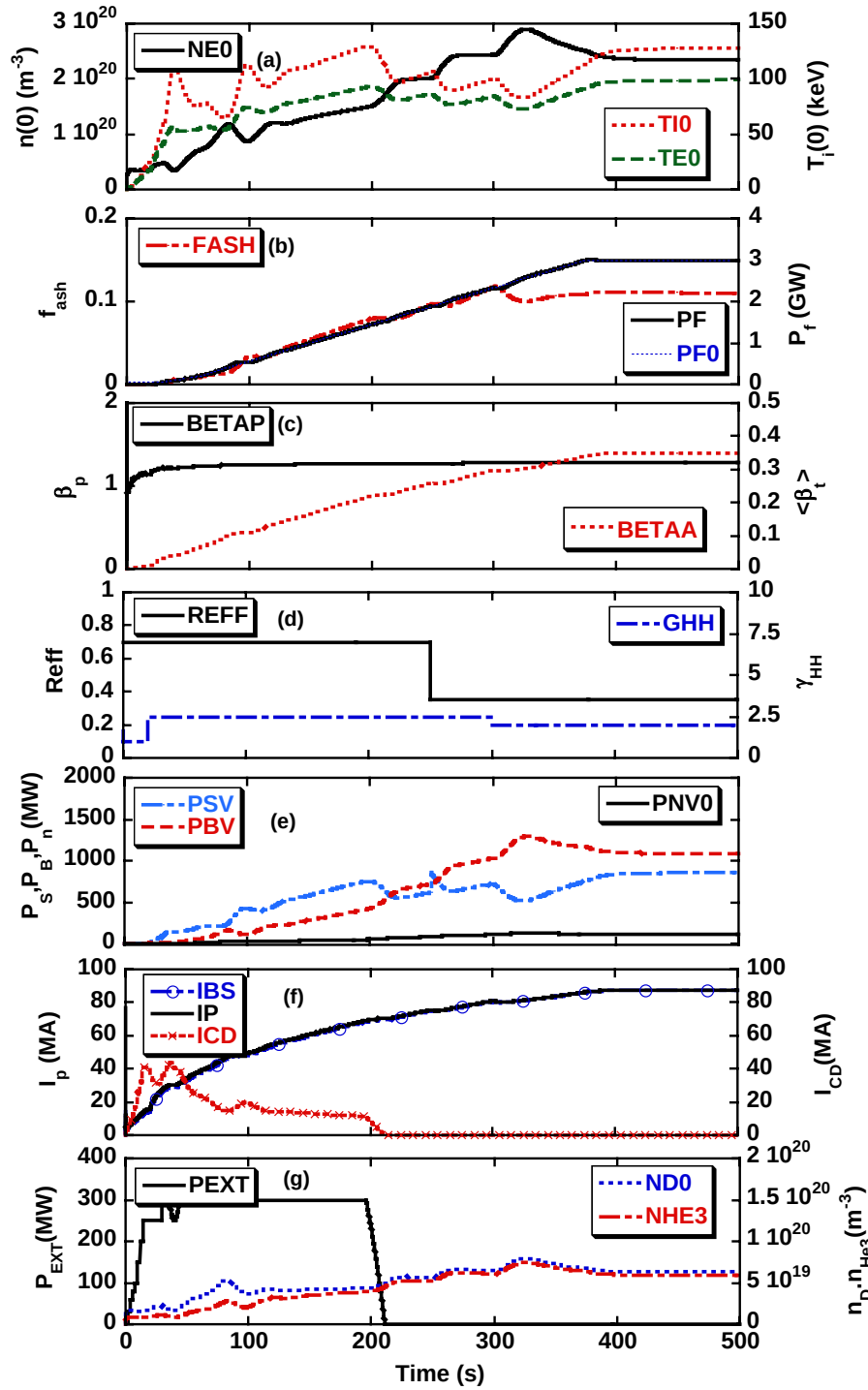


Figure 5. Temporal evolutions of the plasma parameters when the wall reflectivity dropped from $R_{\text{eff}}=0.7$ to 0.35 at $t=250$ s. Externally given parameters are the same as in Fig. 2 except for $n_D : n_{\text{He3}} = 0.53 : 0.47$. (a)-(g) are the same in Fig. 2 except for (d) wall reflectivity R_{eff} and confinement factor G_{HH} , and (e) the bremsstrahlung loss P_B , synchrotron radiation loss P_{SV} , and the neutron power P_{NV} .

But, fusion reaction should be increased using the high D-³He fuel ratio. Actually, the synchrotron radiation loss power to the wall is increased to 713 MW as shown in Fig. 5-(e) at $t=1000$ s. The confinement factor in the later phase is 2.0, $\langle\beta_t\rangle\sim 35\%$ and $\beta_N\sim 6.0$. The detailed parameters are listed in Table 1.

As the toroidal field is very strong as 11.2 T at the inboard plasma edge ($R_0=2.2$ m), more elaborate analysis on the synchrotron radiation in a ST is required to make concrete statements of D-³He spherical tokamak reactor, which is beyond this work. We also note that the poloidal field is not taken into account into the beta value itself and the synchrotron radiation loss.

3.4. Long Particle Confinement

When the particle to energy confinement time ratio is increased, fusion product ashes are accumulated, and ignition is terminated due to fuel dilution. Therefore, this parameter is crucially important in a D-³He reactor. In Fig. 6 is shown the case of $\tau_D^*/\tau_E = \tau_{HE3}^*/\tau_E = \tau_I^*/\tau_E = \tau_p^*/\tau_E = \tau_\alpha^*/\tau_E = 4$, which is close to the value used in the present ITER analysis, and the ion to the electron confinement time ratio is $\tau_{Ei}/\tau_{Ee} = 6$. Ignition is achievable at $\langle\beta_t\rangle\sim 36\%$ and $\beta_N\sim 6$ for a large quantity of deuterium fuel of $n_D : n_{HE3} = 0.61 : 0.39$ and $R_{eff}=0.9$. Ashes are accumulated up to 15.1 % and the density is $2.54\times 10^{20} \text{ m}^{-3}$, and the neutron power is as high as $P_n \sim 187$ MW. The other parameters are the same as in Fig. 2. The deuterium fuel density fraction cannot be reduced further because a large fusion reaction is needed due to the dilution effect. However, this is quite remarkable because the particle to energy confinement time ratio in the previous D-³He tokamak design has been as small as 1 ~ 2 [32] although the large neutron power is produced. The detailed parameters are listed in Table 1, and the neutron power is also plotted in Fig. 7.

3.5. Lower Neutron Power Mode Operation

The conditions with the small reflectivity and the large particle to energy confinement time ratio produce the high neutron power. To reduce the neutron power in a D-³He fusion, the particle to energy confinement time ratio should be as small as possible which needs strong pumping of the fusion product ashes, and reflectivity larger than ~ 0.9 . As shown in Fig. 2, the neutron power is decreased when the particle to energy confinement time ratio, $\tau_D^*/\tau_E = \tau_{HE3}^*/\tau_E = \tau_I^*/\tau_E = \tau_p^*/\tau_E = \tau_\alpha^*/\tau_E$, is decreased from 4 to 2, where fuel ratio was also changed from $n_D : n_{HE3} = 0.66 : 0.34$ ($n_{HE3}/n_D = 0.51$) to $n_D : n_{HE3} = 0.42 : 0.58$ ($n_{HE3}/n_D = 1.38$) to maintain ignition. When the reflectivity is further increased to 0.99, the ³He density can be slightly increased to $n_D : n_{HE3} = 0.40 : 0.60$ ($n_{HE3}/n_D = 1.5$), reducing the neutron power to 52.5 MW. This might be a limit of operation as shown in Fig. 7 without nuclear spin polarization. However, if a nuclear spin polarization of D and ³He is available and it works, the neutron power could be further reduced. First, when D-³He reaction is enhanced by 50 % [33]-[35], such as $\sigma_{spin}(D-^3He) = 1.5$ and $\sigma_{spin}(D-D) = 1$, the neutron power is reduced to $P_n \sim 18.0$ MW with the fueling ratio of $n_D : n_{HE3} = 0.3 : 0.7$ ($n_{HE3}/n_D = 2.33$).

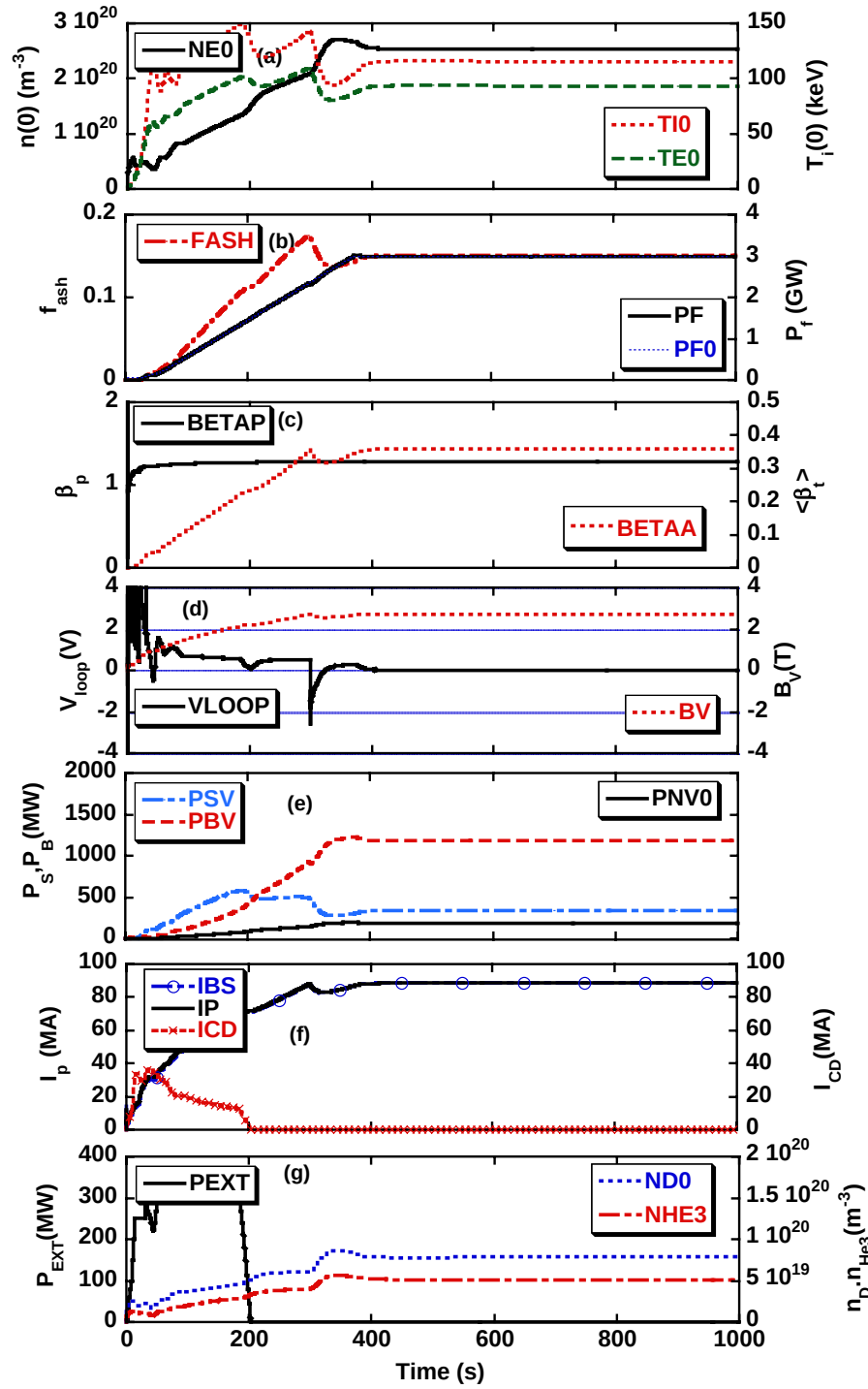


Figure 6. Long ash particle confinement time effect on the discharge in the case of $\tau_D^*/\tau_E = \tau_{\text{HE3}}^*/\tau_E = \tau_T^*/\tau_E = \tau_p^*/\tau_E = \tau_\alpha^*/\tau_E = 4$. The neutron power increases up to 193 MW due to large deuterium fraction of the fuel ratio of $n_D : n_{\text{HE3}} = 0.61 : 0.39$. (a)- (g) are the same in Fig. 2 except for (e) the bremsstrahlung loss PB, and synchrotron radiation loss PSV, and the neutron power PNV.

