Dr. 1048

31
11/7/74

MATT-1050

A FUSION POWER PLANT

R. G. MILLS, EDITOR

PRINCETON UNIVERSITY
PLASMA PHYSICS LABORATORY

MASTER

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

AVAILABILITY

Copies available from:

National Technical Information Services (NTIS)
U. S. Department of Commerce
Springfield, Virginia 22151

A FUSION POWER PLANT

Edited by R. G. Mills

NOTICE

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Atomic Energy Commission, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

Plasma Physics Laboratory
Princeton University
Princeton, New Jersey

August 1974

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

"But it hasn't worked yet."

A. A. Milne

This work was supported by U. S. Atomic Energy Commission Contract AT(11-1)-3073, and the Electric Power Research Institute Project RP 113; also use was made of computer facilities supported in part by National Science Foundation Grant GP 579.

THIS PAGE
WAS INTENTIONALLY
LEFT BLANK

Contents

PART I <i>Overview</i>	1. Introduction	1
	R. G. Mills	
	2. Summary of Technical Features	10
	R. G. Mills	
	3. Advantages of Fusion Power	20
	P. Bonanos	
<hr/>		
PART II <i>Technical Features</i>	4. Plasma Characteristics and Control	24
	J. L. Johnson and S. L. Gralnick	
	5. The Operating Cycle	40
	S. L. Gralnick	
	6. The Divertor and Vacuum Systems	75
	F. H. Tenney and G. Lewin	
	7. Fuel Injection	126
	S. L. Gralnick	
	8. Magnetics	138
	U. R. Christensen	
	9. Choice of Materials	148
	F. H. Tenney	
	10. Nucleonics	164
	W. G. Price, Jr.	
	11. Heat Transfer and Energy Conversion Systems	217
	P. W. Davison and K. C. Sokolosky	
	12. The Vacuum Wall, Radiation Damage, Structural Problems.	291
	P. Bonanos and H. K. Perkins	
	13. Superconducting Magnets and Cryogenic Systems.	323
	J. File	
	14. Fuel Handling	362
	E. F. Johnson	
	15. Tritium Distribution and Leakage	411
	V. A. Maroni	

Contents

PART III System Features	16. Hazards	432
	P. W. Davison	
	17. Environmental Compatibility	458
	P. W. Davison	
	18. Operational Procedures	477
	K. C. Sokolosky and J. File	
	19. Maintenance, Repair and Overhaul	493
	T. A. Coultas	
	20. Cost Analysis.	508
	J. File	
<hr/>		
PART IV Remaining Problems	21. Uses for the Reference Design	526
	R. G. Mills	
	22. Needs for Further Research	531
	R. G. Mills	
<hr/>		
Appendices	Principal Plant Characteristics	543
	Appendix A	
	The Profiles Code	552
	Appendix B	
	Helium Heated Steam Generator	561
	Appendix C	
<hr/>		
Index		573

Chapter 1

Introduction

R. G. Mills

Personnel

This book is the result of the efforts of a large number of people. Of the fifteen authors, eight are members of Princeton University's Plasma Physics Laboratory: P. Bonanos, U. R. Christensen, J. File, S. L. Gralnick, G. Lewin, R. G. Mills, W. G. Price, Jr., F. H. Tenney; two are members of the Argonne National Laboratory: T. A. Coultas, V. A. Maroni; two are members of the Westinghouse Electric Corporation: P. W. Davison, J. L. Johnson; one is a member of the Narragansett Electric Company: K. C. Sokolosky; and two are members of Princeton University's Department of Chemical Engineering: E. F. Johnson, and H. K. Perkins.

Other contributors have included E. Greenspan who served at Princeton for nineteen months on leave from the Israeli Atomic Energy Commission and played a key role in establishing a neutron-transport capability at Princeton; T. Fujisawa, who spent a year at Princeton on leave from the Japanese Atomic Energy Research Institute to study reactor control; A. J. Impink of Carnegie Mellon University, who spent two weeks assisting in the initiation of neutron transport work; D. J. Rose of the Massachusetts Institute of Technology, who spent a week at Princeton in July of 1971 in lecturing and helping organize a catalog of problems pertinent to the study of fusion reactors; and R. Hancox of the Culham Laboratory in England, who spent a few days studying our proposed superconducting coil design and made helpful suggestions for improving it.

The book has been edited by R. G. Mills capably assisted by C. C. Hopkins, who is responsible for the Index. The computer-controlled composition has been provided by R. Slafkovsky and M. L. Halliday.

History of Interest in Fusion Reactors

As soon as the knowledge of the atomic nucleus allowed accurate measurements of the different isotopic masses, it became clear from the equivalence of mass and energy that there was a vast amount of potential energy available from the light elements, and that in principle fusion reactors should be possible, although no realistic suggestions were made for machines.¹ The stars seemed to depend on nuclear fusion energy, and thermonuclear chain reactions were proposed to explain their vast energy production.²

Fission proved to be more readily achievable on earth, and the pressures of war led to its rapid harnessing - within an amazing six years of discovery of the effect.³ Although fission workers at Los Alamos during the war had discussed possibilities for fusion apparatus, serious work was not undertaken until the early nineteen-fifties. With special stimulus from a report by Lyman Spitzer,⁴ Chairman of the Astronomy Department at Princeton, the classified Project Sherwood was born. The early history of this project is described in the book by Bishop.⁵

One of the early tasks undertaken by Spitzer was to organize a small group of engineers and scientists from industry to consider the conceptual design of a practical power plant based on the stellarator type of magnetic confinement design. This group worked together during the summer of 1954 and issued their report, the so-called "Model D Report",⁶ the following fall. This was the first effort and for many years the only detailed study of what a fusion power station might be.

Concurrently, the plasma physics research effort was growing, and larger, more powerful experimental facilities brought into operation. As experimental sophistication grew, the nature of the problem became clearer. It became increasingly more doubtful that the high betas (relative plasma pressures) assumed for Model D were realistic. It became clear that the machine as described could not operate.

The prognosis for low-beta, normal magnetic field machines was gloomy. The curves shown in Fig. 1.1 indicate how the situation appeared in March, 1961.⁷ The discovery earlier that year of high field superconductivity⁸ was the answer to the low beta problem. The 100 kilogauss superconducting line in Fig. 1.1 was essentially an afterthought (added to the figure originally prepared under the assumption that 60 kilogauss was the maximum reasonable superconducting field to assume) following the announcement of the Bell Laboratories' discovery of Nb_3Sn as a high field superconductor. This historic development made the fusion reactor again appear as a practical possibility and also raised hopes for the previously apparently unattainable small demonstration reactor.⁷

Nevertheless, the plasma physics experimental work progressed slowly and disappointingly throughout most of the sixties. At the same time the interest in fusion reactors was spreading in the technical community. The vast complex problems of plasma physics absorbed essentially all of the support available, and some people wondered if there were not engineering problems of equal or greater difficulty that might preclude successful fusion reactors or at least require substantial development efforts. At the Massachusetts Institute of Technology under the leadership of Professor D. J. Rose,

744181

Fig. 1.1. Requirements on the size and field-strength required for stellarators to produce a given amount of power for sale, P_s . The refrigeration power needed for cryogenic normal conductors (much greater than the magnet power) is represented by P_R . The magnet power for a normal conductor is represented by P_M . The magnetic field strength is plotted in kilogauss (k Γ). The assumed β (ratio of plasma pressure to magnetic field pressure) is expressed as a percent.

five theses were written in the mid sixties on engineering aspects of thermonuclear reactors.^{9,10,11,12,13}

In the latter half of that decade interest grew rapidly, culminating in the 1969 conference at the Culham Laboratory in England,¹⁴ the first large conference devoted exclusively to the technology and problems of nuclear fusion reactors. It was in that same year that the impressive performance of tokamaks was verified, and the current high optimism engendered.

Today the experimental success in tokamak-type toroids, their behavior in accordance with newly-developed theory, and the fact that extrapolation to reactor-sized devices from laboratory devices for the first time predicts successful reactors have convinced most workers in the field of thermonuclear research that scientific feasibility will be proved within the next several years.

This present state of active optimism has spawned a great number of groups organized to study the engineering problems of controlled thermonuclear reactors. Such groups are at work in Europe, especially in England and Germany as well as in Russia, France, Italy, and the Scandinavian countries; in Asia, especially in Japan; and in North America, at the University of Wisconsin, Lawrence Livermore Laboratory, Oak Ridge National Laboratory, Los Alamos Scientific Laboratory, the University of Texas, the Princeton Plasma Physics Laboratory, and elsewhere.

This book reports some of the work of the Princeton Group. For over ten years, Princeton personnel have believed that the original Model D report should be rewritten. The time did not seem ripe until 1971.

The Princeton Reactor Studies Program

Princeton's Reactor Studies Group was organized in the summer of 1971 by R. G. Mills, who assembled an international interdisciplinary group to attempt a comprehensive study of all aspects of the engineering problems associated with practical power production based on the nuclear fusion reaction. Initially supported at a meager level with funding from the Atomic Energy Commission as a small part of the activities of Princeton's Plasma Physics Laboratory, it was able to expand its level of effort significantly when the Edison Electric Institute in the winter of '71-'72 designated the Princeton Program as an approved Edison Electric Institute Research Project (No. RP 113). In addition to this formal support, industry contributed informally by providing members of their engineering staffs at no cost to Princeton to join the reactor studies effort.

Approximately eight man-years of effort are currently being expended on the

activities of the Reactor Studies Group. The major part of this effort during 1972 and 1973 was to complete the conceptual design of the fusion power plant described in this report.

In undertaking such a task, one is faced with numerous choices. Like a smorgasbord laden with multifarious tempting delicacies, the great variety of engineering challenges associated with this new form of machine can overwhelm an individual allowing him to sample sparingly from many dishes, but to do full justice to but few.

The advantage of a large group is its ability to do a complete job on many aspects of the problem and do it in a coordinated manner that avoids contradictions and inconsistencies. When studying only one part of a complex problem, it is very easy to ignore the constraints that would be imposed on that problem by requirements of other subsystems. Many assumptions must be made in such a conceptual design because of the incompleteness of current knowledge, but only those assumptions that must be made should be made.

To many plasma physics researchers, considerations of eventual power plants are of interest only if they relate to the current plasma physics research programs. In addition to the obvious needs of such research to determine the transport properties of magnetized plasmas and effective methods of heating them, the current studies have emphasized the need for plasma research on fuel injectors and on the performance of divertors. On the engineering side, the need for the development of superconducting coils is merely the top item of a long list that will become more evident to a reader as he peruses this volume.

The Model

To undertake a conceptual design of a future power plant, a selection must be made of the type of plant to study. The model should serve as a point of reference or benchmark against which to gauge changing ideas. One opinion is to describe an ultimate power plant, one that would represent the best that can be hoped for in a fusion system under reasonable assumptions of future availability of high temperature metals, high efficiency thermodynamic systems, and ideal plasma behavior. With such a model the considerable advantages of a fusion system will become very obvious, and current research can be directed toward those problems that need solution to achieve this very worthwhile objective. Opponents to this approach say that any attempt to look that far in the future can only serve to bring the model even further from reality, and may tend to oversell fusion systems. There is also some risk that such a model may be misinterpreted to imply that these numerous optimistic assumptions may be necessary to make a fusion

system appear economic and therefore reduce the credibility of fusion's future.

Carrying the credibility argument to its logical end results in a body of opinion that one's reactor model should be the minimum possible extrapolation of current laboratory experience, the smallest possible machine that would produce real output power and with a minimum of new auxiliaries and devices. In essence this is a prototype reactor not to be expected to be economic. Such a model will be much more useful in guiding one's plans for the next generation of plasma physics experiments. The argument against this point of view is that it falls short on some points. It ignores the eventual goal of economic power and therefore fails to show the potential advantages of a fusion system.

We have taken a compromise position on this question in an attempt to satisfy the critics of the "very first" and "very last" reactor model types. By choosing a first-generation commercial power plant as our target, we attempt to show the advantages of fusion power without straining technical credibility by reaching for the ultimate. By making it a near-term objective we strive to save its relevance in guiding current research.

Simplicity has influenced many of our decisions. Enough complication is forced upon us by the inherent complexity of toroidal magnetic confinement of a plasma. Wherever choices of methods or devices have appeared, we have attempted to choose the simplest possible solutions even at some sacrifice in performance.

Conservative choices have been made when possible between properties available today versus hoped-for future improvement. New developments should, as a consequence, allow favorable changes in the performance of the model.

Completeness has been a principal goal of this study. It is all too easy simply to ignore difficult problems or areas in which knowledge is lacking. We hope we have avoided this pitfall by clearly identifying where we have been forced to make assumptions, what they are, and why we have made them.

Unknowns have been treated by extrapolating current experimental data, predicting regions of probable future developments, and within this range assuming a conservative (i.e., pessimistic) result of these future developments.

The assumptions required relate to the transport properties of reactor grade plasmas, methods of injecting fuel into a reacting plasma, heating and ignition procedures, performance of plasma divertors, and the rate of embrittlement and useful lifetime of the first vacuum wall in the neutron flux. All these questions are discussed thoroughly in the text.

Organization of this Report

The book is divided into four parts: Overview, Technical Features, System Features, and Remaining Problems. The Overview is intended for those who wish a summary of the work, but who do not wish to spare the time to read the entire document. It contains three chapters: this Introduction, Summary of Technical Features, and Advantages of Fusion Power. Chapter 2 is a brief summary and condensation of the twelve chapters of Part II, while Chapter 3 does the same for the five chapters of Part III.

Part IV describes possible uses for this report and areas of future research for which needs have been discovered.

The model herein described, rather than being a science fiction reporter's view of what a future power plant might be, is supposed to be a reference design to be used as a standard against which the value of different ideas and the effect of future developments may be measured.

A careful effort has been made in the Index to facilitate the book's use as a reference.

References

- ¹G. Gamow, The Birth and Death of the Sun (The Viking Press, New York, 1945) p. 109.
- ²H. A. Bethe and C. L. Critchfield, Formation of Deuterons by Proton Combinations, Phys. Rev. 54, 248 (1938); H. A. Bethe, Energy Production in Stars, Phys. Rev. 55, 434 (1939).
- ³H. D. Smyth, Atomic Energy for Military Purposes (Princeton University Press, 1948).
- ⁴L. Spitzer, A Proposed Stellarator, U. S. Atomic Energy Commission Report NYO-993 (1951).
- ⁵A. S. Bishop, Project Sherwood (Addison-Wesley Publishing Co., Inc. Reading, Mass., 1958).
- ⁶L. Spitzer, D. J. Grove, W. E. Johnson, L. Tonks, W. F. Westendorp, Problems of the Stellarator as a Useful Power Source, U. S. Atomic Energy Commission Report NYO-6047 (1954).
- ⁷R. G. Mills, Thermonuclear Power and Superconductivity, in Engineering Aspects of Magnetohydrodynamics (Columbia University Press, 1962) pp. 515-520.
- ⁸J. E. Kunzler, E. Buehler, F. S. L. Hsu, J. H. Wernick, Superconductivity in Nb_3Sn at High Current Density in a Magnetic Field of 88 Kilogauss, Phys. Rev. Letters 6, 89 (1961).
- ⁹A. J. Impink, Jr., Neutron Economy in Fusion Reactor Blanket Assemblies, Massachusetts Institute of Technology Technical Report 434 (1965).
- ¹⁰W. G. Homeyer, Thermal and Chemical Aspects of the Thermonuclear Blanket Problem, Massachusetts Institute of Technology Technical Report 435 (1965).

¹¹L. N. Lontai, Study of a Thermonuclear Reactor Blanket with Fissile Nuclides, Massachusetts Institute of Technology Technical Report 436 (1965).

¹²P. S. Spangler, Fusion Reactor Blanket Experiment, Massachusetts Institute of Technology Technical Report 437 (1965).

¹³L. M. Petrie, Jr., Gamma-Ray Spectra in Fusion Blanket Mock-Ups, Massachusetts Institute of Technology Technical Report 438 (1965).

¹⁴Nuclear Fusion Reactor Conference Proceedings (British Nuclear Energy Society, London, 1969).

Chapter 2
Summary of Technical Features
R. G. Mills

Plant Characteristics

The electric plant described in this report produces 2030 MW of electric power for sale. The nuclear heat generated is 5305 MW. The overall net efficiency is 38%. The gross electric efficiency is 45.3%. The generators produce 2405 MW of electricity of which 375 is used within the plant, primarily for circulating the helium gas coolant. A chart of these characteristics, together with a large number of other operating parameters, is presented for quick reference in Appendix A to this document. A detailed drawing of the plant is presented on the fold-out illustration inside the back cover.

The primary fuels consumed within the reacting plasma are deuterium and tritium. Although an initial inventory of about 3 kg of tritium is required in order to begin operations, subsequent needs for tritium will be met by generating it from lithium in the lithium-beryllium-fluoride salt (flibe) present in the blanket. At full power, 1.1 kg of lithium will be consumed per day, but since the blanket contains over 160,000 kg of lithium, it may be several years before any lithium need be added to the blanket. This lithium need not be considered as a fuel but more as a maintenance and renewal expense in servicing the facility. The deuterium fuel requirements (0.35 kg per day) may be met by the purchase of heavy water. Daily consumption of D_2O will be about 1.76 kg which should cost less than eighty dollars.

Maintenance of the facility should be very similar to that for other large power stations, novel features being the cryogenic and vacuum systems. Replacement of the vacuum wall, an embrittled and activated weldment of large size, looms especially large as a possible complication. The assumption of 200,000 operating hours for the plant (25-30 years of useful lifetime) implies the replacement of this part of the plant a few times. Service life information is not yet available; so the detailed design of the first vacuum wall must allow for 1) very small ($< 1\%$) elongation to fracture of the irradiated metal and 2) arrangements for relatively fast replacement of damaged or elderly sections.

Principal Systems

The Magnets

The various sets of coils have been given the names: toroidal field coils, vertical field coils, divertor coils, and control windings. These will be discussed in turn. Figure 2.1 shows a cross section of the machine on which the various coils are

741100

Fig. 2.1. Cross section of the nuclear island.

located. The toroidal field coils generate the most intense magnetic field and the largest magnetic flux. They are formed in a special shape to eliminate large bending moments and thereby minimize the amount of necessary structural material.^{1,2} The superconducting material chosen is Nb_3Sn . The field level is chosen to match the limit of the current state of the art of magnetic field design.

The vertical field coils are positioned to give the proper intensity and curvature of this field for equilibrium and stability of the plasma toroid current (14.6 MA) characteristic of a tokamak machine.

The divertor coils produce the separatrix for the divertor field and steer the scrapeoff layer through the divertor channel into the plasma collection chamber. Placing the null of the poloidal field on mid-plane between the plasma column and the major axis of symmetry is the prime cause of the large reactor size, since a minimum amount of neutron shielding is required between the plasma and the toroidal field coil. The position of this null is optimum from the point of view of providing proper field curvature for plasma magnetohydrodynamic stability.³

The control windings provide a source of flux to generate the voltage that provides the ohmic losses of the plasma during the hundred-minute burn time. This flux does not cross the plasma channel and does not contribute to the field that is shaped by the vertical and divertor field coils.

The Plasma

The quasi-steady state operation of the reactor will continue for a period of 97 minutes followed by a three-minute interruption for reignition; thus the duty factor is 97%. The low density (10^{-8} atmosphere) gas filling will be preionized by rf electric fields, and ionization will be completed by an ohmic heating current induced by the vertical field coils. Ohmic heating may not be sufficient to bring the plasma to the ignition point. Neutral beam injection into the ohmically-heated target plasma will be used to make up any energy deficit from the ohmic heating process. Once the D-T plasma ignition and the alpha self-heating begin to raise the temperature rapidly, fuel with an argon radiant added will be injected by an electrostatic accelerator, and the plasma density will rise to the operating level. When this quasi-steady state has been achieved, continued fuel injection in equilibrium with the diffusion losses will take place rather uniformly throughout the plasma, and the temperature and density profiles shown in Figs. 2.2 and 2.3 will be established.