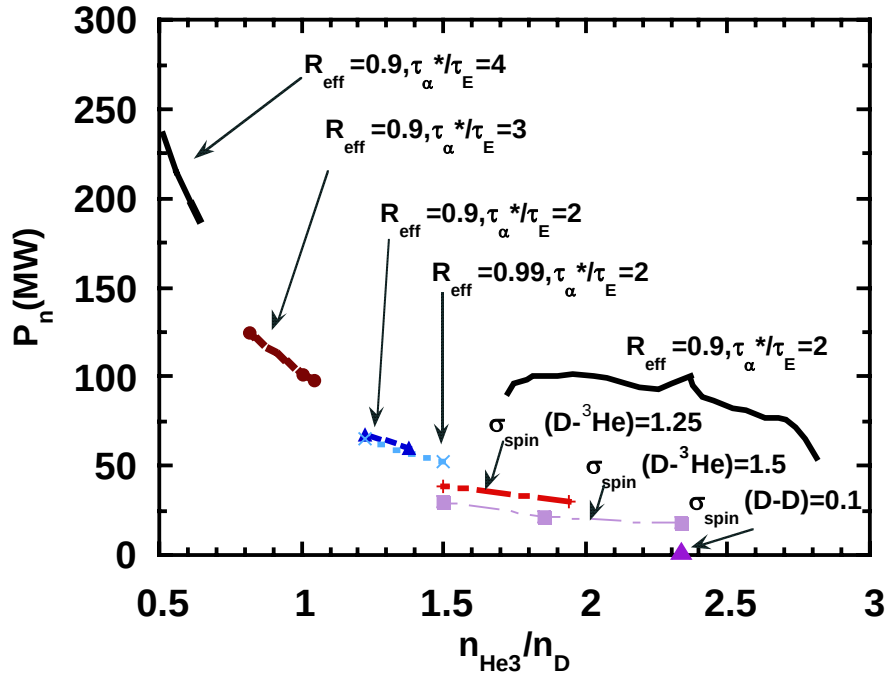


Figure 7. The neutron power in the Final ST for various conditions. Lowest neutron power of $\sim 2\sim 20$ MW range is obtained when the ^3He is nuclear spin-polarized completely ($\sigma_{\text{spin}}(\text{D-}^3\text{He})=1.5$), partially polarized ($\sigma_{\text{spin}}(\text{D-}^3\text{He})=1.25$), and D-D suppression effect ($\sigma_{\text{spin}}(\text{D-D})=1/10$) is taken into account. The ^3He fraction can be increased due to the enhancement of the fusion reactivity. Parameters are $\tau_{\text{D}}^*/\tau_{\text{E}} = \tau_{\text{He3}}^*/\tau_{\text{E}} = \tau_{\text{T}}^*/\tau_{\text{E}} = \tau_{\text{p}}^*/\tau_{\text{E}} = \tau_{\alpha}^*/\tau_{\text{E}} = 2$ and 4 , and $R_{\text{eff}}=0.9$ and 0.99 .

The ignition exists to the lower quantity of deuterium density by fusion cross-section enhancement. Secondly, if $\text{D-}^3\text{He}$ reaction is enhanced by 50 % and D-D reaction is suppressed by a factor of 10, such as $\sigma_{\text{spin}}(\text{D-}^3\text{He}) = 1.5$ and $\sigma_{\text{spin}}(\text{D-D}) = 1/10$, we have further smaller neutron power of $P_n \sim 1.8$ MW with the same fueling ratio of $n_{\text{D}} : n_{\text{He3}} = 0.3 : 0.7$. (See Table 1.) We note that the depolarization in the high beta plasma is not studied yet, and D-D suppression is not proved experimentally and still controversial theoretically [36]-[39]. The magnetic field in the equatorial plane does not align to the same direction, and especially in a high beta $\text{D-}^3\text{He}$ plasma, the toroidal field is decreased in the plasma region due to diamagnetic effect and then the poloidal field remains. Thus, the spin-polarized pellet injected horizontally would provide less reactivity than 150 %. In the case of $\sigma_{\text{spin}}(\text{D-}^3\text{He}) = 1.25$ and $\sigma_{\text{spin}}(\text{D-D}) = 1$, the neutron power would be 30.5 MW for $n_{\text{D}} : n_{\text{He3}} = 0.34 : 0.66$ ($n_{\text{He3}}/n_{\text{D}} = 1.94$).

3.6. Shutdown of $\text{D-}^3\text{He}$ Discharge

Plasma current ramp-up is induced by the vertical field, which is changed by the fusion power and plasma energy. Therefore, it is interesting to know whether shutdown can be done by the fusion power control. In Fig. 8, the shut down is shown after the plasma current flat top at

$t=500$ s. By decreasing the fusion power linearly, the density and temperature go down, and then the plasma current is decreased smoothly together with the plasma energy. During the shutdown phase, the heating power up to 250 MW is applied automatically by the feedback control in order to avoid the L-mode transition. When the confinement is reduced abruptly during the shut-down phase, the plasma current is decreased abruptly. Therefore, the minimum confinement factor is set to $\gamma_{HH}=1$ during the L-mode phase ($M_{HL}<1.0$) as seen in Fig. 8-(e). The plasma current is decreased to 0.9 MA level, which is almost zero compared to 90 MA, and then computation is terminated. Thus current ramp-up and ramp-down are demonstrated by controlling the fusion power.

3.7. Long Time Behavior of the D-³He Discharge

We have assumed the favorable scaling parameters for the bootstrap current coefficient of $C_{BS}=1$ in Eq. (A-15) so far. Here, we have examined the case with the less favorable parameters such as $C_{BS}=0.5$ (Fig. 9-(a) and (b)), $C_{BS}=0.7$ (Fig. 9-(c) and (d)), and $C_{BS}=0.9$ (Fig. 9-(e) and (f)). The reference discharges are taken from Fig. 2 with the internal inductance of $\ell_i = 0.42$, and $R_{eff}=0.9$. For the smaller bootstrap current coefficient of $C_{BS}=0.5$, the plasma current decays gradually from 92.3 MA to 72.0 MA at ~ 9744 s (2.7 h) and then disrupts as shown in Fig. 9-(b). The characteristic decay time at 6000 s is $\tau=L_p/\{R_p(1-f_{BS})\}=2.77\times 10^{-6}/8.09\times 10^{-11}\sim 9.5$ h. During the current decay, the plasma density increases and the ion and electron temperature are decreased (Fig. 9-(a)), and ignition is over, leading to the plasma current termination.

For the bootstrap current coefficient of $C_{BS}=0.7$, the plasma current decays slower as shown in Fig. 9-(d). Judging from the average current decay rate of $dI_p/dt\sim -0.0012$ MA/s between two points of $I_p=92.854$ MA at 470.72 s and $I_p=81.18$ MA at 10000 s, then ignition might be terminated when the plasma current reaches the same level of 74.7 MA at $t=10000+(81.18-74.7)/0.0012\sim 14500$ s (~ 4.2 h). On the other hand, the characteristic decay time is $\tau=L_p/\{R_p(1-f_{BS})\}=2.77\times 10^{-6}/2.2\times 10^{-11}\sim 34.9$ h at $t=14000$ s.

If the bootstrap current coefficient is more than 90 %, the plasma current does not decrease for a long time due to very high temperature (Fig. 9-(f)). Although the characteristic decay time is $t=2.77\times 10^{-6}/1.56\times 10^{-11}\sim 49.3$ h at $t=14000$ s, it is expected that the much longer steady state like operation with ~ 1700 h (73 days) is possible from the average current decay rate of $dI_p/dt\sim -2.8\times 10^{-6}$ MA/s between 470.72 s and 10000 s. When the plasma current reaches $I_p\sim 75$ MA, it should be ramped down by the controlled manner with the fusion power shutdown. After the plasma current becomes zero, it is ramped up again by the heating power. Thus "quasi-continuous cyclic DC operation" is possible as will be explained in the next section. Thus, we have seen that the pulse length in this ST reactor depends on the bootstrap current fraction when any non-inductive current drive power is not applied.

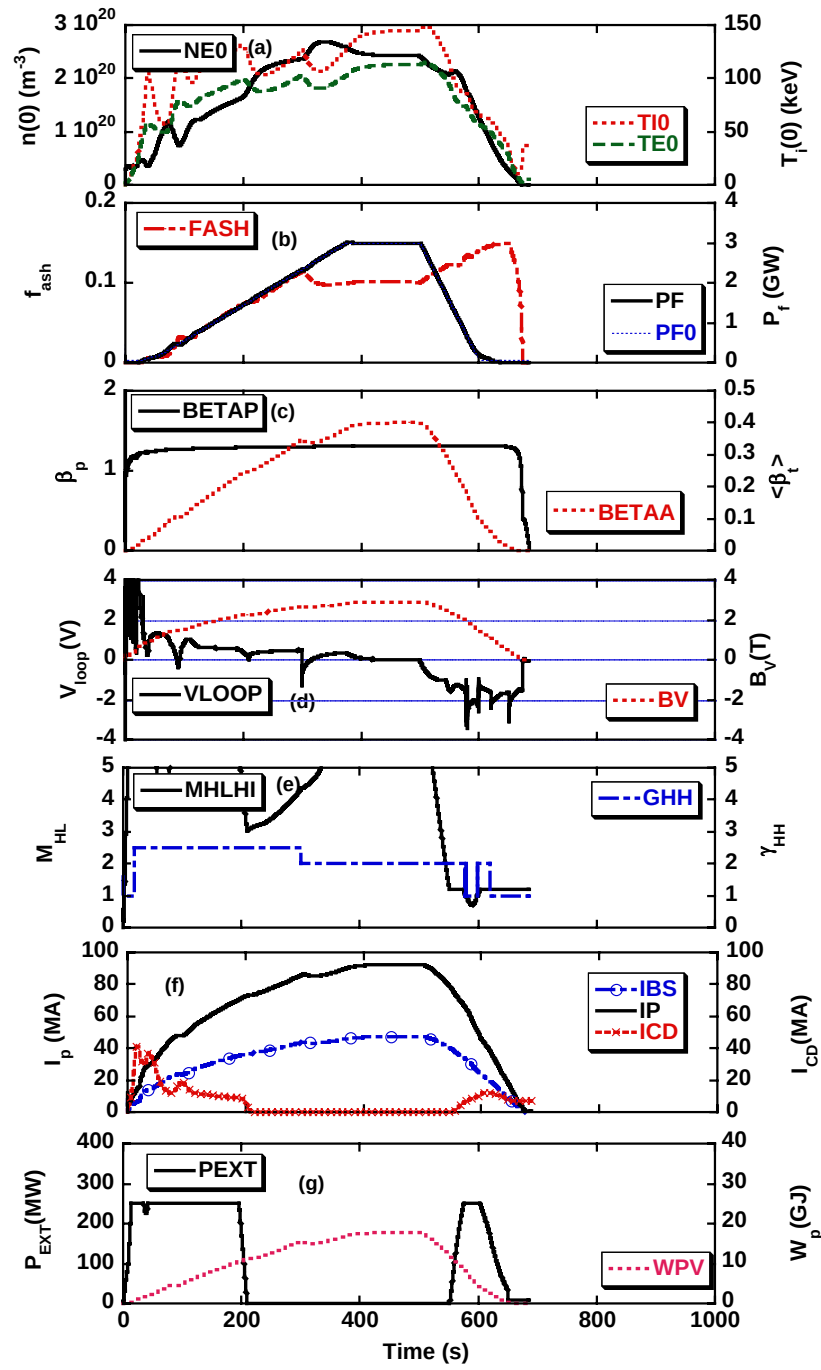


Figure 8. The shutdown phase of the $\text{D-}^3\text{He}$ discharge. The fusion power starts to decrease at $t=500$ s. (a)- (g) are the same in Fig. 2 except for (d) H-mode factor M_{HL} and confinement factor γ_{HH} , and (e) the plasma energy which is important parameters in this operation. The plasma current is determined by the plasma energy. Other parameters are the same as in Fig. 2.

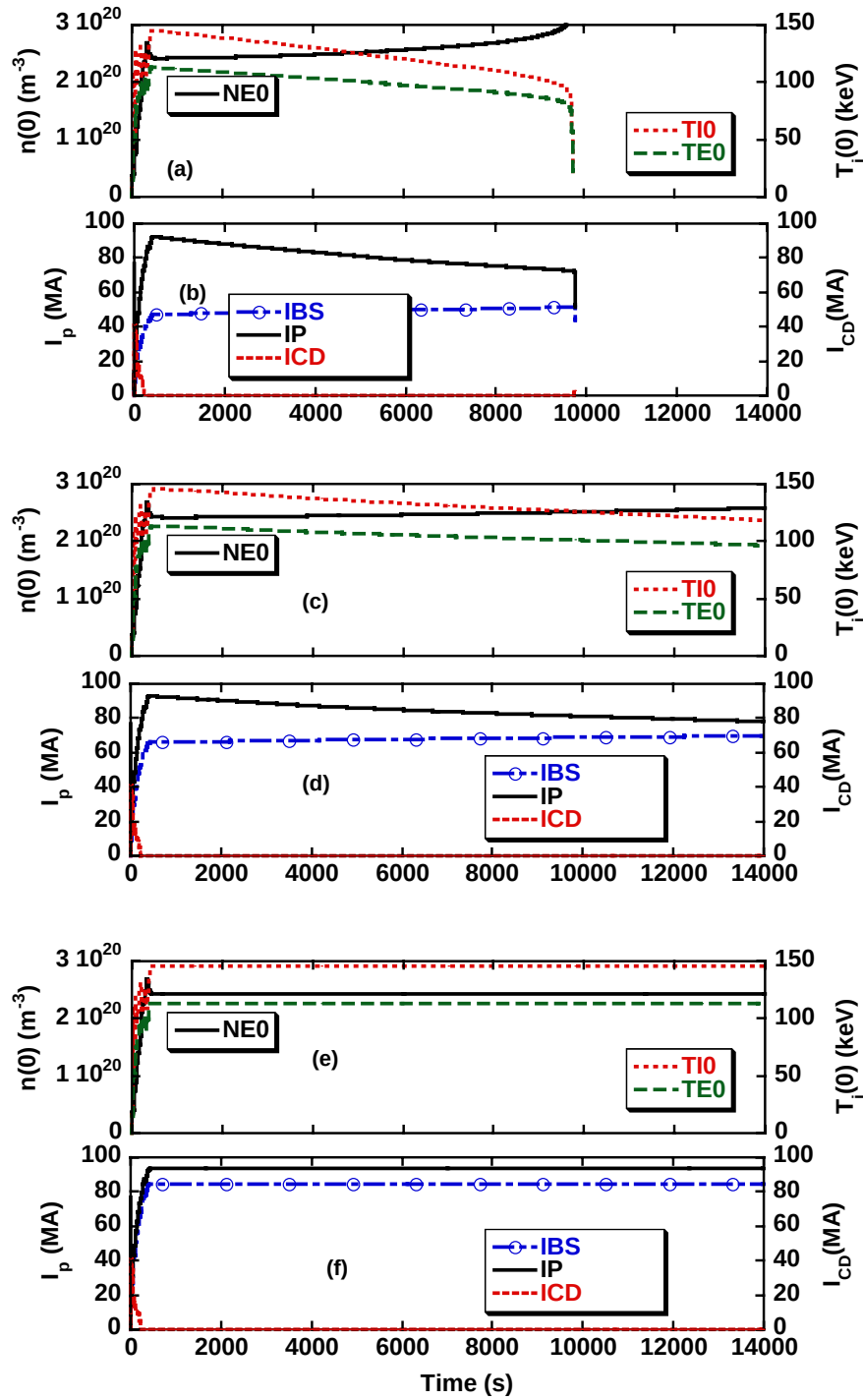


Figure 9. Plasma current ramp-up and subsequent decay phase without any application of the heating/current drive power for the various bootstrap current coefficients of (a), (b) $C_{BS} = 0.5$, (c), (d) $C_{BS} = 0.7$, and (e), (f) $C_{BS} = 0.9$. Parameters are the same as in Fig. 2 except for C_{BS} . It is seen that the current rise-up phase does not depend on the bootstrap current fraction.