744305

Fig. 2.2. Temperature profiles of reacting plasma.

744304

Fig. 2.3. Density profiles of reacting plasma.

The Divertor

The divertor protects the wall from bombardment by energetic ions. The exiting plasma is guided to an external chamber where its residual energy is recovered primarily by recombination line radiation and subsequently by delivery to a higher density gas dump in the exhaust pump manifolds. The divertor also protects the inner, high temperature reacting plasma from impurities by ionizing and therefore immobilizing any neutrals approaching from the outside and conducting them also to the external dump. Figure 2.4 shows the plasma temperature in different regions of the divertor. (See Chapter 6 for further discussion.) The outer boundary of the plasma core has a temperature of about 500 eV. The divertor, a region of high transport along field lines, provides a temperature drop to about 100 eV at the exit throat where a major drop in temperature (and corresponding rise in pressure) results. Outside the divertor and within the manifold, the higher density gas rapidly comes to thermal equilibrium with the wall and is pumped out by the vacuum system. The position of the ionization, temperature, and density front where the flow becomes supersonic is controlled by the throughput and composition of the plasma.

The Fuel System

The primary fuel loop simply returns the collected, compressed exhaust gas, after separation of the argon, helium, and protium contaminants, to the fuel injector where the liquified fuel is formed into pellets with appropriate argon addition. During a single pass through the machine, about 8.7% of the deuterium and tritium burns. The makeup deuterium and tritium comes from the blanket tritium generating cycle and the heavy water electrolyzer.

Tritium bred from lithium appears in the blanket in two places. The amount that diffuses into the helium coolant is oxidized by the oxygen content of the helium and is collected on an adsorbent. That which appears in the salt forms tritium fluoride which, after pumping from the salt, is collected in cold traps. Subsequent electrolysis regenerates tritium which is recycled to the primary fuel loop. The "ash" of a D-T fusion power plant consists principally of helium. In the reference design, 0.70 kg/day is produced as the fusion product, while another 1.12 kg/day is released as a byproduct of tritium breeding in the blanket. This should suffice to replace ordinary leakage of helium from the plant coolant.

744060

Fig. 2.4. Plasma temperature in different regions of the divertor.

The Blanket and Shield

The blanket converts the neutron energy to heat and breeds tritium. The shield reduces the neutron flux to levels tolerable by the superconducting coils and the cryogenic refrigeration and further reduces it to allow periodic maintenance on the divertor and pumping manifold structure. The 14.1 MeV neutrons, basic in the D-T reactor, yield a further 9 MeV of energy when absorbed in the blanket. Tritium is bred primarily from neutrons absorbed in Li-6 but also to some extent by multiplication in Li-7. The net tritium breeding ratio is 1.04, which yields a tritium doubling time of four months, substantially shorter than the maximum allowable. Actually, as the blanket ages, the doubling time increases as the isotopic abundance of Li-6 falls. The economy of when to recharge the blanket with lithium, the isotopic composition of the recharge, and the net overall breeding ratio to select is an intricate problem of optimization that depends on the overall state of the fusion power economy, i.e., the market for tritium.

The production rate of tritium is 0.548 kg/day compared with a burning rate of 0.527. With copper in the heat exchangers, only 2×10^{-7} kg will be lost per day. The total inventory in the plant will be 2.6 kg, two of which is fuel reserve representing 8 hours of feed or 4 days of burnup. The holdup time in the primary fuel loop is 1 hour, and in the recovery and pumping from the blanket, 2 hours. Thus a reserve is provided in case of technical difficulty with these systems.

Cooling and Steam Generation

The total 5305 MW of nuclear heat plus 241 dissipated in the helium from the circulating pumps is delivered to the steam in the steam generators. This 5546 MW of thermal power is converted to 2405 MW of electric power by the turbine generators, a cross-compound, two-shaft system with one stage of superheat and two stages of reheat. The gross generator output of 2405 MW provides 2030 for external sale and 375 for internal plant needs. Most of this (251 MW) goes to the helium circulators where 10 is lost in the motors and 241 adds to the thermal energy of the helium. The balance of the plant's needs (124 MW) represents a refrigeration load of 100, a cooling tower fan requirement for 15, and 9 for miscellaneous auxiliaries including ohmic heating, ignition, and injection equipment.

Although the gross electric plant efficiency is 45.3%, the net is only 38.3%, the penalty attributable primarily to the choice of helium as a coolant. The advantages of the helium coolant (chemical and physical compatibility) are worth this penalty in

efficiency, however. In fact, for fusion power, the economic motivation for increasing efficiency is somewhat different from that in the fossil or fission power industry. There are no cost savings in fuel since fuel costs are negligible. One must look for reductions in capital equipment (such as evaporative cooling towers) or in cooling water needs, etc., in order economically to justify complications that produce higher efficiency. An important attraction is the reduction of heat released to the environment ("thermal pollution"). Eventually, with extraordinarily advanced fusion power systems, one may be able to approach 60% net thermal efficiency. This would allow reduction of waste heat to about 50% of current levels for a given power demand.

The rejected heat will, however, be the principal environmental concern in the operation of a fusion power plant, more important than the inevitable but small leakage of tritium to the environment. By using copper as a barrier, proposed here for the first time, it appears that leakage through the steam generators (the only significant place) can be limited to about 2 curies per day, a small fraction of what would be acceptable.

System Peculiarities

The Cryogenic Superconducting Magnets

The technology of fusion reactors will include several features quite different from any in large scale power apparatus in use today. The superconducting magnet is one example. It will be refrigerated to a cryogenic environment, 4-10 K, and will store over 250 gigajoules of energy. It will be necessary to provide a safe method of dumping this energy externally in case of some malfunction. - A small external pond can absorb this energy with external resistors switched appropriately into the confining field circuit. The circuitry described elsewhere in this report allows such a dump without raising the temperature of the Dewars by an appreciable amount. A complete warmup of the Dewars followed by a recooling could require two weeks and cause an unreasonably long interruption in electric service.

Need for Reignition

The plasma ring forms a one-turn, closed electric circuit carrying 14.6 MA. The resistance of this closed conductor, although very low, is nevertheless finite, and the resultant energy dissipation implies a limited time during which the current can be maintained. The control windings produce a linearly changing flux through the ring sufficient to generate the ten millivolts needed to drive the current. The vertical field on the major axis of the machine produced by these coils changes from minus to plus

22 kilogauss. This takes place during a period of 97 minutes. In three minutes the plasma is extinguished and reignited. Details of this process may be found in Chapter 4. The blanket temperature drops by $< 50^{\circ}\text{C}$ during the 3-minute interruption to maintain a continuous output power of 2030 MW.

Radiation Damage

This severe problem may prove crucial to the future of fusion power. The neutron energy spectrum is considerably harder than that encountered in fission reactors and is therefore more effective in producing helium within the structure from endothermic (n,α) reactions. The embrittlement of metals due to helium accumulation along grain boundaries appears to be a problem for which no satisfactory solution has yet been demonstrated. Alloys have been developed that have dispersed precipitates. This alleviates the swelling produced by the neutron flux. It has been suggested that these alternate sinks for the helium may prevent, or at least slow, the accumulation on grain boundaries. However it is not expected to eliminate the problem, and thus one is faced with the task of designing a structure that will rapidly become very brittle (of the order of 1% elongation to fracture). Thus temperature excursions and other stress-inducing phenomena become a major design consideration.

The Vacuum System

Although ultra high vacuum techniques have become a recognized industry in the last twenty years, the volume required for this reactor model ($\sim 3000\text{ m}^3$) is unprecedented. Nevertheless, except for the embrittlement problem mentioned above, there seem to be no extraordinarily difficult problems to meet in the vacuum system.

References

¹L. V. Leites, A Coreless Toroidal Reactor for Power Systems, Elektrichestvo 11, 76 (1960); V. D. Shafranov, Optimum Shape of a Toroidal Solenoid, Soviet Phys.-Tech. Phys. 17, 1433 (1973).

²J. File, R. G. Mills, G. V. Sheffield, Large Superconducting Magnet Designs for Fusion Reactors, IEEE Transactions on Nuclear Science NS-18 (1971) pp. 277-282.

³G. Leval, H. Luc, E. K. Maschke, C. Mercier, R. Pellat, Equilibrium Stability and Diffusion of a Toroidal Plasma of Non-Circular Cross Section, Plasma Physics and Controlled Nuclear Fusion Research (International Atomic Energy Agency, Vienna, 1971) Vol. 2, pp. 507-517.

Chapter 3
Advantages of Fusion Power
P. Bonanos

The need for alternate fuel sources, convertible to electrical and thermal energy, has recently received widespread recognition. Forecasts of reserves of fossil fuels and rich-ore fissile materials all reach the same conclusion, differing only in the final numbers; these resources are finite, and extended use in the current manner will continue for no more than several generations or at most, a few centuries. The extraction of these fuels and their modification into useful forms are increasingly difficult and often baneful.

Abundant low cost energy assures the continued satisfaction of human material needs. The supply of food, water, shelter and clothing will, in the long term, depend more strongly on available energy production.

The ultimate source of future needs lies in the nuclear burning of uranium, thorium, deuterium and lithium in breeder and fusion reactors. The breeder reactor is clearly feasible technically, and a positive demonstration of this concept is expected in approximately 15 years. The alternative of fusion power still requires a series of large-scale scientific experiments to prove feasibility in parallel with, and followed by, an intensive engineering effort, drawing much practical data from breeder development, to construct and operate a power reactor. Fusion and the breeder offer the only solution to long-term energy supply, and while the following discussion cites the manifold benefits of fusion power, many of the specific points are not exclusive but common advantages held by both.