We should also note that the maximum plasma currents at the plasma current rise-up phase until $t = 200$ s for different values of C_{BS} are almost the same, but subsequent decay times are quite different. We do understand from this fact that the bootstrap current does not affect the plasma current ramp-up because the vertical field effect is stronger.

The bootstrap current affects only the steady state phase as found by the plasma current decay. These facts can be understood from the circuit equation of (A-13) or (A-18) that the bootstrap current effect is smaller due to the low plasma resistance R_p than the vertical field effect in a transition phase, and the bootstrap current effect is dominant in the steady state because of no induction effect of the vertical field. This point was discussed in the previous paper [5][6]. Although the actual C_{BS} in ST may be around $C_{BS} = 0.6$ [40], the effective C_{BS} would be slightly larger than that when the diamagnetic current is taken into account in addition to the bootstrap current [41][42]. As we have made a simple calculation based on the simple geometry, more detailed analysis including the plasma shape and profile effect would be necessary to estimate the accurate C_{BS} and to study the steady state phase.

3.8. New Operation Scenario of Quasi-Continuous Cyclic DC Operation

In this D-³He ST reactor, the plasma current is ramped up essentially by the fusion power through the plasma energy, and ramped down smoothly to zero by reducing the fusion power as demonstrated in Fig. 8. When the bootstrap current fraction is as small as 50 %, the plasma current decays and is terminated as shown in Fig. 9. Therefore, when the bootstrap plasma current fraction approaches 75 MA, the discharge should be shut down in a controlled manner by the fusion power shutdown before the current termination. And then the heating power is again applied to ramp up the plasma current as shown in Fig. 10-(a). The heating/current drive power is applied during the current and fusion power rise-up phase, and fusion power shutdown phase. This is the "quasi-continuous cyclic DC operation" with the thermal storage unit to keep the electric power constant, which is quite similar to the "quasi-continuous AC operation" employed for a high aspect ratio D-³He tokamak reactor [35].

We tend to think that the heating/current drive power can be applied to ramp the plasma current back to the initial value after the current decay if the bootstrap current fraction is small. This possible operation is schematically shown in Fig. 10-(b). To examine this scenario, the huge heating/current drive power of 2.4 GW was applied near the current termination phase between 9500~10500 s as shown in Fig. 11 with the bootstrap current fraction of 50 %. The plasma current is ramped up during this phase, and then return to almost the previous value (~91 MA), and is terminated at $t \sim 11600$ s. This is because the plasma energy, which is the driving force of the plasma current ramp-up, rises and drops again after termination of the huge heating/current drive power. Discharge duration is extended only for ~1900 s. At the same time, the temperature goes up and the density down. We see again that in a high temperature D-³He operation regime, the current drive is not effective because of too high temperature. It needs a quite long duration to see its effect.

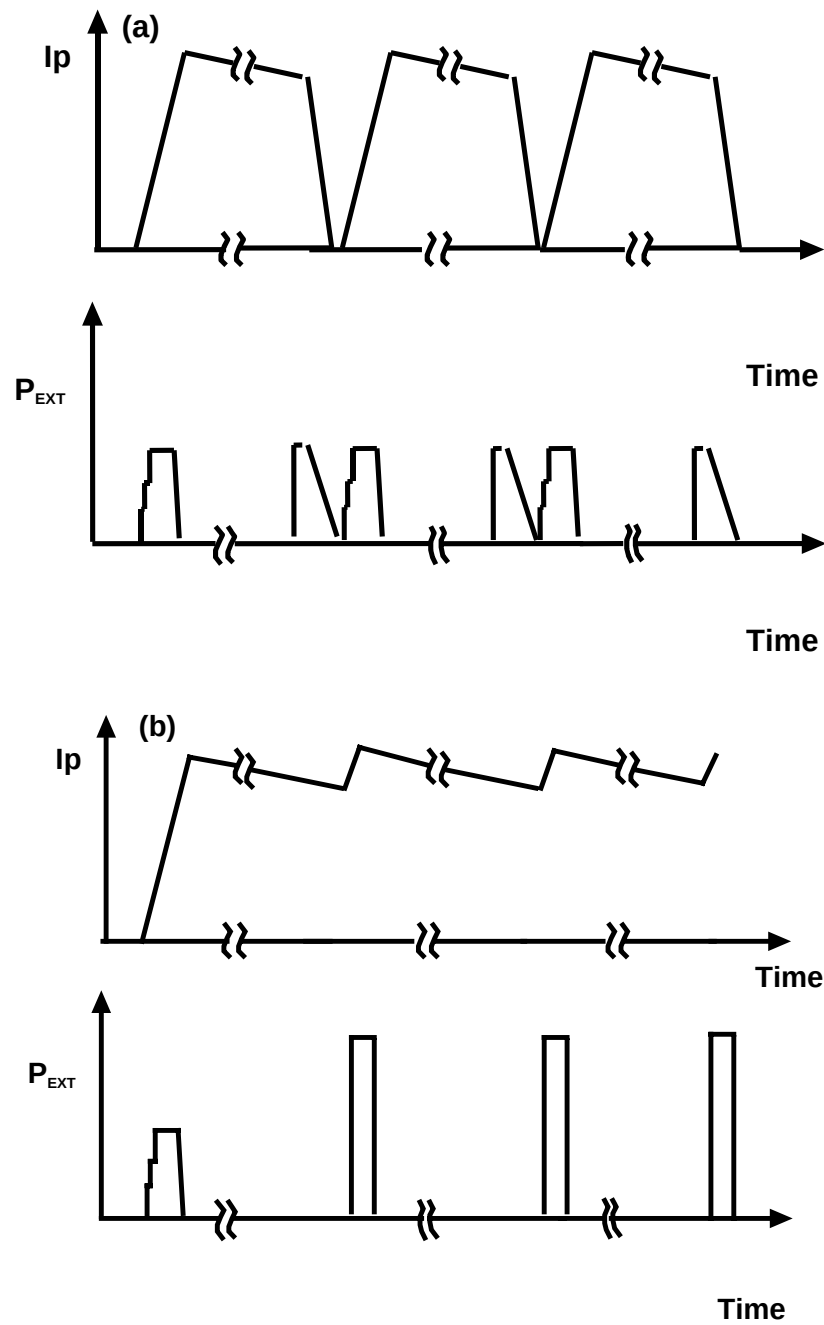


Figure 10. (a) Proposed "quasi-continuous cyclic DC operation" when the bootstrap current fraction is less than 100%. The plasma current decays and is shut down after a long time, and then ramped up again. (b) Possible operation to restore the steady state when the bootstrap current fraction is less than 100 %. However, this operation does not work properly as shown in Fig. 11.

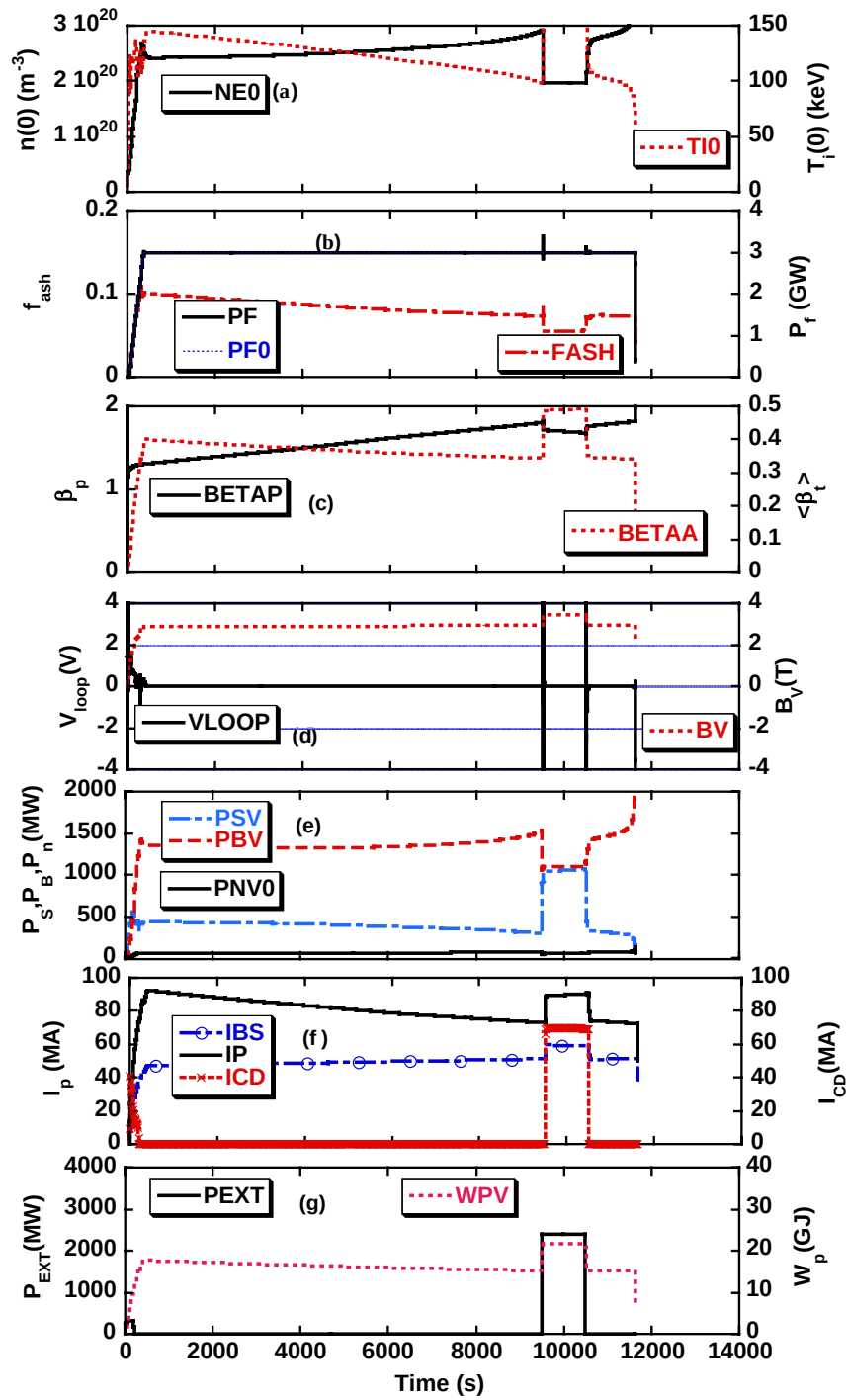


Figure 11. Possible operation scenario does not work properly in order to obtain the steady state when the bootstrap current fraction is less than 50 % of the total plasma current. During 9500 s and 10500 s, the huge heating/current drive power of 2.4 GW is applied. During this phase the plasma current increases and then decreases at the time of switching off the heating/current drive power, and returns to almost the same level before power application.

4. Various Issues in a D-³He ST Reactor

4.1. External Heating Power

The reason of setting the large confinement factor of 2.5 in the initial current ramp-up phase is to reduce the heating power to 250 ~300 MW range in this D-³He ST reactor. Therefore, further improvement of the confinement factor is required in ST experiments. Here, although we have assumed the power fraction to ions from the external heating power to be $f_{pi}=0.5$, more detailed studies on this value using distribution functions is necessary.

The external heating power is applied during the fusion power rise-up phase and shut down phase. Therefore, it should work in the lower density than $n(0)=2 \times 10^{20} \text{ m}^{-3}$ and in the lower beta than $\langle \beta_t \rangle = 20\%$. During ignition phase no heating power is applied. We have not specified the heating device yet in this D-³He ST reactor. Neutral beam injection (NBI) with high energy $> 2 \text{ MeV}$ and electron cyclotron resonance frequency heating (ECRH) with 123 GHz ($B_{t0} \sim 4.4 \text{ T}$) could be candidates. ECRH system can work in the initial current start-up phase because the operating density is less than the cut-off density of $n=1.8 \times 10^{20} \text{ m}^{-3}$ in the heating phase as shown in the reference case.

We also note that the heating power is applied from the beginning of the discharge, the internal transport barrier (ITB) mode of operation could be obtained with high confinement factor. The heating power of 200 ~ 300 MW is rather large, but if the current ramp-up time is longer, the heating power could be slightly decreased due to dW/dt effect in the power balance equation. For the large power supply of the heating power, SMES may be useful to isolate it from the power grid.

Additionally, although it is not recommended, injection of the small amount of the tritium accumulated during D-³He operation could reduce the heating power and required confinement factor.

4.2. Heat Flux to the First Wall

The total bremsstrahlung power of 1369 MW and the synchrotron radiation of 311 MW to the first wall provide the heat flux of $\sim 1 \text{ MW/m}^2$ in the reference case, which is an engineering tolerable value and could heat up the first wall coolant such as supercritical water up to $\sim 380^\circ \text{C}$ [44] or organic coolant up to $\sim 425^\circ \text{C}$ [45] within the evaporation temperature limit. The heat flux is found not so large due to the wide area of the first wall in ST ($S_w \sim 1,780 \text{ m}^2$). However, the heat flux peaking factor (the ratio of the heat flux to the outboard/inboard first wall) is around 1.64 based on the two-dimensional cylindrical model of the inner, outer first wall and the plasma without Shafranov shift. Therefore, the heat flux to the outboard surface is 1.32 times larger than the average heat flux. (We note that three dimensional treatment may somewhat reduce the peaking factor through the solid angle.) As the heat flux peaking factor due to the Shafranov shift of the radiative zone can also enhance the peaking factor, it must be taken into account for more accurate estimation of the heat flux. As the distance between the plasma surface and the outboard first wall should be separated to reduce the peaking factor, compromising with the resistive wall mode is necessary.

The fusion power itself may be reduced to decrease the radiation loss. As the volumetric heating by the neutron is small in a D-³He reactor and blanket is not necessary to multiply the energy with Be and to produce tritium by Li, only the coolant is heated up in the first wall cooling pipe, which is located in the area with ~4 cm from the first wall front (energy conversion layer) [45]. Therefore, the coolant temperature is lower than that in a D-T reactor. Here, the heat load to the divertor can be used for preheating of the main coolant. Hence, we have assumed the somewhat lower thermal efficiency around 40 % to estimate the electric power output of ~1 GW.

The material and the thickness of the first wall are important parameters to consider a D-³He reactor. Simple estimation provides that 3~5 mm first wall thickness may be possible for the low activation Ferritic steel F82H or high Z material Tungsten (W) because the heat flux is more manageable than the previous study with 1.8 MW/m² [32]. Actually, as the hot-isostatic-pressed (HIP) bonded F82H first wall mock-up test with built-in rectangular cooling channel and first wall thickness of 3 mm has successfully shown experimentally the structural soundness up to the heat flux of 2.7 MW/m² [44], the first wall for Final ST may not be a problem. Rear part of the energy conversion layer is to shield the small amount of the neutron and gamma rays induced by the neutron bombardment. This shield for the high-temperature superconducting coil needs more study in this case.

4.3. Heat Flux to the Divertor

The divertor heat flux in the double null configuration is simply estimated using the plasma conduction loss of ~1000 MW, which is divided by the surface area of the divertor wetted width of 1 m on the major radius as $P_{div} = P_L / (2\pi R_o \times 1m) / 2 \sim 15$ to 20 MW/m². This is larger than the present allowable value of 10 MW/m² in ITER divertor. However, for example, the pebble divertor capable up to 30 MW/m² [46] or liquid Ga divertor [47] may provide the solution. In both the cases, the hydrogen isotope such as the proton ash and small amount of tritium should be removed.

4.4. Tritium Production

The tritium production is small in the D-³He operation, but not negligible. Exhausted tritium produced by D-D reactions in the reference discharge in Fig. 2 is estimated using the tritium particle loss term in Eq.(2-3), $n_T(0)/\tau_T = f_T n_e(0)/(2\tau_E)$, in the steady state. For $f_T \sim 1.0 \times 10^{-3}$, $n_e(0) \sim 2.4 \times 10^{20} \text{ m}^{-3}$, $\tau_E \sim 15.8 \text{ s}$, and the tritium mass $M_T = 5.00 \times 10^{-27} \text{ kg}$, the total tritium mass produced per a full operation day is $M_T(\text{total}) \sim \{n_T(0)/\tau_T / (1 + \alpha_n)\} V_o (24 \times 60 \times 60 \text{ s}) M_T \sim 9.22 \text{ g}$. After one year, the total tritium mass is 3.3 kg. This is the exhausted tritium mass from the plasma and it is concerned how much tritium remains in the first wall, shadow region, shield and vacuum vessel.

At the ideal theoretical limit of nuclear spin polarization, the minimum tritium production is 0.40 g/day and 146 g/year, which is quite attractive from the safety point view of a reactor.

4.5. Plasma Energy and Disruption Effects

In a D-³He ST reactor, in general, the plasma energy content is much larger than a D-T reactor due to low neutron power production. In Final ST, the plasma energy contained in a charged particles is very large as $W_p = P_L \tau_E = 15 \sim 17$ GJ due to the large plasma volume as seen in Fig. 8-(g). This is ~ 7 times larger than the plasma energy of ~ 2.4 GJ in a D-T ST reactor [6]. Therefore, it can induce the larger plasma current than that in a D-T ST reactor in spite of the larger plasma inductance of the large machine size.

However, this large plasma energy may induce the serious disruption damage on the first wall in a D-³He reactor than in a D-T reactor when the plasma has disruption. If a high-pressure noble gas is injected at disruption, the plasma energy is uniformly distributed on the first wall by radiation as demonstrated in DIII-D [48]. If the same technique could be employed in Final ST, the energy density received by the first wall is $17 \text{ GJ} / 1780 \text{ m}^2 \sim 10 \text{ MJ/m}^2$. For 1 ms disruption time under 10 MJ/m^2 , the melting layer thickness for W and Be are $186 \text{ }\mu\text{m}$ and $80 \text{ }\mu\text{m}$ as given in Table 20 in the reference [49], respectively. Therefore, Be sacrificial layer could be attached on the first wall. On the other hand, the complete disruption mitigation technique must be also developed even if the disruption effect is thought to be small in a ST.

4.6. Shafranov Shift Effect

As the plasma current profiles in a high beta plasma shifts to the outward direction due to the Shafranov shift, the plasma inductance may be increased due to the larger major radius [50]. On the other hand, as the plasma shifts to an outboard side, the mutual inductance between the shifted plasma center and the vertical coils increases. In the case of a high aspect ratio tokamak, the effect of the plasma inductance is larger [51], then the plasma current may be decreased when the plasma shifts to the outboard side. It should be evaluated in the case of ST. When the plasma current is decreased due to Shafranov shift, the larger plasma elongation for example $\kappa \sim 3.4$ could be chosen to reduce the plasma inductance to increase the plasma current. Thus, further optimization may be necessary to determine reactor parameters. To simulate the current ramp-up, a detailed current profile must be also taken into account with 1 dimensional transport equations, which is beyond the scope of this paper.

4.7. Energy Transfer Fraction to Ions

We have assumed the energy transfer fraction of the fusion product heating to ion of $F_{\text{ion}} = 0.4$ in this study. Nuclear elastic scattering of high energy protons and alpha particles could have $F_{\text{ion}} \sim 0.4$ in the high temperature regime, which has been calculated by Matsuura et. al. [23]. As nuclear elastic scattering is based on the collision process, this effect may be robust and at least can provide a D-³He ignition in a ST reactor. We note that a larger value of $F_{\text{ion}} > 0.75$ is assumed to go to ion for obtaining the hot ion mode of $T_i/T_e > 1.6$ in ARIES-III D-³He high aspect ratio tokamak [52], although the plasma profile effects are not taken into accounts. However, if other mechanism such as stochastic heating could supply the more power to ions, it would reduce the ignition condition by achieving the hotter ion mode as shown in Fig. 4.

Actually stochastic heating by super-Alfvénic instability induced by neutral beam injection (NBI) ($V_{\text{NBI}} > 4 V_A$) has been proposed to explain the hot ion mode of $T_i/T_e = 1.5 \sim 2.0$ observed in NSTX [53]. Here, V_{NBI} is the injected particle speed and V_A is the Alfvén velocity. In a D-³He ignition plasma, the thermal velocity of the fusion products with high energy proton with 14.7 MeV and alpha particle with 3.5 MeV are much larger than the Alfvén velocity for $B_{t0} = 4.4$ T and $n_e(0) \sim 3 \times 10^{20} \text{ m}^{-3}$ such as $V_\alpha \sim 3.8 V_A$ and $V_p \sim 7.5 V_A$, respectively. As the toroidal field is decreased due to the diamagnetic effect, the Alfvén velocity is further decreased. Active study on this subject is highly recommended for application of ST to both D-T and D-³He ST reactors. Alpha and “proton channeling”, where the RF wave can transport the energy to ion preferentially, should be also studied more actively [54].

4.8. Prompt Fusion Product Loss

We have assumed that the fusion product particles with the high energy are confined perfectly. In recent experiments in NSTX, however, the loss of high energy particles are observed due to the toroidal Alfvén eigen-mode (TAE) or chirping instabilities [55]. When 5 % of all the fusion products are lost promptly, it is difficult to maintain ignition in the same condition as shown in Fig. 2 ($n_D : n_{\text{HE3}} = 0.42 : 0.58$). In such case, ignition can be maintained if deuterium fuel ratio is slightly increased to $n_D : n_{\text{HE3}} = 0.45 : 0.55$ although the local heat flux to the first wall would be increased by the prompt loss of the fusion product particles.

4.9. Other Scaling

Recently, it has been observed in JET and DIII-D tokamaks that the confinement time scaling does not depend on the beta value. If it holds in a high beta ST, it is very much favorable. So we have examined the ignition access using the electrostatic gyro-reduced Bohm scaling [56] given by

$$\begin{cases} \tau_{\text{ESGRB}} = 0.028 A_i^{0.14} I_p^{0.83} [\text{MA}] n_{19}^{-0.49} [\times 10^{19} \text{ m}^{-3}] R_o^{2.11} [\text{m}] \epsilon^{0.3} \\ \quad \times \kappa^{0.75} B_{t0}^{0.07} [\text{T}] / P_{\text{NET}}^{0.55} [\text{MW}] \\ \tau_E = \gamma_{\text{HH}} \tau_{\text{ESGRB}} \end{cases} \quad (4.1)$$

To obtain the similar discharge and ignition performance as shown in Fig. 2, the confinement enhancement factor γ_{HH} can be reduced from 2.0 to 1.6 in the steady state. Therefore, this electrostatic gyro-reduced Bohm scaling is favorable for D-³He operations.

4.10. On Pioneering Works

In the pioneering work on D-³He spherical tokamak ignition study, the synchrotron radiation was not radially averaged due to the large toroidal field gradient. The aspect ratio of $A = 1.2$

and the minor radius of $a = 2.0$ m provides the major radius $R_o = 2.4$ m [8]. The density profile is the same as ours and the temperature profile ($\alpha_T = 1.5$) is more peaked than ours. This proposed size is too small for a D-³He fusion reactor by our analysis with the present confinement scaling. However, the volume averaged synchrotron radiation used in our work may be underestimated as pointed out by the earlier work [8]. More elaborate studies on the synchrotron radiation loss in a spherical tokamak should be developed. The energy transfer fraction of the fusion energy to ions is assumed to be $F_{ion} = 0.2$ [8] which is much smaller than our assumption $F_{ion} = 0.4$.

Our proposal is not much different from Stambaugh's estimation ($R_o = 5.25$ m, $a = 3.75$ m, $B_{to} = 2.7$ T) [9]. However, as the density and temperature profiles ($\alpha_n = \alpha_T = 0.25$) are much broader than our assumption with more peaked density ($\alpha_n = 0.5$) and temperature ($\alpha_T = 1.0$) profiles, it may be more difficult to reach ignition. In a D-³He fusion reactor, peaked profiles are inevitably necessary for ignition.

5. Summary

A proposed superconducting D-³He ST reactor with the parameters of $R_o = 5.6$ m, $a = 3.4$ m, $B_{to} = 4.4$ T, the external heating power of < 300 MW and the confinement enhancement factor less than $\gamma_{HH} = 2.5$ over IPB98(y,2) scaling, looks feasible based on the present data base.

Largest problem is to maintain the hot ion mode in the high-density regime. As a nuclear elastic scattering ensures 40 % of the fusion power going to ions [23], it is at least possible to achieve the hot ion mode in the proposed D-³He ST reactor. If stochastic heating by super-Alvénic instability and alpha and "proton channeling" exist in addition to nuclear elastic scattering, it is easier to achieve the hotter ion mode in a D-³He ST reactor.

The large plasma current of more than 90 MA, automatically induced by the heating power and vertical field, provides a wide operation regime.

The ignited operation with the low wall reflectivity and medium particle confinement time ratio up to 4, close to the present database, is also possible although neutron power is large. On the other hand, if a lower particle to energy confinement time ratio is achieved, deuterium fuelling can be reduced, leading to the low neutron power of 55 MW. Nuclear spin polarization of D and ³He and D-D fusion suppression could further reduce the neutron power to 1.9 MW, which is the ideal theoretical limit.

In this reactor, the bremsstrahlung and synchrotron radiation losses to the first wall (heat flux $\sim 1\text{MW/m}^2$) could be converted to an electric energy through the super-critical pressurized water coolant or the organic coolant together with the divertor heat load.

If the bootstrap current fraction is less than 100 %, the plasma current decays and eventually terminates after a long pulse DC operation. A "quasi-continuous cyclic DC operation" with the thermal storage unit can be employed, which is similar to the quasi-continuous AC operation. This operation is only one solution for a D-³He ST reactor if the bootstrap current fraction is less than 100%.

Although assumed physics parameters are approaching the present database, more studies are required for demonstrating the possibility of a D-³He ST reactor.

Appendix

A.1. Zero-Dimensional Power Balance Equations

Each term in the power balance equation in Eq. (2-10) and (2.11) is described as follows [21].

1. The volume averaged total plasma conduction loss is

$$\bar{P}_L = \frac{1.5(f_D + f_{HE3} + f_T + f_p + f_\alpha + 1/\gamma_i)n_e(0)T_i(0)1.6 \times 10^{-19}}{(1 + \alpha_n + \alpha_T)\tau_E} \quad (\text{A.1})$$

The power in the confinement time τ_E , Eq.(A-4) , is given by the net heating power

$$P_{NET} = P_{EXT} + (\bar{P}_{oh} + \bar{P}_{FP} - \bar{P}_b - \bar{P}_s)N_o$$

2. The volume averaged ohmic heating power is $\bar{P}_{oh}$ including the neoclassical resistivity.
3. The volume averaged bremsstrahlung loss is $\bar{P}_b = A_b n_e(0)^2$ which includes the ion-electron and electron-electron scattering with the relativistic effects. The coefficient A_b is given by

$$\begin{aligned} A_b = 1.5 \times 10^{-38} Z_{eff} & \left[\frac{1}{1 + 2\alpha_n + 0.5\alpha_T} + \frac{1}{1 + 2\alpha_n + 1.5\alpha_T} g \frac{2T_e(0)}{mc^2} \right] \sqrt{T_e(0)} \\ & + 3.0 \times 10^{-38} \left[\frac{1}{1 + 2\alpha_n + 1.5\alpha_T} + \frac{0.5}{1 + 2\alpha_n + 2.5\alpha_T} g \left\{ \frac{T_e(0)}{mc^2} \right\} \right. \\ & \quad \left. - \frac{3}{1 + 2\alpha_n + 3.5\alpha_T} g \left\{ \frac{T_e(0)}{mc^2} \right\}^2 \right] \frac{2T_e(0)^{1.5}}{mc^2} \end{aligned} \quad (\text{A.2})$$

where mc^2 is the electron rest energy.