Fuels

The major advantage of fusion power is the essentially inexhaustible and cheap fuel supply. The total mass of fuel consumed in a 2000 MWe plant is small, less than 2 kg/day, and fuel costs are estimated to be less than 0.02 mill/kWh. Deuterium gas is presently extracted from water in commercial quantities at reasonable cost. For D-T reactors the tritium is not available naturally and must be bred within the plant using primarily the reactions $\text{Li-6 } (n, \alpha) \text{ T}$ and $\text{Li-7 } (n, n, \alpha) \text{ T}$. Lithium is naturally abundant. Total reserves are not accurately known because easily available surface sources such as western United States lake brines represent many centuries' use at current consumption rates. The incentive for further exploration does not yet exist. If

this reserve alone were utilized in fusion plants for all United States energy uses, not only electrical, it would correspond to a 9000-year supply at 1970 consumption rates.¹ If oceanic reserves were ever needed and additional processing resulted in an order of magnitude increase in cost it would still be a cheap fuel.

The low mass and volume of fuel needed for a large electric plant offers greater freedom in plant placement and layout. Plant sites need not be tied to port facilities or railroads with capacity for continuous delivery of massive fuel quantities. Plant yard area for fuel storage and handling is essentially eliminated. A year's supply of deuterium could occupy less than 100 square feet of floor space and might be stored conveniently as liquid in a 1000-liter Dewar, electrolyzed from a heavy water tank or as a gas in high pressure cylinders.

The fuel is not only plentiful but indigenous. Thus, the concern over supply and control of primary fuels is removed from the objective of reliable power production. The international politics and finance of oil and gas supply and delivery have reduced importance. A further result is not only a national independence with respect to fuels but an ability, through design, for each utility to be practically self sustaining for long periods. If the initial charge of lithium in the reactor model proposed here were enriched to 12% Li-6 (7.5% natural), it might be used for 20 years with little change in breeding characteristics. Again, external influences, in this case labor strikes at coal mines, oil refineries and in transportation, do not significantly affect existing plant operations. The self-reliance is not total however, and is limited to fuels.

A potential advantage held in common by all nuclear systems is that fossil fuels would not be burned for electrical needs. These convenient compounds of hydrogen and carbon would then be available for other purposes such as the production of portable fuels, petrochemicals, plastics and fibers thus extending the period during which they, too, will be completely exhausted. Ultimate replacement of these commodities may be achieved using more abundant resources and energy.

Plant and System Characteristics

Reactor designs of the tokamak type indicate irreducibly large plant sizes of 500 MWe or more and are thus suitable only as base-load plants. They will require substantial water sources for once-through heat rejection. Atmospheric rejection through dry towers with their consequent higher rejection temperatures, turbine exhaust pressures and increased capital costs may remain an economically tolerable alternative when overall system costs are examined. The point here is that a fusion reactor could, in concept, be

built in a desert or another normally inappropriate site with a small quantity of makeup water piped to the plant. The limiting level of energy production on a global scale will not be limited by fuels but rather by heat rejection, and this limit is very large.

The reacting plasma volume is a tenuous heat source. Improper control of magnet coils, fuel feed rates and composition, vacuum system components or cryogenic refrigeration machinery may stop the reaction. The supportive auxiliary equipment is, collectively, a machinery complex of high technical order requiring well-trained operating and maintenance personnel for continuous reliable operation.

Safety and Environmental Characteristics

The main dangers inherent in fusion power plants are derived from (1) the use of tritium and (2) the production of high energy neutrons. Tritium is biologically harmful through emission of a low energy beta particle. It measurably permeates metal walls at high temperatures, and containment will require careful detailed design and sensitive detection equipment.

The vacuum wall and blanket structure will activate during operation and require some form of long-term disposal at the end of its functional life. The internal plant structure will be inaccessible for contact maintenance, and complex remote handling and repair facilities are essential. There are also hazards intrinsic in the potential energy associated with high pressure, high temperature working fluids and high magnetic fields.

Fusion power systems offer inherent safety characteristics as well. Sustained thermal "runaways" are clearly impossible since the reacting medium is fueled for a few seconds of operation only. After initial startup, no further radioactive fuel transport is required, nor is external fuel reprocessing necessary. The added safety may permit plant siting close to large industrial users and population centers. This reduces energy losses, land use and capital costs for transmission and offers the potential for sale of waste heat for direct uses such as industrial and district heating.

Thermal efficiencies in fusion plants will match or exceed those of any competing thermal system. Blanket design is flexible, and upper temperature limits are set by the same metallurgical constraints common to all heat cycles. There is a possibility also of the development of some direct conversion system with still further efficiency improvement. Waste heat, if not usable, is therefore reduced to a minimum.

In conclusion fusion power is an energy generation system freed from fuel restraints, of high conversion efficiency, and offering great flexibility in plant design and siting.

Reference

¹J. P. Holdren, Adequacy of Lithium Supplies as a Fusion Energy Source, unpublished.

Chapter 4
Plasma Characteristics and Control
J. L. Johnson* and S. L. Gralnick

Introduction

Here we give a brief statement of the problems that guide our choice of plasma properties, discuss the specific parameters chosen, and mention a few options available for plasma control.

For a steady-state plasma, the energy entering or produced by nuclear reactions in any volume element must just equal that lost by diffusion, convection, heat conduction, and radiation. Unless this condition is satisfied, the plasma will cool and the reaction will go out, or the temperature of the plasma will increase. This generalization of the Lawson criterion¹ imposes serious restrictions on the choice of the plasma parameters. Two specific problems associated with this are a need for an enhancement of plasma diffusion over that predicted by classical models and for control of the plasma temperature. The first arises from the fact that if a significant fraction of the fuel reacts before it has diffused through the system and the resulting helium is well confined, a major part of the magnetic pressure would be used to contain the ash. Similarly, left on their own, the electrons would tend to get too hot and exert too much pressure on the confining magnetic field. Thus, a given externally imposed field could not contain as many reacting ions, and the reactor economics would suffer. To prevent these difficulties, we postulate that something enhances the diffusion mechanism to spoil the containment (an idea guaranteed to upset any plasma theorist) and introduce some argon impurity (a technique to disturb the experimentalists as well) to enhance the bremsstrahlung loss.

Plasma Properties

Transport

In a magnetically confined plasma, the charged particles gyrate around magnetic field lines executing orbits of radius $\rho = mv_{\perp} c/eB$. Collisions with other particles cause their guiding centers to move to new magnetic field lines. This random walk provides a net motion in the opposite direction from the density gradient² and thus provides diffusion. To the extent that one can treat the electrons or ions as a fluid and introduce an average velocity to represent them and replace the many small-angle

*On loan from Westinghouse Research Laboratories.

collisions that occur with a Coulomb interaction by some average collision frequency, the diffusion is well described by a random walk

$$D \approx \rho_e^2 v \sim (B^2 T^{1/2})^{-1} \quad (4.1)$$

Geometric effects increase this by a factor $(1 + 2q^2)$ with $q \approx a B_t / R B_p$ the safety factor. This "classical" diffusion expression holds if the collision frequency, ν , is greater than v_{th}/qR , the frequency with which particles sample all parts of the magnetic surfaces.

If the collision frequency lies in the range $(a/R)^{3/2} v_{th}/qR < \nu < v_{th}/qR$, the particles trapped in the toroidal magnetic field mirrors undergo many bounces between collisions and thus do not contribute excessively to diffusion. The guiding centers of the untrapped electrons do not have time to complete their banana orbits between collisions, however, so the effective step length is increased. To further complicate the problem, the effective collision frequency depends on the shape of the distribution function for Coulomb collisions and is thus enhanced by the geometry. Then³

$$D \sim (q v_{th}/R) \rho_e^2 \quad (4.2)$$

Note that, in this regime, the coefficient is independent of the collision frequency. For even lower collision frequencies, $\nu < (a/R)^{3/2} v_{th}/qR$ the trapped particles provide the main contribution to diffusion. Then

$$D \sim (R/a)^{3/2} q^2 \rho_e^2 \nu, \quad (4.3)$$

a factor $(R/a)^{3/2}$ larger than that predicted by the "classical" Pfirsch-Schluter formula. The relation of the diffusion coefficient to the collision frequency is shown in Fig. 4.1.

It is a reasonable conjecture that plasma turbulence could further amplify the diffusion, primarily by further increasing the frequency or size of the step in a random walk. Quasilinear calculations associated with trapped particle instabilities indicate that this is indeed the case as is shown in Fig. 4.1. It is useful to postulate that such turbulence could persist for all values of the collision frequency⁴ so that

$$D \sim (\rho_e B_p / B)^2 \nu \quad (4.4)$$

744320

Fig. 4.1 Diffusion and electron thermal conductivity. 1) Due to pair collisions; 2) due to trapped-ion instability; 3) due to trapped-electron instability; 4) thermal conductivity due to trapped-electron instability. (Courtesy of Nuclear Fusion)

the coefficient being essentially the same as that of the banana regime where the untrapped particles were most important. This speculation fits well with most experiments and is usually used for scalings.

Since diffusion represents an expansion of the plasma through the magnetic field, it is clear that it must be associated with a dissipative process. Indeed, the concepts of resistivity and diffusion are closely related, and the coefficient of resistivity is fixed when the diffusion is prescribed. Obviously, since thermal conductivity is associated with the energy carried by particles (principally ions) as they move along the field lines or across them because of collisions, this prescription also determines the thermal conductivity.

As noted earlier, the diffusion coefficient must be specified within fairly narrow limits or the plasma cannot be confined under steady-state conditions. For this design, we therefore assume an optimum value from this point of view and assume that the physics can be adjusted to provide it. Since the desired number is not far from that associated with pseudo-classical diffusion,⁴ this seems reasonable.