4. The volume averaged synchrotron radiation loss is $\bar{P}_s = A_s n_e(0)^2$ which includes the relativistic effects, the torus effects, and the non-reflecting surfaces such as holes. The coefficient A_s is given by

$$\begin{aligned}
A_s = & 2.5 \times 10^{-56} T_e(0)^4 \gamma_i^{1.5} \frac{(f_D + f_{HE3} + f_T + f_p + f_\alpha + 1/\gamma_i)^{1.5}}{\beta(0)^{1.5} \sqrt{aB_{to}}} \left\{ f_H + (1 - f_H) \sqrt{1 - R_{eff}} \right\} \\
& \times \int_0^1 \left[(1 - x^2)^{0.5\alpha_n + 2.5\alpha_T} \left\{ + T_e(0)(1 - x^2)^{\alpha_T} / 204000 \right\} \right. \\
& \times \left. \left\{ - \beta(0)(1 - x^2)^{\alpha_n + \alpha_T} \right\}^{5/4} \sqrt{\sqrt{T_e(0)}(1 - x^2)^{0.5\alpha_T} + \frac{2a}{R_o} \sqrt{\frac{mc^2}{2\pi}}} \right] 2x dx
\end{aligned} \tag{A.3}$$

where R_{eff} is the wall reflectivity, f_H is the hole fraction for divertor and ports ($f_H=0.1$), the central toroidal beta is given by $\beta(0) = \langle \beta \rangle (1 + \alpha_n + \alpha_T)$ with the average toroidal beta $\langle \beta \rangle = (f_D + f_{HE3} + f_T + f_p + f_\alpha + 1/\gamma_i) n_e(0) T_i(0) / \{ (1 + \alpha_n + \alpha_T) (B_o^2 / 2\mu_o) \}$.

A.2. Confinement Scaling

The following IPB98(y, 2) confinement time scaling [16] has been used for the global plasma conduction loss .

$$\begin{cases} \tau_{IPB98(y,2)} = 0.0562 A_i^{0.19} I_p^{0.93} [MA] n_{19}^{-0.41} [\times 10^{19} m^{-3}] R_o^{1.97} [m] \epsilon^{0.58} \\ \quad \times \kappa^{0.78} B_{to}^{0.15} [T] / P_{NET}^{0.69} [MW] \\ \tau_E = \gamma_{HH} \tau_{IPB(y,2)} \end{cases} \tag{A.4}$$

A.3. Control Algorithm of Fueling and Fuel Ratio

The total fueling of D-³He particles is controlled by the fusion power P_f with PID or PI controller as presented here.

$$S_{DHE3}(t) = S_{DHE30} \left\{ e(P_f) + \frac{1}{T_{int}} \int_0^t e(P_f) dt + T_d \frac{de(P_f)}{dt} \right\} G_{fo}(t) \tag{A.5}$$

where $e(P_f) = 1 - P_f / P_{fo}(t)$, P_f is the total fusion power, $P_{fo}(t)$ is the time dependent set value of the fusion power and $G_{fo}(t)$ is the time variable gain proportional to the fusion power, the integration time of $T_{int} = 10$ s and the derivative time of $T_d = 0.5$ s have been used in this paper.

In a D-³He reactor, fuel ratio control is very important because the neutron power depends on the fuel ratio. Here, each component of D-³He fuels is controlled by the cascade algorithm [26],

$$S_D(t) = \left\{ \left[\frac{n_D(0)}{n_D(0) + n_{HE3}(0)} \right]_o + G_{NDHE3} \left(e(n_D / n_{HE3}) + \frac{1}{T_{DHE3int}} \int_0^t e(n_D / n_{HE3}) dt \right) \right\} S_{DHE3}(t) \quad (A.6)$$

$$S_{HE3}(t) = \left\{ \left[\frac{n_{HE3}(0)}{n_D(0) + n_{HE3}(0)} \right]_o + G_{NDHE3} \left(e(n_{HE3} / n_D) + \frac{1}{T_{DHE3int}} \int_0^t e(n_{HE3} / n_D) dt \right) \right\} S_{DHE3}(t) \quad (A.7)$$

where $[]_o$ shows the externally set value, $G_{NDHE3}=10$ is the gain of the fuel ratio, $T_{DHE3int}=10$ s is the integration time of fuel ratio, and

$$e(n_D / n_{HE3}) = \left[\frac{n_D(0)}{n_D(0) + n_{HE3}(0)} \right]_o - \frac{n_D(0)}{n_D(0) + n_{HE3}(0)} \quad (A.8)$$

$$e(n_{HE3} / n_D) = \left[\frac{n_{HE3}(0)}{n_D(0) + n_{HE3}(0)} \right]_o - \frac{n_{HE3}(0)}{n_D(0) + n_{HE3}(0)} \quad (A.9)$$

In the steady state with $e(n_{HE3}/n_D) \rightarrow 0$ and $e(n_D/n_{HE3}) \rightarrow 0$, the deuterium and ^3He fueling rates approach $S_D(t) \rightarrow [n_D/(n_D+n_{HE3})]_o S_{DHE3}(t)$, and $S_{HE3}(t) \rightarrow [n_{HE3}/(n_D+n_{HE3})]_o S_{DHE3}(t)$, respectively. The fuel ratio in this study is defined by $n_D(0) : n_{HE3}(0) = n_D(0)/(n_D(0) + n_{HE3}(0)) : n_{HE3}(0)/(n_D(0) + n_{HE3}(0)) = 2/3 : 1/3 \sim 0.3 : 0.7$.

A.4. Control Algorithm of the Heating Power Based on the H-Mode

When the net heating power P_{NET} is larger than the H-mode threshold power P_{thresh} , the H-mode can be maintained. As H-mode power threshold is established for the total power balance in experiments, we introduce the H-mode level as $P_{NET}/P_{thresh} = M_{HL0} > 1$. The external heating power is given by this relationship as

$$P_{EXT}(HL)[W] = M_{HL0} \times 10^6 P_{thresh} - (\bar{P}_{oh} + \bar{P}_{FP} - \bar{P}_b - \bar{P}_s) V_o \quad (A.10)$$

using the net heating power $P_{NET} = P_{EXT} + (\bar{P}_{oh} + \bar{P}_{FP} - \bar{P}_b - \bar{P}_s) V_o$ and the H-mode threshold power [26]

$$P_{thresh}[MW] = 2.84 \bar{n}^{0.58} [10^{20} m^{-3}] B_{to}^{0.82} [T] R_o^{1.0} [m] a^{0.81} [m] / A_i \quad (A.11)$$

with $\bar{n}$ being the line averaged density, B_{to} being the vacuum toroidal field, R_o being the major radius, a being the minor radius, and A_i being the mass factor composed of the fuel particles, given by

$$A_i = \frac{2n_D(0) + 3n_{HE3}(0) + n_T(0)}{n_D(0) + n_{HE3}(0) + n_T(0)} \quad (A.12)$$

A.5. Plasma Circuit Equation with Vertical Field Coils and Divertor Coils

The plasma circuit equation with the vertical coil current I_V , the shaping coil current I_{sh} , the divertor coil current I_{div} and with OH coils inducing the voltage V_{OH} is given by [5][6]

$$L_p \frac{dI_p}{dt} + R_p(I_p - I_{CD} - I_{BS}) = - \left(M_{pV} \frac{dI_V}{dt} + M_{psh} \frac{dI_{sh}}{dt} + M_{pdiv} \frac{dI_{div}}{dt} \right) \quad (A.13)$$

where L_p is the plasma inductance, M_{pV} is the mutual inductance between the plasma center and the vertical coil, M_{psh} is the mutual inductance between the plasma center and the shaping coil, and M_{pdiv} is the mutual inductance between the plasma center and the divertor coil. I_p is the plasma current, I_{CD} is the non-inductively driven current estimated by

$$I_{CD} = \frac{\eta_{CD}}{nR_o} P_{CD} \quad (A.14)$$

where $\bar{n}$ is the line average density, η_{CD} is the current drive efficiency with the value $\eta_{CD} = 0.25 \times 10^{20} [\text{Am}^{-2}\text{W}^{-1}]$, and R_o is the major radius. I_{BS} is the bootstrap current given by

$$f_{BS} = I_{BS} / I_p = C_{BS} \sqrt{\varepsilon} \beta_p \quad (A.15)$$

with f_{BS} being the bootstrap current fraction, ε is the inverse aspect ratio, $C_{BS} = 1$ is assumed in this study unless otherwise noted, R_p is the neo-classical plasma resistance given by

$$R_p = \eta_{NC} \frac{2\pi R}{\pi a^2 \kappa} \quad (A.16)$$

where η_{NC} is the neo-classical resistivity and κ is the plasma elongation.

The vertical field B_{VE} produced by the vertical field coil current I_V placed at the radial and vertical position of (R_V, H_V) , by the shaping coil current I_{sh} at (R_{sh}, H_{sh}) , and by the divertor coil current I_{div} at (R_{div}, H_{div}) are given by

$$B_{VE} = B_{zoV} I_V + B_{zodiv} I_{div} + B_{zossh} I_{sh} \quad (A.17)$$

where, we define that the direction of I_V has the clockwise direction (-), I_{sh} the clockwise direction (-) and I_{div} the counter-clockwise direction (+) for I_p the counter-clockwise plasma current (+). Therefore, the divertor coil activation reduces the plasma current.

Using the current ratio of $\alpha_{div} = I_{div}/I_p$ and $\alpha_{sh} = I_{sh}/I_V$, and inserting I_V (obtained from (A-17)) into (A-13), we have the equivalent circuit equation

$$L_{peff} \frac{dI_p}{dt} = -R_{peff} I_p - \left\{ \frac{M_{pV} + M_{psh} \alpha_{sh}}{B_{zoV} + B_{zosh} \alpha_{sh}} \right\} \frac{dB_{VE}}{dt} \quad (A.18)$$

where the effective inductance is

$$L_{peff} = L_p + \left\{ M_{pdiv} - \left\{ \frac{M_{pV} + M_{psh} \alpha_{sh}}{B_{zoV} + B_{zosh} \alpha_{sh}} \right\} B_{zodiv} \right\} \alpha_{div} \quad (A.19)$$

and the effective plasma resistance is

$$R_{peff} = -R_p (1 - f_{CD} - f_{BS}) \quad (A.20)$$

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SHORT COMMUNICATIONS

A PRAGMATIC COURSE IN NUCLEAR ENGINEERING

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Abstract

Global warming and energy crises are demanding new directions in sustainable engineering. Academia needs to summarily respond with enhanced curricula that will better prepare students for the changing world. While perspectives are shifting, only a limited number of American programs have nuclear-related components. Due to financial constraints, a new engineering department is often unattainable. Often a new course concentration may not be achievable due to student population or institutional interest. At a minimum, a single introductory course would benefit the modern education of all engineering majors. This technical elective can also provide multi-disciplinary team experiences which may be lacking due to ever-decreasing degree hour requirements.

A nuclear-related course will comply with common university goals by enhancing engineering curricula, providing more qualified non-nuclear engineers for the nuclear industry, and improving faculty teaching competencies. The only required cost is faculty time; enough interest exists in academia that at least one faculty from each technical community will be interested. The resulting nuclear training is an educational benefit for both students and the instructor. The appropriate lecturer needs little experience with nuclear systems in particular but does need a broad-based view of complex engineered systems. In fact, the examination of a new technical field may have the added benefit of research stimulation. The development of nuclear engineering education will also improve student recruitment and industrial collaboration. However, participation from the nuclear industry may be a challenge due to confidentiality and security. If possible, nuclear plant site tours can contribute to the educational experience by allowing students to see full-scale systems in a practical application.

As an example, the innovative initiation of nuclear technical education at the University of Mississippi is detailed. Nuclear power generation will be employed as the model application of interdisciplinary systems engineering. Promoting technical appreciation rather than apprehension of nuclear technology, the proposed course relates nuclear systems

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engineering, safe reactor design, infrastructure sustainability, and environmental management. Including a sample outline, course development, assessment, dissemination, and sustainability plans are discussed from the viewpoint of a professor with nuclear industrial experience.

Introduction

Due to environmental and economic concerns, sustainability is now at the forefront of the engineering profession. As the greatest user of world energy resources, the United States of America should certainly contribute to energy technological education and development. Academia needs to summarily respond with enhanced curricula that will better prepare students for the changing world. While global perspectives are shifting, only a limited number of American programs have nuclear-related components. Twenty-four universities have isolated nuclear-related programs, including Nuclear or Radiological Engineering, Nuclear Science, or Nuclear Physics; some are strictly graduate programs related to industrial research and development. Another nineteen programs exist regarding Health Physics [1].

Neither the United States nor the European Union has a comprehensive system for nuclear engineering education. Russia's higher education system may be considered a model of how industry and education can progress together. As the nuclear industry required quality specialists, the education system responded accordingly. Russian universities have served as a direct supply to fulfill nuclear needs. In fact, a "Training Methodological Commission" for each nuclear-related emphasis ensures that industrial needs are met [2]. However, this system is more equivalent to technical certification training in the U.S. system.