Radiation

In a fully ionized plasma, the two main kinds of radiation are bremsstrahlung and synchrotron. Plasmas with reactor parameters are transparent to bremsstrahlung and some of the synchrotron radiation. Since the vacuum wall should be a good reflector for the synchrotron radiation generated by the electron acceleration associated with gyration around the field lines, we assume a truncated black body spectrum and calculate the wall absorption. We rely on the bremsstrahlung to keep the electrons from getting too hot. Indeed, we inject a 4.8 percent argon impurity to increase this loss. Then the ion heating through direct interaction with the reaction alpha particles keeps the ions hotter than the electrons.

Plasma Equilibrium

We use a one-dimensional cylindrical model to determine a steady state of the plasma, solving transport equations for conservation of number of particles, momentum, and energy for several species - electrons, thermal ions (deuterium and tritium), and energetic ions (alpha particles). We assume that injection of cold particles can be accomplished so that the electron density is parabolic,

$$n = n_0 \{1 - \alpha(r/a)^2\} \quad (4.5)$$

and, using our assumed values for diffusion, thermal conductivity, resistivity, radiation, thermal relaxation between species, etc., iterate to a stationary solution. Then, using tokamak equilibrium theory, we calculate the toroidal shift of the centers of the magnetic surfaces from the magnetic axis^{5,6} to see that neglect of toroidal curvature is reasonable. For the parameters of this study only the outermost surfaces are shifted significantly. The constant magnetic flux surfaces, based on this calculation, are approximated in Fig. 4.2 as a nested set of toroids. To compute the separatrix surface, the complex currents in the discharge were approximated by a single filament of current, and use was made of the equilibrium theory to specify the required externally applied vertical field in the region of the magnetic axis. The magnetic surfaces shown near the magnetic axis are circles shifted in accordance with the calculation, but the outer surfaces have been distorted to conform to the shape of the separatrix.

Plasma Stability

Since the plasma must be confined in a steady state, we must be concerned with instabilities. The most dangerous mode, the $m = 1$ kink, sets in if $q = 1$ anywhere in the plasma. Our adopted current distribution prevents this mode. On an idealized model all higher- m kink modes are stable;⁷ resistivity allows field line breaking to enable tearing modes to grow. Our model should be marginally stable to such an $m = 2$ mode and stable to higher- m ones.

Flute or interchange modes do not cause concern because the toroidal shift of the magnetic surfaces provides a minimum-average-field configuration by concentrating the field lines in regions of favorable curvature. Ballooning modes, in which the perturbations are concentrated in regions of unfavorable curvature, will occur when the field energy necessary to bend the field, $|\vec{B} \cdot \vec{\nabla} \xi|^2 \approx B^2 \xi^2 / R^2$ is exceeded by the curvature term, $2(\vec{\xi} \cdot \vec{\nabla} p)(\vec{\xi} \cdot \vec{\kappa}) \sim 2\beta B^2 \xi^2 / aR$. Thus, they should cause no problem if $\beta < a/2R$ which we barely satisfy.

Because of the high operating temperature, the current-driven and the ordinary collisional drift modes are probably stable. For our parameters, collisional trapped ion modes should be unstable.⁸ These should be responsible for the generation of the turbulence that is necessary to provide the desired diffusion coefficient.

Design Plasma Profile

The results of a one-dimensional numerical calculation of the plasma parameters using the "Profiles" code, described in Appendix B, provide the reactor plasma

744316

Fig. 4.2. Magnetic flux surfaces in the discharge volume.

design profile. The basis of this calculation is the satisfaction of the equilibrium condition; that is, at each point in the reactor cross section the rate of deposition of energy by the thermalization of the fusion produced alpha particles is equal to the rate of loss of energy from all causes. Numerical experimentation with this code leads one to the conclusion that the equilibrium reached depends on many parameters. The choice of transport coefficients, the fuel mixture, the effective Z of the plasma, the density source function, and the boundary conditions imposed at the separatrix all influence the final equilibrium profile to some extent. We have not attempted to optimize the equilibrium with respect to these parameters in this study; this is an area of research that is still to be explored. Rather, after looking at many equilibria we have relied on intuition based on experience, and a necessity to satisfy conditions of compatibility with the reactor system as a whole to guide us in choosing a "design" plasma profile.

We have assumed a constant diffusion coefficient and a density source function that is constant in r . Under these conditions the solution of the one dimensional diffusion equation, in symmetric cylindrical coordinates, subject to the boundary condition;

$$n(0) / n(a) = \gamma, \quad (4-6)$$

is

$$n = \left(\frac{a^2 S}{(1-\gamma)4d} \right) \left(1 - (1-\gamma)\left(\frac{r}{a}\right)^2 \right) \quad (4-7)$$

which we write as

$$n = n(0)(1 - \alpha(r/a)^2). \quad (4-8)$$

Initially we presumed that the value of γ would be close to zero and performed the analysis under these conditions. This condition corresponds to having the divertor operating at 100% efficiency (a particle crossing the separatrix disappears instantly). This proved to be an unrealistic assumption about the behavior of the divertor (see Chapter 6), and consequently it became necessary to use larger values of γ . For fixed transport properties and source strength this variation strongly affects the structure of the temperature profile. A small value of γ implies a steep density gradient and rapid convective energy transfer. As a result, differences in temperature are smoothed and a "flattish" temperature profile results. If a larger γ value is assumed, the density profile is filled in, and the convective mechanism of energy transport is much weaker. Under these circumstances strong temperature gradients occur, and the energy is carried by conduction.

The second boundary condition specified at the separatrix is the ion temperature. Once again the behavior of the plasma in the scrapeoff region and divertor proved our initial conception to be false. We began by assuming a high temperature at the boundary. Strong conduction parallel to the field then implied that the plasma temperature near the walls in the divertor was excessively high. The divertor required that a lower boundary temperature be sought. We found that equilibria having low boundary temperatures could be generated and chose one such equilibrium for our design solution. It is interesting to note that for boundary temperatures below 1 keV the region over which the effect of the boundary is felt is very narrow: a very steep temperature gradient develops in a "boundary layer" at the separatrix. The effects of this steep gradient and the detailed behavior of the plasma in this boundary layer are not well understood and will require further study.

The equilibrium temperature is strongly influenced by the effective Z of the plasma. We have represented this effect by the addition of a small fraction (4.8%) of argon atoms to the fuel mixture. The desirability of having $Z \geq 1$ comes from the requirement that we operate at an equilibrium point. Fig. 4.3 shows that for a given Z two equilibria occur, one at low temperature and one at high temperature. The condition for such an equilibria to be stable to small perturbations in T is that

$$\frac{d}{dT}(dT/dt) \geq 0; \quad (4-9)$$

consequently, only the high temperature intersection can be used. It has been suggested that one might operate at an unstable equilibrium point utilizing some unspecified feedback control. As the stability of these equilibria have not been studied in detail and a physical mechanism for stabilizing them is not yet known, we do not consider this possibility. In a pressure limited system such as the tokamak, high temperature implies low density and low power density (due to the n^2 effect in P_α) - this is uneconomical. By increasing Z we are able to lower the operating temperature (see Fig. 4.3) and make better use of the magnetic field that is obtained at great cost.

The same effect can be obtained by changing the ratio of deuterium to tritium in the fuel. This, however, also changes the neutron production rate. Consequently, the choice of an optimum fuel mixture is a question of some subtlety. For the purpose of this report we have elected to use a 50:50 fuel mix and to vary the argon content as required. This is not necessarily the optimum combination.

744256

Fig. 4.3. Solutions of the equilibrium equation are given by the intersection points. The lower temperature solution is unstable, the high temperature one is stable. Increased Z lowers the temperature of the high temperature solution.

744305

Fig. 4.4. Radial temperature profile of the plasma constituents.

In exploring various equilibrium solutions the first requirement that had to be met was a fixed total thermal power production. Next, it was necessary to set the boundary conditions required for compatibility with successful divertor operation. Finally, and most important, it was necessary to generate a plasma profile that did not present a gross departure from realistic plasma physics. Specifically, we were concerned with the average value of β_0 and the maximum values of β and $\Delta r/r$ (the toroidally induced shift of the flux surfaces).

Figures 4.4 and 4.5 give the temperature and density profiles of the several species present in the plasma. The resulting pressure profile in units of the magnetic energy ($\approx i.e., \beta$) is given in Fig. 4.6. The peak value, which occurs on the axis, is 13% and the average value is 4%. The average value of β_0 is 1.83; as this value is equal to the square root of the aspect ratio to within 2% we believe it to be credible.

Our calculations yield large values of $\Delta r/r$. The need for investigations based on toroidal models in which the M.H.D. equilibrium problem is explored for pressure profiles characteristic of reactors of economic size is brought out by this result - the model used here is cylindrical and considers the transport equations only. Finally we have calculated the value of the safety factor, q , (Fig. 4.7) and are confident that gross stability is obtainable with this profile.

Equilibrium Condition

The calculation that is performed by the profiles code is based on thermal equilibrium. This condition has also been called the ignition condition in the literature. This is somewhat of a misnomer, and we have chosen to use the more explanatory term. Derivations of this condition are usually based on a point model that may also be considered to be a global statement about the entire plasma volume. In this analysis the equilibrium condition is met locally as well as globally. This has produced several results that are not apparent in a global analysis.

A substantial fraction of the volume of the plasma is not in equilibrium in the classical sense. (See Fig. 4.8.) The alpha energy produced in these shells ($r > 215$ cm) is not sufficient to balance the radiation losses. Maintenance of the equilibrium in these shells depends on the transport of energy from the interior by conduction and diffusive convection. A question of some interest then is "what is the most efficient utilization of the plasma volume?" A large volume of plasma might be kept in equilibrium at peak power production ($T \approx 14$ keV, the peak of the $\langle \sigma v \rangle / T^2$ curve) by relying on this transport phenomenon. Another aspect of this problem is the existence of

744304

Fig. 4.5 Density profiles of reacting plasma.

744319

Fig. 4.6. Radial profile of β based on the average toroidal magnetic field.

744318

Fig. 4.7. Radial profile of the safety factor, $q = a B_r / RB_p$. $q = 2$ rational surface occurs within plasma.

744317

Fig. 4.8. Radial distribution of α power delivered to plasma and bremsstrahlung radiated.

oscillatory solutions in which regions of the plasma profile have alternately increasing and decreasing temperature as a function of time. The nature of this oscillation has not been studied nor has the behavior of the design equilibrium under various perturbations. The stability of any reactor operating state and its control is obviously of some importance.

Other Possibilities

One feature of this model that is particularly disturbing is the necessity to add argon to increase the bremsstrahlung radiation and thus prevent the electrons from getting too hot. Other possible solutions include absorption of some of the synchrotron radiation through controllable apertures and variation of the fuel composition. The occurrence of more rapid transport losses would also alter the scope and nature of this problem. It has been shown⁹ that if trapped-ion mode scaling were assumed, in which n_i decreases as $T_e^{7/2}$, the $Z = 1$ stable equilibrium point would occur at 18 keV. This would eliminate the necessity for the argon additive.

An alternative solution that should be pursued in a future study would be to interrupt the duty cycle at frequent intervals to avoid both this problem and that associated with too long confinement of the alphas. This would have the added advantage that one could hope to utilize the full neoclassical diffusion (the trapped particle instabilities should be damped for somewhat more dense plasmas). It should provide alternatives to some of the divertor and injection problems discussed in Chapters 6 and 7.

References

- ¹J. D. Lawson, Some Criteria for a Power Producing Thermonuclear Reactor, Proc. Roy. Soc. (London) B 70, 6 (1957).
- ²L. Spitzer, Jr., Physics of Fully Ionized Gases, 2nd edition pp. 40-48. (Interscience Publishers, Inc., New York, 1967) pp. 40-48.
- ³B. B. Kadomtsev and O. P. Pogutse, Trapped Particles in Toroidal Magnetic Systems, Nuclear Fusion 11, 67 (1971).
- ⁴S. Yoshikawa, Possibility that Anomalous Plasma Diffusion Depends on Geometric Factor, Phys. Rev. Letters 25, 353 (1970).
- ⁵V. D. Shafranov, Plasma Equilibrium in a Magnetic Field, Reviews of Plasma Physics, ed. M. A. Leontovich (Consultants Bureau, New York, 1966). Vol. 2, pp. 103-151.
- ⁶J. M. Greene, J. L. Johnson, and K. E. Weimer, Tokamak Equilibrium, Phys. Fluids 14, 671 (1971).
- ⁷E. A. Frieman, J. M. Greene, J. L. Johnson, and K. E. Weimer, Toroidal Effects on Magnetohydrodynamic Modes in Tokamaks, Physics of Fluids 16, 1108 (1973).
- ⁸M. N. Rosenbluth, D. W. Ross, and D. P. Kostomarov, Nuclear Fusion 12, 3 (1972).
- ⁹S. L. Gralnick and F. H. Tenney, Optimum Fuel Composition for Equilibrium CTR Operations, Princeton Plasma Physics Laboratory Report MATT-1046 (1974).

Chapter 5
The Operating Cycle
S. L. Gralnick

Introduction

This chapter treats the operating cycle of the Princeton Reference Design Fusion Reactor. We have chosen a design based on a long pulse tokamak - the pulse length is much longer than the residence time of the particles in the system. Consequently, fuel injection is provided, (see Chapter 7), and a divertor is used to remove the reaction products (alpha particles) and unburnt fuel (see Chapter 6). In this manner, the plasma parameters are maintained constant during the extended pulse. The plasma is not a perfect conductor - it is resistive, and the line integral of the electric field around the torus does not vanish. A continuous change of magnetic flux must be provided in the reactor core to preserve the equilibrium configuration. The maximum pulse length is determined by this process. As the flux increases in the reactor core, the magnetic field strength at the superconducting coils increases until the quenching field is approached. At this point, the pulse must be terminated. A particular reactor design then has a volt-sec rating of available flux which determines the maximum burning pulse length and the frequency of shutdown and reignition procedures.

The operating cycle of this design employs a 100 minute burning pulse and a current shutdown and reignition procedure that occupies less than 3 minutes. This provides a duty factor, defined as the ratio of the time during which power is produced to the total time of 97%. During the "downtime" the electrical load on the station is satisfied by withdrawing thermal energy from the molten salt (flibe) in the reactor blanket and storage areas, and consequently electrical power is delivered without interruption or load shedding. In this sense this design is a steady state power reactor.

The plasma parameters during the extended pulse are determined by the PROFILES code (see Appendix B) which is based on a one-dimensional, cylindrical, steady state model. Spatial averages of the plasma density and temperature are the stationary points at the beginning and end of the current interruption period that are treated here by a zero dimensional, time dependent analysis. The time history of the average plasma parameters, (density and temperature), and the discharge current is found.

The procedures that have been chosen to shut down and reignite the discharge are based to a considerable extent on a set of assumptions that we have made regarding

the availability of required technologies. The analysis then in some areas is particular to this design. However, the several phases of the operating cycle that will be described are characteristic of all tokamak type fusion reactor concepts.

The organization of this chapter is parallel to the evolution of the discharge from ignition, through the burning pulse and finally to the shutdown procedures that close the cycle.

Ignition

There is to be found extant today an extensive literature on the ignition problem.¹⁻⁵ Our analysis differs from this work in its more detailed accounting of the physical constraints imposed by ancillary reactor components and to a somewhat lesser extent by the costs associated with different approaches. The latter point is well illustrated in Fig. 5.1 which gives the extrapolated cost of a motor-generator set designed to be used as an ohmic heating power supply during ignition. At power levels in the one to five hundred megawatt range the cost of this power supply is likely to be under $\$15 \times 10^6$. More importantly it represents a small fraction, less than five percent, of the total design cost. Consideration of this level of cost in the face of the yet unknown plasma physics would be fatuous. However, what happens if the desired power is increased by a factor of ten? Cost is now a real consideration; and, in fact, cost should in this case influence the direction of research so that a realizable system is conceived and developed.

Several terms used in our discussion must be defined here for clarity. In particular, we wish to differentiate between what we will call n-squared ignition, which is achieved when the thermalized portion of the alpha power is equal to the sum of all of the losses that are proportional to the square of the density, and true ignition, which is achieved when the thermalized alpha power is equal to the sum of all of the losses. Unless very optimistic assumptions are made about energy confinement times in the tokamak, it is not possible to achieve true ignition by ohmic heating alone. However, by operating at reduced densities, we find that in a sufficiently large device ohmic heating is sufficient to reach the n-squared ignition point. Once the n-squared ignition temperature is reached, fuel injection can be used to achieve both higher density and temperature until finally true ignition is obtained.

During the ohmic heating phase of the ignition procedure, the inductive energy of the discharge is transferred from an auxiliary storage device, (the flywheel of a motor generator set), to the plasma. This is accomplished by applying a voltage to the

744188

Fig. 5.1 Costs of motor generator set power supplies to be used during current initiation. [These costs are extrapolated from costs for equipment available today (1973 dollars)].

appropriate vertical field coils, and the resulting change of flux in the core of the machine induces the discharge current in the plasma. Simultaneously, the vertical field is increased to provide the necessary equilibrium force. The large discharge current, 14.6 MA, implies that the inductively stored energy of the plasma is

$$U_m = 2.5 \times 10^3 \text{ MJoule.} \quad (5.1)$$

As we have assumed that the power available for ignition is not to exceed 500 MW, and that the rate of change of magnetic field at the superconductor is not to exceed 10 kG/sec., a minimum current buildup time of 10 sec will be necessary (assuming 50% efficiency of inductive coupling). The ignition procedure presented here is intended to take advantage of this extended current initiation period.

The total time required to achieve true ignition will depend on the neutral injection power available as shown in Fig. 5.2. The minimum power is the cold fuel requirement, the power necessary to inject cold fuel pellets, while increased power gives a shorter total ignition period. We see from the figure that an ignition system based solely on ohmic heating (i.e., cold injection) requires an excessive amount of time and that shorter time periods for achieving ignition are possible at the expense of a larger injection power requirement.

The Ignition Model

Our analysis of the evolution of the plasma parameters is zero dimensional and time dependent between stationary equilibrium points given by the operating state and an initial state. We consider: the time development of the discharge current, governed by

$$-L \frac{dI_p}{dt} = I_p R + \dot{\phi}_{\text{ext}} \quad (5.2)$$

where I_p is the total discharge current in amperes. R is the plasma resistance once around the torus in ohms. L is the self inductance of the discharge in henries and $\dot{\phi}$ is the rate of change in the magnetic flux linking the discharge due to external sources in volts.