A new engineering department is often unattainable without extreme financial and time investment. Additionally, a new course concentration may not be achievable due to student population or institutional interest. At a minimum, a single introductory course would benefit the modern education of all engineering majors. This nuclear-related systems technical elective can

- Provide more qualified non-nuclear engineers for the nuclear industry
- Enhance engineering curricula with a truly multidisciplinary course
- Improve faculty teaching and research competencies

The ultimate effect is the promotion of technical understanding rather than general apprehension of nuclear technology. While mass media tends to feed public fears, risk perception can be reduced through education [3]. For the most part, the more people understand the risks and benefits, the more comfortable they become with nuclear power. However, a thin line divides education and persuasion. Proponents of nuclear energy must be careful not to present bias versus facts, such as selecting statistics. Sung and Vaganov explore both sides of this argument with conflicting findings. Technical education alleviates fears of technically-minded people, but overall public opinions are strongly formed against nuclear power expansion. The inertia of this opinion is an enormous and often irrational risk perception [4].

Motivation

The world's energy crisis is deepening as population grows and oil prices skyrocket. Public attitudes are changing with increased rolling blackouts and energy costs. More and more countries have turned to nuclear plants to ease their power grids. Misperceptions have existed even in the technical community, contributing to a shortage of qualified graduates in the nuclear-related industries. Due to the nominal number of nuclear engineering programs, more non-nuclear engineers are hired than nuclear engineers, making proper training even more vital. The demand is especially great as the current nuclear workforce ages.

Academia needs to respond by at least providing future engineers the opportunity to choose careers in the nuclear industry. The students must be informed of facts, not fiction, regarding nuclear so that they can make well-informed decisions. As the Bureau of Labor Statistics reports, the number of employees in the nuclear industry fell from 72,000 in 1990 to 56,000 in 2001. As of 2002 employee numbers began increasing, leveling out to 62,000 in 2006. These numbers of employees may be sufficient to support the current state of nuclear in the U.S. but not a nuclear resurgence. Just 0.61% of engineers work in this growing industry. While demand is skyrocketing, employee compensation is also quite high. Engineers in the nuclear industry command the third highest salaries of all engineers. The average engineer earns \$66,190 while the average nuclear engineer earns 39% more, or \$92,040. In fact, the top 10% of the industry's engineers earn at least \$124,510. Current compensation rates are itemized by job duty in Table 1.

Table 1. Job duties, employed number, and compensation for 10,670 of the 14,870 nuclear professionals as of May 2006 [5].

Position in the Nuclear Industry	Population Employed	Annual Mean Wage
Management, Scientific, and Technical Consulting Services	110	\$123,990
Employment Services	80	\$120,320
Scientific Research and Development Services	4,490	\$97,530
Architectural, Engineering, and Related Services	1,820	\$91,360
Electric Power Generation, Transmission and Distribution	4,170	\$90,530

Through a nuclear-related technical elective, students can be trained for introductory positions at the 32 U.S. nuclear operating companies and 4 manufacturing companies. Positions in the nuclear industry occur in numerous states including Idaho, South Carolina, Washington, Virginia, Colorado, Nevada, Maryland, Georgia, Illinois, Ohio, Nevada, Alabama, Tennessee, and the District of Columbia. Potential jobs vary from plant design and operation to environmental effect research. Engineering students are actively being recruited by nuclear companies, such as Southern Nuclear Operating Company at the University of Mississippi. Students are drawn to this industry by interesting, environmentally-conscious work in addition to the excellent compensation.

Engineering curricula can certainly benefit from a nuclear-based technical elective. This course option can channel graduates into virtually any industry by improving their ability to

consolidate major topics in different engineering specialties. A nuclear systems technical elective will contribute to nearly all program outcomes of the Accreditation Board of Engineering and Technology (ABET). Satisfying Criterion 3's Outcomes (a), (c), and (k), the systems engineering approach will require students to assimilate information from various courses and apply their knowledge to process engineering. The topic of nuclear energy will introduce contemporary issues and critical thinking into engineering problems [Outcomes (e), (i), (j)]. The format of the course can incorporate multidisciplinary teamwork and improve communication skills [Outcomes (d), (g)]. This course will require students to participate in interdisciplinary teams which are extremely rare since most engineering seniors work on capstone projects entirely within their own majors. This interaction in a unique field may serve to engage students.

The result will be a deep comprehension of complex systems in a "global, economic, environmental, and societal context," as noted in Outcome (h). While political issues are not the focus of this technical course, professional and ethical issues must be discussed within the context of safe design [Outcome (f)] [6].

Further discipline-specific criteria can also be satisfied by a nuclear-related technical elective. Civil engineering, for example, additionally prescribes to the Body of Knowledge as published by the American Society of Civil Engineers. This document imparts the 24 ideal qualities of the engineer of year 2025. Technical goals to be addressed in this course include problem recognition and solving; design; sustainability; contemporary issues and historical perspectives; and risk and uncertainty. One final aspiration is breadth in civil engineering areas: most civil engineers do not even consider the nuclear industry as a potential employer. Professional goals to be addressed include communication, public policy, globalization, leadership, teamwork, and ethics. The coursework may also improve upon an engineer's attitude toward complex issues, thus contributing to lifelong learning [7].

Specific school and departmental objectives may also be fulfilled through a nuclear-related technical elective. The development of new educational areas and improved research diversity are especially beneficial at the school or college level, facilitating partnerships and collaborations with industry and government. As a clean fuel option, demand exists among most areas of engineering and several applied sciences. Organizational support must be obtained from each relevant department. Since each has a limited number of technical electives in its respective curriculum, department chairs need to encourage their students to enroll, thus strengthening multidisciplinary interaction. Offering seminar presentations and informational sessions in multiple arenas can boost course enrollment. The dual-listing of the course as both an undergraduate elective and a first-year graduate course should ensure sufficient student interest even in universities with smaller technical populations.

At the University of Mississippi, cooperation has been obtained from the School of Engineering as well as the departments of civil, chemical, mechanical, geological, electrical, and general engineering. This course will meet the School of Engineering's objective of preparing students with a broad-based education for entering the engineering profession, for advanced studies, and for careers in research. This endeavor is most strongly supported by the Department of Civil Engineering as it satisfies its vision of an effective state-of-the-art undergraduate curriculum. The course also supports the departmental program objectives of providing the necessary qualifications for employment so as to be productive in the workplace.

As beneficial byproducts, collaborative research efforts can be enhanced through such a course. The new expertise will encourage cooperation between colleagues in similar or even dissimilar fields. International universities have already begun developing connections to improve nuclear education. For example, a 2008 cooperative effort among three universities in Japan and Indonesia have resulted in student and faculty exchange and joint curriculum development. An additional upshot has been research coordination in the related fields, including radioactive waste processing, fuel system development, and next generation reactors [8].

Contact with current nuclear-related programs is vital for faculty self-education and course development. One extremely helpful resource is the Department of Nuclear, Plasma, and Radiological Engineering at the University of Illinois at Urbana-Champaign [9]. In order to design a course that meets workforce demands, industrial partnerships are also recommended.

Development

The modern generation of engineers at the University of Mississippi faces new energy challenges. The course instructor must provide a factual overview of major nuclear system principles. He/she will need to effectively use nuclear systems engineering as an example of a multidisciplinary system design. Developing students' capacities for integrative approaches to complex systems is fundamental to success, responsibility, and engagement in global society [10].

An introductory nuclear engineering course for non-nuclear engineers should employ several instructional approaches in order to convey systems engineering through the technical issues of nuclear power. While incorporating the nuclear power industry's specific needs is possible, an entire field cannot be covered in just one course. Nonetheless, successful course materials limit the daily scope to digestible nuggets. Even Lamarsh and Baratta's text entitled "Introduction to Nuclear Engineering" contains far more detailed information than is needed for non-nuclear engineers [11].

Course lectures will be the most beneficial when creative methods are used. With its ability to display photographs and animations, Microsoft PowerPoint is an efficient means of material presentation. However, PowerPoint slides alone are usually received negatively by students in the classroom [12]. Therefore, enhanced learning will result from the combination of group discussion and projects. Group interactions as interdisciplinary teams can be coordinated for these assignments. Relating nuclear power to individual student interests, projects will also allow students to delve into a discipline-related topic and teach it to others.

Suggested Course Outline

1. History of nuclear industry
 - a. Development
 - b. Statistical safety study
 - c. Case studies
 - i. Three Mile Island

- ii. Chernobyl
- 2. Fission processes
 - d. Isotopes
 - e. Radioactivity
 - f. Nuclear reactions
 - g. Fission energy fuel cycle
- 3. Design principles
 - h. Operational characteristics
 - i. Reactor components
 - i. Reactor kinetics
 - ii. Thermodynamic cycles
 - j. Safety components
 - i. Safety procedures
 - ii. Containment structures
 - k. Sustainability
- 4. Reactor types and components
 - l. Pressurized water reactors
 - m. Boiling water reactors
 - n. Additional reactor designs
- 5. Radiological management
 - o. Radiation dosimetry
 - p. Natural environmental radiation
 - q. Safe level determination
 - r. Health physics
 - s. Radioactive waste management
 - i. Waste generation as compared to other industries
 - ii. Waste treatment and recycling
 - iii. Waste storage
 - iv. Environmental remediation
 - v. U.S. regulation
- 6. Advanced topics

Topic Descriptions

I. History of Nuclear Industry

The discovery of natural radiation phenomena will be briefly introduced. Varying from water to weapon, the development of radiological engineering uses will be discussed. Public policy issues will also be introduced to aid students in selecting research paper topics that relate to their own interests. As media examples, the Three Mile Island and Chernobyl incidents will be examined for public opinion. Including environmental and health risks, direct statistical comparisons will be made between the nuclear industry and other means of power generation.

II. Fission Processes

A discussion of nuclear fission processes must begin with atomic chemistry. The properties of different radioactive isotopes will be discussed as each relates to a practical application such as corrosion inhibition or medical tracing. The fission energy cycle will be described through nuclear chain reactions beginning with fuel pellets or rods. The uranium enrichment process will be addressed as a self-sustaining fuel source.

III. Design Principles

Major factors to be discussed involve operation, mechanics, and safety. The economics of nuclear power are important to the resurgence of nuclear. Reactor design criteria will be analyzed through selection of reactor components for primary and secondary systems. The operation and design of the reactor will be described through systems instruction including the core and steam systems. Components to be incorporated are turbines, condensers, pumps, compressors, distillers, boilers, and generators. The engineering design controls for power generation and distribution components will be stressed as will thermodynamic cycles such as the Rankine steam turbine cycle, Brayton gas turbine cycle, and Carnot efficiency.

The safety components involved in the reactor design are numerous, and the risk analysis extends into several fields. Human factors are interwoven with emergency operating procedures, so criticality and accident scenarios will be discussed. The combination of design, equipment, and personnel reliability will be related to incidents, such as that at Three Mile Island. Security efforts will also be discussed: fears of weapons proliferation and terrorist attacks have restrained the nuclear industry worldwide. Radiation containment via metal cladding, reactor vessel, and concrete shielding will be related to aging plants. The water chemistry affects corrosion properties so it influences reactor design selection.

IV. Reactor Types and Components

Several reactor types and their mechanical layouts will be presented. The advantages and disadvantages of each will be discussed. For instance, the economic benefits of pressurized water reactors will be weighed against the higher pressure requirements. Sizing and design specifications will include fuel rod assembly, reactor vessel construction, power loop requirements, and steam generator types. A similar analysis of boiling water reactors is required to understand the potential dangers as those at Chernobyl. Additional reactor designs may include light water reactors, heavy water reactors, high temperature gas cooled reactors, very high temperature reactors, and liquid metal fast reactors. The reprocessing reactor and fast breeder reactor will be offered as sustainable power options. Smaller and potentially safer reactor options will also be presented.

V. Radiological Management

The detection of radiation will be discussed. The significance of human exposure to various quantities of millirems will be discussed. Comparisons will be made between common sources of exposure (air travel, natural foods, medical use, etc.) and living near or working in a nuclear power plant. The absorbed radiation in the human body depends upon several

factors including spatial distance, oxygen concentration, energy transfer rate, and bioresponse. Radiation overdose can create respiratory, gastrointestinal, neurological, vision, skin, and birth abnormalities.

Radioactive waste management will be addressed in detail. Significantly less than other power generation industries, relatively small amounts of waste are generated due to the high efficiency of nuclear power plants. However, this waste does retain radioactive properties and must be properly handled. High radioactivity wastes can be recycled or reprocessed through lower level processes, such as medical radiation uses, to obtain waste with low levels of radioactivity. However, the half-life of even these materials can be millions of years, so safe storage is a necessity. Like garbage in general, burial is the most common storage form, but the locations of such facilities is extremely controversial. Currently, U.S. laws require safe storage for 10,000 years: a brief discussion of U.S. laws will take place. Environmental remediation will also be addressed through treatment options often called "dilute and disperse, delay and decay, concentrate and confine."

VI. Advanced Topics

Numerous issues involving the nuclear industry are well-suited for technical student projects. A research paper and/or presentation should be required of each student enrollee. The research subjects will span the possible variety of student backgrounds from biology to electrical engineering. The project objective is to relate nuclear power to student interests while improving their communication skills. Ensuring relevancy, contemporary topics should be used for student projects. Suggested topics include the following:

- Non-Destructive Testing: Flaw Detection Using Iridium and Selenium
- Electronics Using Radioactive Materials as Semiconductors
- Biomedical Radiation: Diagnostic Imaging and Cancer Therapy
- Nuclear Propulsion of Naval Vessels
- The Use of Nuclear Distillation for Desalination
- Space Power: Rocket Propulsion and Cosmic Radiation
- Global Warming: How the Nuclear Industry Can Change Greenhouse Gas Emissions
- The Mining and Enriching of Uranium
- Gamma Radiation Sterilization as Insect Control
- Food Preservation through Irradiation
- Using the Earth as a Geothermal Energy Source
- Fusion in Our Galaxy
- Yucca Mountain: Storage Technology, Strategy, and Difficulties
- NIMBY: Not In My BackYard!
- Bioremediation through Flora and Fauna

Assessment and Sustainability

Evaluation plans include both course assessment of the students and student assessment of the course. Students are not expected to become experts on nuclear energy: as this course will

survey the field, students should develop an integrated concept of nuclear systems. While the instructional value of examinations is arguable, select practice should be included to measure students' technical comprehension and relate directly to the industrial requirements of new employees. Student presentation skills can be evaluated by the professor as well as other students based upon their own understanding of each topic. Indirectly assessed through confidential surveys, enrollees will be asked to rate technical level, material presentation, and course format. Questions should be formulated to gauge the success of course objectives, and comments should be utilized to improve future course offerings.

Once the academic expertise is developed, this course is sustainable for future offerings. No equipment or supplies are required but may be useful. Supplemental technical support is recommended for website development. Continually expanding, a digital online library aids in distance learning as well as the development of other nuclear-related courses both in-house and worldwide. If at all available, a nuclear power plant site visit is suggested to aid in student visualization of schematic diagrams as connected to actual plants. As part of the course, enrolled students can heavily document the plant visit, and journals can be compiled and published for internet publication. Institutional interest may also lead to larger contributions to the technical community, such as a nuclear engineering sequence or professional engineering workshop.

Conclusion

The development of nuclear engineering at an American institution of higher learning should increase student recruitment while developing relationships with industry. Nuclear technology instruction will be a rewarding experience for the students as well.

The use of nuclear engineering as a "grabber" can encourage student comprehension of complex systems engineering. The new pool of more qualified graduates will attract potential students in addition to drawing recruiters from multiple industries to the university. This comprehensive technical elective will be also attractive to several majors, providing a variety of students with the initial tools to work in a nuclear field. New hires will already possess general knowledge of nuclear technology, reducing on-the-job training at company expense. Since the students will understand nuclear principles prior to employment, greater worker retention rate also is expected. By expanding both engineering curricula and faculty competencies, this single multidisciplinary course can pay both educational and research dividends.

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Author Biography



Dr. Elizabeth K. Ervin has been a professor at the University of Mississippi since August 2006. She also worked in the Naval Nuclear Propulsion Program for nearly five years and has published several papers in the area of impact in nuclear structures.

LOW-DENSITY-PLASTIC-FOAM CAPSULE OF CRYOGENIC TARGETS OF FAST IGNITION REALIZATION EXPERIMENT (FIREX) IN LASER FUSION RESEARCH

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Abstract

Development of foam capsule fabrication for cryogenically cooled fuel targets is overviewed in the present paper. The fabrication development was initiated as a part of the Fast Ignition Realization Experiment (FIREX) Project at the ILE, Osaka University in the way of bilateral collaboration between Osaka University and National Institute for Fusion Science (NIFS). For the first stage of FIREX (FIREX-1), a foam cryogenic target was designed where low-density foam shells with a conical light guide will be cooled down to the cryogenic temperature and will be fueled through a narrow pipe. The required diameter and thickness of the capsule are 500 μm and 20 μm , respectively. The material should be low-density plastics foam. We have prepared such capsules using 1) new material of (phloroglucinolcarboxylic acid)/formalin resin to control kinematic viscosity of the precursor, 2) phase-transfer-catalyzed gelation process to keep density matching of three phases of the emulsion. 3) non-volatile silicone oil as outer oil of emulsion in order to prevent hazard halogenated hydrocarbon and flammable mineral oil. The obtained foam capsule had fine structure of 180 nm (outer surface) to 220 nm (inner surface) and uniform thickness reaching to resolution limit of optical analysis ($\sim 0.5 \mu\text{m}$). A small hole was made before the solvent exchange and the drying process to prevent distortion due to volume changes. The density of dried foam was 0.29 g/cm^3 . After attaching the petawatt laser guiding cone and fueling glass tube, poly([2,2]paracyclophane) was coated on the foam surface and supplied for a fueling test of cryogenic hydrogen.

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1. Introduction

The fast ignition concept is one of the most attractive paths to inertial fusion energy [1, 2]. After the invention of the hollow cone/shell target geometry [3], the heating of the deuterated hydrocarbon plasma was demonstrated upto nearly 1 keV temperature using Gekko XII and peta watt laser facilities at Osaka University [4-6]. Fuel target technology is a key issue in fast ignition research [7, 8]. To achieve the breakeven gain, the fast ignition realization experiment (FIREX) was designed and started with construction of a 5 PW laser in the phase of FIREX-1 [9, 10] and cryogenic DT fuel target.

In our previous study on cryogenic fuel targets for a central ignition scheme [11-13], the capability of the supporting foam structure to control the fuel shape by wicking into it was demonstrated. Conceptually, the separation of the layer shape from the ambient isotherms allows considerable freedom in target design.

The design of target for FIREX-1 is shown in Fig. 1 [14]. The diameter of the fuel shell is $\sim 500\ \mu\text{m}$, which is similar to that for a central ignition target for Gekko XII. To fabricate a uniform, non-spherical solid-deuterium-tritium layer, a low-density foam supports liquid or solid fuel ($\sim 20\ \mu\text{m}$ thick) and the shell is covered with a thin ($1 - 5\ \mu\text{m}$) plastic layer.

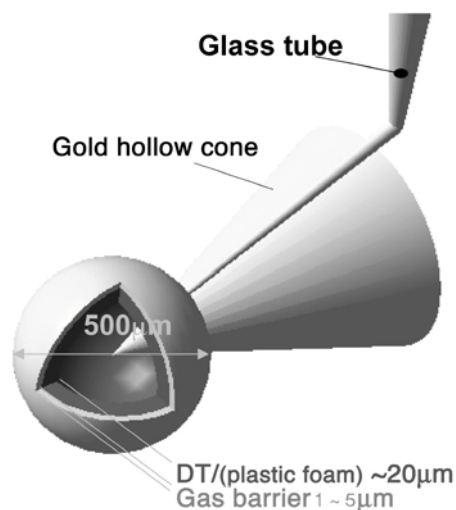


Figure 1. Schematic view of the cryogenic DT target with a plastic foam shell and a gas feeder.

There are several kinds of foam materials for IFE targets [15], and resorcinol-formalin (RF) is one of the leading materials due to its high transparency in a visible region, which allows routine characterization of shells using well-developed optical techniques including interferometry. The details of RF shell fabrication were reported by a U.S. group [16 - 19], where an RF polymer solution and density-matched oil formed an emulsion through a triple orifice droplet generator. The emulsion was converted into hollow foam shells. The density range was $100 \sim 200\ \text{mg}/\text{cm}^3$, and the thickness was $30 \sim 60\ \mu\text{m}$. A fuel barrier membrane was coated with the so-called glow discharge polymer (GDP) by their method, where the smoothness of the surface depended on the surface roughness of RF foam structure [18].

Recently, we found a new gelation method using a phase-transfer catalyst [20], which can exhibit a constant density of the oil and water phases and gave highly concentric shell,

because the phase-transfer catalyst activated the gelation reaction at room temperature and kept density matching between O and W phases. New linear polymer prepared from phloroglucinolcarboxylic acid and formalin (PF) was also invented in order to form thinner shells through an increase of viscosity of W phase [21]. To prevent hazard halogenated hydrocarbon and flammable mineral oil, a mixture of silicone oil (SO) was chosen as outer oil [22]. Here, we overview RF-PF capsule fabrication using integrated technique of the O/W/SO emulsion process, the phase-transfer catalyst, and RF-PF gel, and show the fabrication with gold cone and fueling glass tube.

2. Experimental Part

All the chemicals were commercially available and used without further purification. The water was purified by Millipore Direct-Q. The RF solution was obtained as follows: resorcinol (5.72 g, Sigma Aldrich Japan) was dissolved in 54 mL of pure water, and then 37% formaldehyde (Nacalai tesque, 7.8 mL) and 0.028 g of 2.8 wt% aqueous Na_2CO_3 (Nacalai tesque) were added. The solution was stirred at 70 C for one hour, after which the flask was cooled in an ice water bath for 30 minutes. The density of the resulting solution was measured using a floating picnometer at room temperature. The PF solution was obtained as follows, phloroglucinolcarboxylic acid (Tokyokasei, 2.5 g) was dissolved into 24 mL of pure water, then 5 mL of 1M NaOH (Nakalai tesque) was added and stirred at 70 C. A water solution of 37 % formaldehyde (Nakalai tesque) 3.43 mL was added to the solution, which was stirred at 70 C for one hour. After that, the solution was cooled in an ice bath. PF solution of $2.9 \times 10^{-4} \text{ m}^2/\text{s}$ and RF solution of $9 \times 10^{-6} \text{ m}^2/\text{s}$ were mixed at 1/2. The concentration of the solution was set to be 0.10 g/mL.

The inner oil (O_i) was chosen to be 1-methylnaphtalene. The outer oil (O_o) was chosen to be a mixture of silicone oil. To adjust the density, poly(dimethylsiloxane) (10 cSt) and poly((1,1,1-trifluoropropyl)methylsiloxane) were mixed at 1/1. Encapsulation of the RF solutions to form a compound emulsion was accomplished using a triple-orifice droplet generator. A detailed structure of the apparatus is shown elsewhere [23].

The diameter of the shell is roughly determined by the diameter of the delivery tube. A more precise adjustment of the diameter is achieved by adjusting the flow rate of the exterior oil phase. The wall thickness of the shell is determined by the ratio of the interior oil phase and RF-PF solution flow rates. The flow rates of the W, O_i and O_o liquid are 0.083 mL/min, 0.130 mL/min and 93 mL/min, respectively. An emulsion with an outside diameter (0.553 mm) and inside diameter (0.537 mm) was formed. Fifty emulsions were produced using the droplet generator. The emulsions were transferred to a drum with a 14 cm diameter and 15 cm height. The drum contains 300 mL of O_o with a 0.39% acetic acid catalyst. This drum was rotated at 95 rpm for 5 minutes, then 136 rpm for 1 minute. After that it was kept 95 rpm for 3 hours.

After the gelation, the silicon oil on the surface of the capsules was washed by 1-cSt silicone oil of (Shinetsu, Japan). The 1-cSt silicone oil was volatile and dissolved with O_o . Then, a small hole on the shell of the capsule was drilled using a driller of 40 μm in order to circumvent shrinkage. After drilling a small hole, the capsule was dipped in to the hexane to wash the inner oil of 1-methylnaphtalene and this process took about 2 hours. When the

washing had been finished, the 2-propanol was used to exchange with the water in the shell of RF-PF gel. The solvent of 2-propanol was removed using supercritical fluid CO₂.

3. Phase-Transfer Catalyst for Gelation Reaction

The hollow spherical gel was obtained by combining several techniques. The key process is density matching emulsion technique which is similar to PAMS and PS spherical capsule fabrication. In the case of spherical gel formation from RF emulsion, the gelation catalyst was activated by heating, previously. In the process, because of the density-mismatch of W and O during the reaction as shown in Fig. 2, we have not obtained highly spherical capsule.

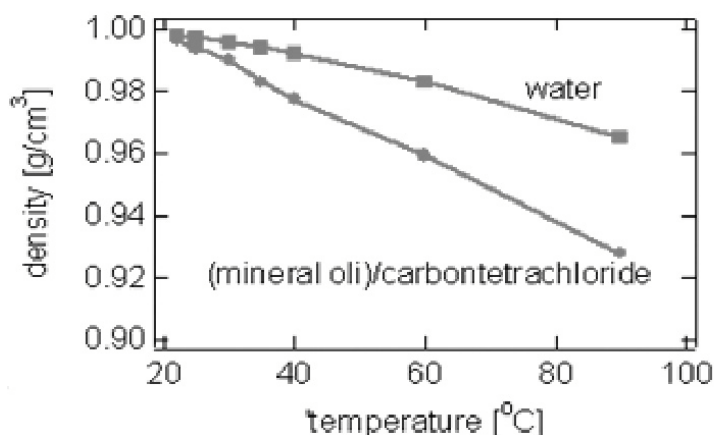


Figure 2. Density of the emulsion component depending on temperature. While the densities of the RF aqueous solution and oil (mineral oil and carbon tetrachloride) were well matched at the room temperature, density mismatch happens during heating due to different slope depending on temperature.

Figure 2 shows the densities of water and oil [(mineral oil)_{wt}/carbontetrachloride_{wt} = 12 g/7 g] depending on the temperature. The densities of water and oil were well matched at room temperature. Both densities decrease with high temperature, and the slope of the oil is higher than that of the water. But the matching was lost at high temperature, and the droplets gradually sank to the bottom. The heating activation has a dilemma of the optimization of catalyst concentration [19]. While the pore sizes and density were improved by increasing the catalyst concentration, the systems gelation time had been decreased. The faster gel time led to poor nonconcentricity for shells. Recently, we found that a phase-transfer catalysis polymerization of RF emulsion [20], where the catalyst was dissolved into the outer oil phase. The crosslinking, i.e., the gelation reaction starts gradually from the outer surface of RF solution. The gelation time was controlled by changing the concentration. For example, in the case of acetic acid, the time was 20 ~ 90 minutes for the concentration of 3.9 % ~ 0.07 %, respectively. The pore size was decreased with the catalyst concentration from 460 to 130 nm.

4. Mixing of a Linear Phenol/Formalin Polymer (Phloroglucinolcarboxylic Acid and Formaldehyde)

Next, we adopted silicone oil (SO) as an outer oil to alter toxic halogenated hydrocarbon and flammable mineral oil [21]. The adoption of silicone oil has another merit to circumvent exact density matching because of its very low volatility. Using the compound emulsion, O/W/SO, the highly concentric RF capsule was obtained by optimizing fabrication conditions such as rotation speed of the round drum, how long it kept, when it started etc.