The plasma density varies according to:

$$\frac{dN}{dt} = S - \frac{N}{\tau_p} \quad (5.3)$$

734469

Fig. 5.2 Duration of ignition period (time after current is established) vs available injection power at 5 keV injection energy.

where N is the total number of deuterium and tritium ions in the system. S is the feed rate in ions per second and τ_p is the average ion confinement time in seconds. The plasma temperature varies according to:

$$nk \frac{dT}{dt} = \left(I_p^2 R + p(1-p) n^2 \langle \sigma v \rangle E_\alpha \right) - \left[S \Delta U + nT \left(\frac{\tau_{ic} + \tau_{ec}}{\tau_{ic} \tau_{ec}} \right) \right] - \left(1.69 \times 10^{-32} f_b(Z) n^2 T^{1/2} + 1.64 \times 10^{-20} \frac{\alpha m^3 B^3 T}{a} + 3 \times 10^{-26} n^2 f_L(Z) \Theta(T) \right) \quad (5.4)$$

where n is the plasma density in particles per cm^3 , p is the tritium fraction, $\langle \sigma v \rangle$ is the Maxwellian average fusion reactivity integral in cm^3/sec , B is the magnetic field in gauss, a is the minor radius, m is the mode number of the highest frequency harmonic contributing to the synchrotron radiation, α is an effective wall absorptivity and τ_{ic} and τ_{ec} are the ion and electron conduction energy confinement times. The functions $f_b(Z)$ and $f_L(Z)$ enhance the bremsstrahlung and line radiation losses due to higher Z ions;

$$f_b(Z) = \sum_j Z_j \frac{n_j}{n} \sum_j Z_j^2 \frac{n_j}{n} \quad (5.5)$$

$$f_L(Z) = \sum_j Z_j \frac{n_j}{n} \sum_{j \neq 1} \frac{n_j}{n} \quad (5.6)$$

the summations being over ionic species. The function $\Theta(T)$ gives the temperature dependence of the line radiation.⁶ We consider only the impurity lines and assume

$$\begin{aligned} \Theta &= 1 & T &\leq 100 \text{ eV} \\ \Theta &= 0 & T &> 100 \text{ eV} \end{aligned} \quad (5.7)$$

ΔU is the difference between the thermal plasma energy and the injected energy per particle. Eqs. 5.2, 5.3, and 5.4 are solved numerically from an initial point of given density, temperature, and current.

n-Squared Ignition

The maximum temperature attainable by ohmic heating during the current buildup is a function of the initial filling pressure and the equivalent impurity content.

Figure 5.3 demonstrates the result for several equivalent oxygen fractions; at sufficiently low density the maximum temperature is in excess of the n -squared ignition temperature and is determined by a balance of ohmic heating power and synchrotron radiation power. (This is also known as synchrotron cutoff).⁷ At slightly higher densities n -squared ignition can not be reached. The ohmic power in this case is consumed by the n -squared losses. Finally, an extreme example of this occurs when insufficient power is available to overcome the impurity radiation. Optimum use then is made of the current buildup period by starting at low filling pressures ($P_g \leq 2 \times 10^{-5}$ torr) which enables us to reach n -squared ignition.

Low initial filling pressures are particularly necessary in the early phases of the discharge. A large minor plasma radius is necessary, and limitations on our ability to drive the primary windings will not allow for the generation of ϕ_{ext} greatly in excess of what is available with current technology. As a result the current density that can be passed through the cross section is limited - in the early stages of the discharge it is extremely limited. (At full current the reactor current density is just slightly greater than the uncompressed ATC plasma and much less than that in the compressed state).⁸ Line radiation, especially that from impurity ions, which will not be fully stripped until high temperatures are reached, is then the limiting consideration if the density is allowed to increase (see Fig. 5.3). In treating the line radiation for ignition design we have considered the contribution of a one percent oxygen equivalent impurity. We also assume that the divertor is effective in eliminating all higher Z impurities originating at the walls and in preventing recirculation. The oxygen impurity is used as an equivalent for all of the several impurity species present. It is counted in the bremsstrahlung calculation as fully stripped (an overestimate at low temperatures), and the strength of the line radiation is maintained until a temperature of 100 eV is reached. The nature of the effect is clear - low density is required to overcome the "lamp effect". The exact numerical evaluation, however, is not of high accuracy.

The current buildup phase is summarized in Figs. 5.4, 5.5, and 5.6, which give the plasma parameters, the power balance and the driving emf. The initial conditions are $N = 10^{21}$ particles ($n = 4.6 \times 10^{11}$), $T = 10$ eV and $\dot{I} = 0$. The applied emf is initially high, 200 volts, in order to break out of the line radiation regime. (This requires 66.8 msec. Subsequently the driving voltage is reduced, and the full current is built up in 13.25 sec. During this phase any increase in the plasma density will result in a reduction in the temperature ($T < n^2$ - ignition). Consequently, no fuel supply is provided,

744368

Fig. 5.3 Maximum temperature that can be achieved by Ohmic heating as a function of the initial filling density. At densities greater than $\approx 2 \times 10^{12}$ line radiation produced by impurities limits the maximum temperature attainable.

744369

Fig. 5.4 Plasma temperature and density history during the 13.25 sec current initiation period (n^2 -ignition reached at 13.25 sec). 1% oxygen impurity is assumed and transport losses 10 times as rapid as pseudoclassical are used.

744335

Fig. 5.5 Plasma power balance during n^2 -ignition.

744370

Fig. 5.6 Externally applied EMF
driving plasma during current initiation

and the reactor will "bleed", thinning the density to $n = 4.5 \times 10^{11}$. It has been assumed that the particles are lost at a rate ten times as fast as pseudoclassical but not faster than that given by the profiles code diffusion coefficient, $D = 3400 \text{ cm}^2/\text{sec}$. The effective diffusion coefficient is shown in Fig. 5.7.

Density Buildup and True Ignition

Once the n-squared ignition point has been reached, it is possible to inject fuel to increase the density to the higher values necessary for reactor operation. The fuel feed rate cannot be arbitrarily selected as there is a loss term in Eq. 5.4 which is proportionate to the feed rate, S . Too large a feed rate will result in a quenching action. This effect is particularly important at the temperatures characteristic of n-squared ignition, four to five keV; $\langle \sigma v \rangle$ changes rapidly with temperature here, and small decreases in T will result in the loss of n-squared ignition.

The implication of the reduced value of S is that the fueling phase will occupy a substantial length of time. This time period may be reduced by injecting the fuel at energies equal to or larger than the thermal energy of the plasma. Fig. 5.8 gives the time needed to reach the operating density as a function of the injection energy of the incoming fuel.

While the reactor density is being increased, the temperature experiences a plateau during which essentially all of the excess input energy is absorbed in the incoming fuel followed by a fairly rapid rise as the alpha source is increased. The duration of the plateau and the temperature achieved when the operating density is reached depend on the injection energy as can be seen in Fig. 5.9. If the operating density is reached prior to a sufficiently high temperature, as is the case with cold fuel injection, the fuel feed is reduced to its equilibrium value ($N = \text{constant}$) and the temperature is allowed to rise to its equilibrium value. When the equilibrium value of T is reached the fuel composition is adjusted (argon is added) to provide thermal stability.

The approach to true ignition is characterized by an increase in the dominance of the alpha power term over the ohmic heating power and of the n-squared dependent losses over the synchrotron loss. Fig. 5.10 gives the following power ratios; P_α/P_O , P_n^2/P_O , P_α/P_L where P_α is the alpha power, P_O is the ohmic heating power, and the loss terms are the n-squared losses, the synchrotron losses and the total losses.

744371

Fig. 5.7 Assumed behavior of the perpendicular diffusion coefficient during the ignition phase of the operating cycle.

744469

Fig. 5.8 Time needed to reach the density required for full ignition vs energy of injected particles. At energies larger than ~4.5 keV the available injection current is the time limiting factor.

744372

Fig. 5.9 Plasma temperature history during density buildup (true ignition phase) exhibiting a plateau followed by rapid α particle heating.

744373

Fig. 5.10 Plasma power balance during density buildup period.

The Extended Pulse Tokamak Discharge

The ignition procedures described here produce a reacting plasma characterized by a plasma current of 14.6 MA, an average temperature of 30 keV and an average fuel ion density of $5 \times 10^{13} \text{ cm}^{-3}$. The fuel injection rate and fuel composition are chosen to insure that a stable equilibrium operating point is maintained (temperature and density constant in time). Resistive effects, however, result in a non-vanishing line integral of the electric field around the torus,

$$\int \underline{E} \cdot \underline{dl} = I_p R \neq 0 \quad (5.8)$$

The current will decay in time according to the solution of

$$-L \frac{dI_p}{dt} = I_p R + \dot{\phi}_{\text{ext}} \quad (5.9)$$

and consequently, in the absence of a change in flux linking the plasma due to currents in external circuits, the plasma current would decay with a characteristic time constant

$$\tau = \frac{L}{R} = 230.2 \text{ min} \quad (5.10)$$

Our approach calls for extending the pulse for as long as possible by producing a compensating flux change in the central core of the reactor through the use of the control field coil system. This set of coils is designed to produce flux in the central core of the reactor but not in the volume occupied by the plasma; consequently, it does not alter the vertical field required for equilibrium. The compensating emf is given by

$$\dot{\phi}_c = -CI_p R \quad (5.11)$$

where R is the plasma resistance in ohms, and C is the compensation factor. C is a constant having values between 0 and 1 - $C = 0$ is an uncompensated system and $C = 1$ is a fully compensated system. The effective time constant of the discharge is then

$$\tau_e = \frac{L}{R(1-C)} \quad (5.12)$$

Two factors will now limit the length of the burning pulse, the maximum allowable flux in the reactor core consistent with superconducting technology and the maximum tolerable percentage decrease in the plasma current. Optimal design will occur when C is chosen such that both of these conditions occur simultaneously. The current and control flux are given by the solution of Eqs. 5.9 and 5.11

$$I = I_o \exp \left(- \frac{R(1-C)}{L} t \right) \quad (5.13)$$

$$\phi = \phi_{\max} - \frac{CI_oL}{(1-C)} \left[1 - \exp \left(- \frac{R(1-C)}{L} t \right) \right] \quad (5.14)$$

(The control flux is changed from a positive maximum value to a negative minimum value during the pulse). The two conditions to be satisfied are

$$\frac{I}{I_o} = \eta \leq 1, \quad (5.15)$$

and

$$\phi = - \phi_{\max} \quad (5.16)$$

Solving for C we find

$$C = \frac{1}{\frac{LI_p(1-\eta)}{2\phi_{\max}} + 1} \quad (5.17)$$

as was noted earlier.

$$\phi_{\max} = \iint \frac{B_{\max}}{\mu_0} \cdot dA_{\text{core}}, \quad (5.18)$$

and for a fixed core area $\phi_{\max}$ depends on the maximum allowable field in the vicinity of the magnets.