In spite of these optimizations of manufacturing process, the minimum thickness of RF foam was 100 μm , which was 5 times as thick as the specification of the FIREX fuel target. In order to obtain thinner thickness, viscous water phase (RF solution) should be applied. Figure 3 shows the relation between minimum wall thickness (S) versus the high kinematic viscosity (ν) of water phase using a droplet generator. The higher ν value gave the thinner S values. Higher ν value of water phase than $9 \times 10^{-5} \text{ m}^2/\text{s}$ induced instability to form emulsion using the droplet generator, therefore the exact thickness values were not measured. Although we have tried to use high- ν RF solution, such viscous RF solution easily changed to gel due to crosslinking reaction in highly concentrated RF polymer. To circumvent the crosslinking of RF solution, we have adopted linear copolymer composed of phloroglucinolcarboxylic acid and formaldehyde (PF) (chemical structure is shown in Fig. 6) [22], where phloroglucinolcarboxylic acid has only two reactive positions (hydrogen) in an aromatic ring, therefore it is impossible to induce crosslinking reaction.

The viscosity of this mixture solution was chosen to be $9 \times 10^{-5} \text{ m}^2/\text{s}$, which corresponds to 20 μm wall the W phase as seen in Fig. 3.

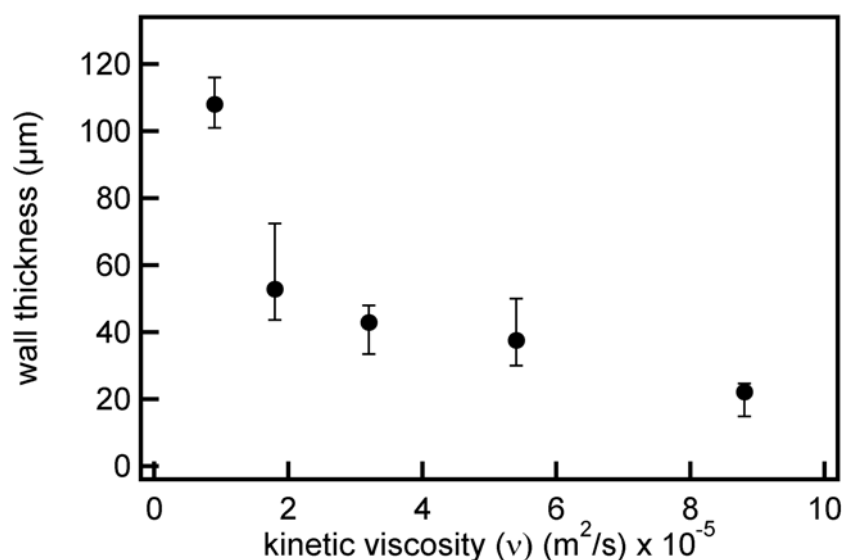


Figure 3. Wall thickness of W phase in $\text{O}_i/\text{W}/\text{O}_o$ compound emulsion depending on viscosity of W phase. O_i ; 1-methylnaphthalene, W; PVA solution, O_o ; mixture of that polydimethylsiloxane (KF-96-10cSt, Shin-etsu Chemical) and poly(1,1,1-trifluoropropylmethylsiloxane) (density = $1.018 \text{ g}/\text{cm}^3$, $\nu = 4.5 \times 10^{-5} \text{ m}^2/\text{s}$).

By using the RF-PF mixture solution and the triple-orifice droplet generator, actually emulsions with 20 μm thickness and 500 μm diameter were prepared. Thirty droplets were transferred to a drum flask (700 mL) with the silicone mixture and 0.39 % acetic acid as a phase-transfer catalyst, and then rotated and its speed increased to 95 rpm within 5 minutes. Five minutes later the rotation speed was increased to 120 rpm. The rotation speed was maintained for 1 minute, and then reduced to 95 rpm, which was maintained for 50 minutes.

5. Fabrication of Target and Its Characterization

After gelation, the capsule was exchanged with 2-propanol/water mixture 4 times whose ratio was gradually increased linearly to neat 2-propanol. Then the 2-propanol gel was dried by supercritical-fluid- CO_2 extraction. But, when a gelated capsule was dipped into 10 wt% aqueous 2-propanol, the wall of the capsule was dimpled. Therefore, part of the capsule surface was spaced using a drill, and then the O_i of the capsule was removed by hexane, and then water in the capsule was exchanged with 2-propanol. Figure 4a shows a hydrogel with a small hole. There existed small expansion (0.5 % in diameter) during exchange in to 2-propanol. After the 2-propanol was removed by extraction using supercritical CO_2 , 2% shrinkage happened in diameter. Figure 4b shows the image of dried capsule after laser machining of a hole with 300 μm diameter to attach cone guide.

The density of the foam was estimated for a hollow shell. The foam shell diameter, the thickness were 514 μm and 8 μm , respectively, and its volume was $6.5 \times 10^{-6} \text{ cm}^3$. The density was calculated to be 0.29 g/cm^3 from the volume and the mass (1.9 μg). Due to shrinkage, all of the balls had a higher density than the gel concentration. The oxygen content of RF-PF was similar to that of RF, while phloroglucinolcarboxylic acid has more oxygen than of resorcinol.

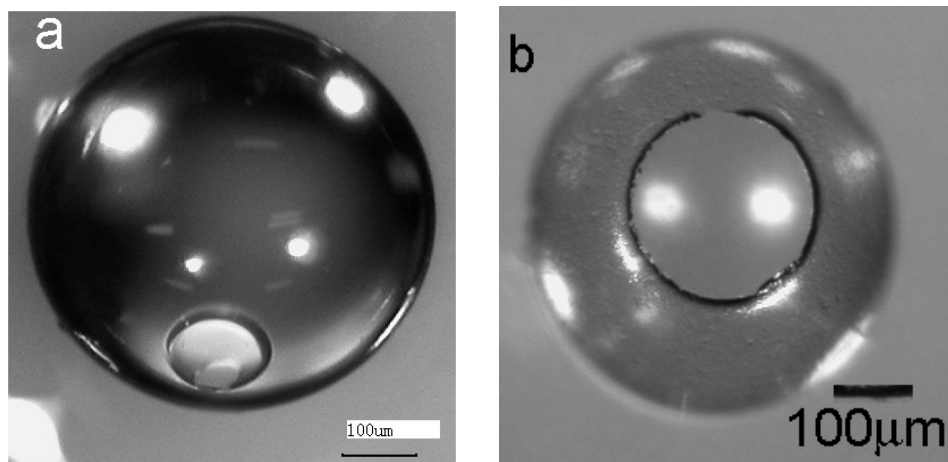


Figure 4. Optical image of RF-PF shell, a) hydrogel with a small hole to prevent distortion and before exchange of solvent, b) dried and after laser machining of a hole for attaching gold cone.

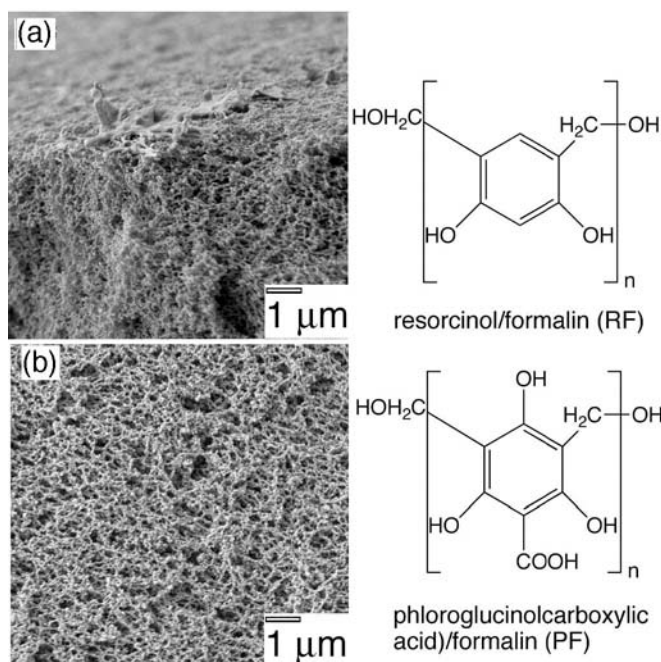


Figure 5. SEM images of surface of RF-PF foam obtained with 0.39 % acetic acid-catalyzed reaction. Images a) and b) show inner and outer surface morphologies, respectively. Chemical formula of PF and RF are shown in upper-right and lower-right sides, respectively.

Figure 5 shows SEM images of the foam balls after supercritical CO_2 extraction. The average cell size C was estimated by counting the number of cross points (N_{cross}) against a linear line of 10 μm , as follows.

$$C = 10 \mu\text{m} / N_{\text{cross}} \quad (1)$$

The C values were 180 and 220 nm for outer and inner surface, respectively. The difference would be due to the difference of the catalyst concentration, because the catalyst transfers from the outer surface to inner surface. In the previous study [20], similar tendency was observed, i.e., while the pore sizes and density were improved by increasing the catalyst concentration, the systems gelation time had been decreased. The gelation process consists of nuclear formation and structure growth, whose rates are denoted as r_n and r_l , respectively. The structure size depends on the ratio of r_n/r_l [24], i.e., the larger r_n/r_l , the smaller foam structure. In the present case, outer surface has smaller structure than inner surface, implying that the r_n/r_l value of outer surface is larger than that of inner surface. Both of r_n and r_l for outer surface should be larger because of higher catalyst concentration, and the r_n contributes more than r_l to the present gelation process.

The detailed characterization was done for gelated capsule (before exchanging solvent to 2-propanol) as shown in Fig. 6, because dried capsule has a hole drilled in order to remove 1-methylnaphthalene as described before. The analysis process is shown in elsewhere [21]. In this case of Figure 6, the outer diameter was 504 μm and the out of roundness in mode 2 was 2 μm . Distance between inner and outer surfaces shows wall thickness of the gel.

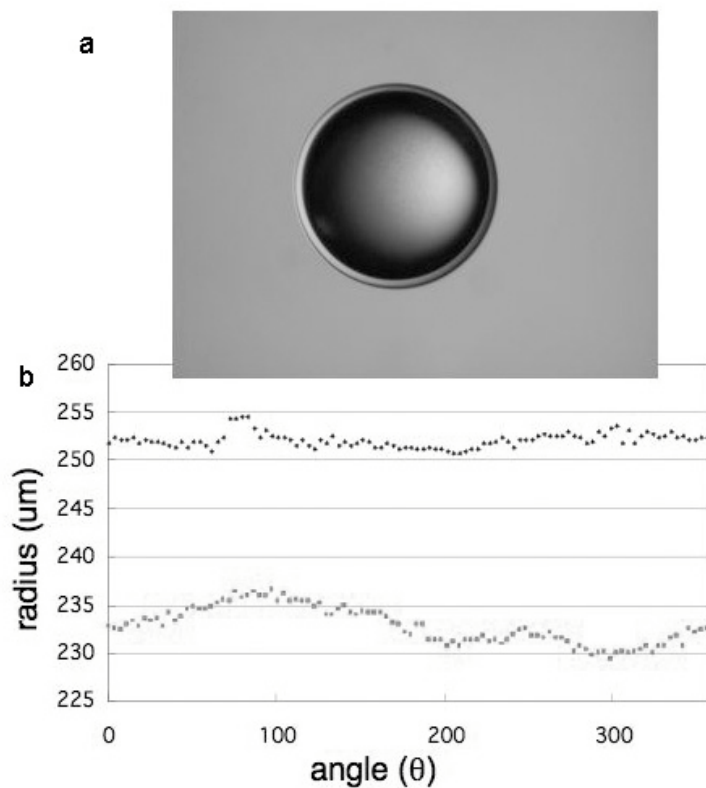


Figure 6. (a) Optical image of a gelated capsule and (b) radius depending on angle by an image analysis

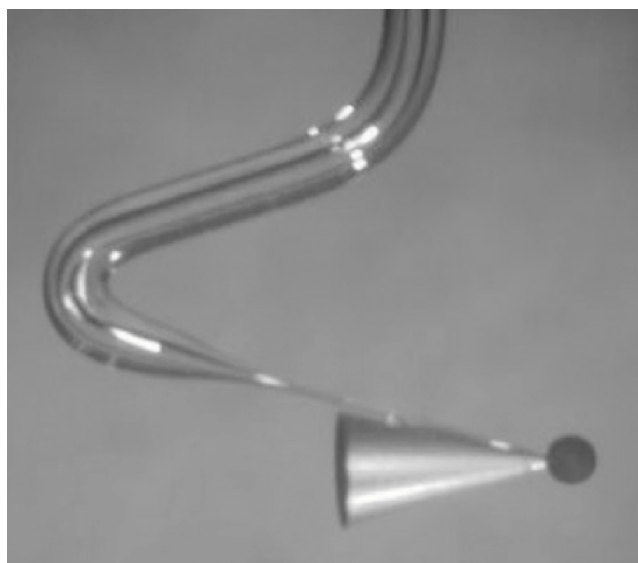


Figure 7. A target consists of RF-PF shell for cryogenic DT experiment in FIREX-1. The capsule is covered with poly([2,2]paracyclophane) film.

The average thickness of the RF-PF capsule was 19 μm and the mode 2 non-uniformity was 1.5 μm . The present optical image has resolution of the same order of the mode 2 of the thickness. Figure 7 shows a picture of a dried capsule. A large hole for increasing gold cone was fabricated using laser machining. Figure 7 is the picture after the laser process. Finally the capsule was assembled with a gold cone as for guiding of the heating laser, and a glass capillary for liquid hydrogen fueling.

6. Conclusion

We have prepared transparent foam capsule meeting with FIREX specification which is 500 μm diameter and 20 μm thickness. The achievement was owing to the phase transfer catalyst for gelation and new foam material of RF/PF mixture phase solution that was not gelated at high kinematic viscosity of $9 \times 10^{-5} \text{ m}^2/\text{s}$. The capsule with the hole was attached with gold cone guide and fueling glass tube. The target was supplied for fueling test of liquid hydrogen.

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