The determination of η is somewhat less certain. As the current decays, β_θ will increase (assuming constant pressure), and the discharge as a whole will shift. The vertical field required for equilibrium is given by⁹

$$B_v = \frac{I_p}{2R_{MAJ.}} \left(\ln \frac{8R_{MAJ.}}{a} - 1.25 + \beta_\theta \right) \quad (5.19)$$

and as the current decreases the discharge will move to a major radius where equilibrium can be maintained. Assuming that the variation of the logarithmic term is small we write

$$R_{eq.} = \frac{I_p}{2B_v} (k + \beta_\theta) \quad (5.20)$$

and assuming constant applied B_v and flux conservation

$$\delta R_{eq.} = \delta I_p \frac{(k + \beta_\theta)}{2B_v} + \frac{I_p}{2B_v} \beta_\theta' \delta I_p \quad (5.21)$$

where β_θ' denotes differentiation with respect to I_p . Since

$$\beta_\theta \propto \frac{RP}{I_p^2} \quad (5.22)$$

we have

$$\beta_\theta' = \frac{1}{R} \frac{dR}{dI_p} \beta_\theta - \frac{2\beta_\theta}{I_p} \quad (5.23)$$

Dividing by R and collecting terms,

$$\frac{\delta R_{eq.}}{R_{eq.}} = \frac{\delta I_p}{I_p} \frac{k - \beta_\theta}{k} \quad (5.24)$$

This demonstrates that the restoring force due to the interaction of the plasma current and the vertical field balances two forces, that due to the plasma pressure and that due to the magnetic stress of the discharge. For β_0 larger than k the pressure force dominates, and a reduction in the restoring force causes the discharge to move outward. On the other hand, as I_p diminishes, the magnetic stress diminishes as well as the restoring force. It, in fact, decays as I_p^2 . If it is the dominant force trying to expand the discharge, ($k > \beta_0$), a reduction of I_p causes contraction. By designing for $\beta_0 \approx k$, reduction of the shift of the discharge is brought about by a cancellation of the two effects. For our design k is very nearly equal to 2 and a β_0 of 1.83 is used. Consequently,

$$\frac{k - \beta_0}{k} = .085 \quad (5.25)$$

The maximum allowable motion of the plasma will be taken as one percent of the major radius or 10.5 cm. This is an arbitrary figure; it is based to some extent on erosion problems in the area of the entrance to the divertor, but is in reality only a guess at what is plausible. Eq. 5.23 then gives

$$\frac{\delta I_p}{I_p} = .118 \quad (5.26)$$

This estimate is based on the initial β_0 . During a 10% decrease in the current β_0 will increase to 2.26, which does not violate the condition $\beta_0 < R/a$ and aids in reducing the motion of the discharge. Solving for C we find

$$C = .82 \quad (5.27)$$

The maximum cycle time (pulse length) is given by

$$\ln(\eta) = - \frac{R(1-C)}{L} t \quad (5.28)$$

and we find

$$t_{\max} = 160 \text{ min} \quad (5.29)$$

When the pulse duration limit is reached, the discharge must be terminated and then reinitiated.

Current Termination Procedure

The problems associated with discontinuing the tokamak discharge are to some extent the inverse of those encountered during ignition. The internal energy of the discharge and the stored magnetic energy of the discharge, vertical field and control field are

$$U_p = 2nkTV = 960 \text{ MJoule} \quad (5.30)$$

and

$$U_m = \frac{1}{2} \sum_{ij} L_{ij} I_i I_j = 7250 \text{ MJoule.} \quad (5.31)$$

About 5000 MJ of the magnetic energy is in the discharge field and vertical field and must be transferred to auxiliary storage or dissipated. The remainder of the field energy is in the control field and will reside there until the next ignition. Our choice of power handling capacity (≤ 500 MW), based largely on economics, requires a shutdown period of at least 10 sec. In light of the large amount of energy stored in the system, it is particularly important that equilibrium be maintained during the shutdown. Failure to do this would result in the rapid transfer of a large portion of this energy to the radiation shield wall, and it is likely that the surface temperature would be raised to above the melting point. Although the erosion that would result from a single occurrence of this kind is not excessive, it could not be tolerated at the end of each pulse. The equilibrium configuration can be maintained at a fixed minor and major radius by requiring that β_0 be constant and that the ratio of the vertical field to the plasma current be held fixed.

The principal shutdown mechanism is to reduce the plasma pressure while the currents are controlled to maintain the equilibrium. Shutdown is initiated by withdrawing the tritium fraction from the fuel feed while maintaining the total feed rate

constant (i.e., the tritium is replaced by deuterium). The density of each of the ion species is assumed to follow

$$\frac{dn}{dt} + \frac{n}{\tau} = S \quad (5.32)$$

where S is the source function for the species in question whose density is n and whose confinement time is τ . We assume that S is a constant of discontinuous value at a finite number of points and τ is a constant given by

$$\tau \approx \frac{a^2}{8D} \quad (5.33)$$

where a is the minor radius and D the reactor diffusion coefficient. The solution of Eq. 5.32 is

$$n = e^{-\frac{t}{\tau}} \left[S_1 \left(e^{\frac{t}{\tau}} - 1 \right) + n_0 \right]$$

where n_0 is the density at the beginning of the time period.

The purpose of removing the tritium from the fuel is to eliminate the source of alpha particles. About 25% of the energy deposited in the plasma by these particles is used in thermalizing cold fuel. Consequently, a "quench" point is reached when the alpha source function decays to 25% of its initial value. The alpha particle source function depends on the product of the deuterium and tritium densities, assuming that large variations in T do not occur.

$$S_{\alpha} \sim n_D n_T = \left(n_0 e^{-\frac{t}{\tau}} \right) \left\{ e^{-\frac{t}{\tau}} \left[2n_0 \left(e^{\frac{t}{\tau}} + 1 \right) + n_0 \right] \right\} \quad (5.34)$$

Here n_0 is taken to be the initial tritium density and a 50/50 fuel balance is assumed. The alpha source is reduced to 25% of its original value when

$$\frac{n_D n_T}{n_D^2} = 2e^{-\frac{t}{\tau}} - e^{-\frac{2t}{\tau}} = .25 \quad (5.35)$$

This is a quadratic equation in $e^{-t/\tau}$ that is solved for the quenching period duration; we find

$$t = 2\tau \quad (5.36)$$

During this time, as the alpha source has weakened the temperature has fallen, due to radiation losses, and this results in quenching occurring actually earlier than $2\tau_p$, which is a conservative estimate. We continue to withdraw energy from the plasma now by radiation. Our model of the shutdown procedure is to begin at the quench point by withdrawing the argon fraction from the feed (total ion feed conserved). This reduces the radiation load on the system, allowing sufficient time to handle comfortably the magnetic energy, and also begins the preparation of the system for the next ignition period during which a very low argon fraction is necessary (1% O is equivalent to 0.09% Ar). Several alternative approaches are possible at this point. One is to maintain the full ion feed rate until equilibrium is lost. This will set a gas density of 5×10^{13} (deuterium) and place the maximum burden on pumping to provide proper conditions for ignition. (Pumping will have to reduce this density by two orders of magnitude, and this would take at least 30 sec.) Alternatively, we can cut the fuel feed off prior to losing the plasma and end up with a somewhat lower density but higher argon fraction. Fig. 5.11 gives the reactor density and argon fraction at shutdown as a function of the feed termination time. While Fig. 5.12 summarizes the reactor parameters during the shutdown period. From the condition that β_0 be constant we have calculated the rate of flux change needed to withdraw the plasma current and tested that maximum power requirements are not violated. The required driving voltage and current are shown in Fig. 5.13.

When a plasma temperature of a few hundred volts is reached, line radiation losses will begin to dominate the energy balance, and a rapid loss of the plasma is to be expected. By choosing the operating procedure to provide the lowest density at this point, the amount of energy deposited in the wall can be minimized, and by using the maximum possible voltage at this point, it is possible to withdraw a sizable portion of the magnetic energy still remaining in the system.

The Equilibrium Problem During Ignition

In the course of the ignition phases of the system operation, β_0 varies from essentially zero to its full operating value. This poses a somewhat difficult

744468

Fig. 5.11 Reactor density and argon fraction at shutdown vs feed cut-off time. Cutoff must occur after quench point is reached.

744375

Fig. 5.12 Plasma parameters during shutdown period normalized to operating state conditions. Shutdown is initiated by withdrawing the tritium fraction, and the argon fraction is shut off at the quench point. The total density is maintained until the shutdown is completed.

744376

Fig. 5.13 Externally applied EMF driving plasma during shutdown period.

equilibrium problem. Consider a symmetric distribution of current in a cylindrical plasma of minor radius, a , located at R (See Fig. 5.14.) A vertical field of the form shown in the figure is assumed; it is characterized by an average higher field strength at small radius. A field null is assumed to exist at x_n (measured from the symmetry axis of the current distribution). The flux passing through the y axis at some point, x , is given by

$$\phi_x = \int_{-x_n}^x B_y dx, \quad (5.37)$$

and at $x = a$,

$$\phi_a = \int_{-x_n}^a B_y dx = 0 \quad (5.38)$$

Using B_v^* for the contribution to B_y we find:

$$2I_p \ln \left(\frac{r_n}{a} \right) = B_v^* (r_n + a) \quad (5.39)$$

But the location of the field null point is determined by

$$B_v^+ = \frac{2 I_p}{r_n} \quad (5.40)$$

Consequently:

$$\ln \left(\frac{r_n}{a} \right) = \frac{B_v^*}{B_v^+} \left(\frac{r_n + a}{r_n} \right) \quad (5.41)$$

We also know that the equilibrium vertical field is given by

$$B_v^* = \frac{I}{R} \left(\ln \left(\frac{8R}{a} \right) - 1.25 + \beta_\theta \right) \quad (5.42)$$

744377

Fig. 5.14 Schematic of vertical field profile needed for equilibrium during current initiation and ignition.

Our problem is to preserve the magnetic limiter as best we can. No physical limiter is provided since serious erosion problems would arise. R , the major radius, must be kept fairly well fixed. This means that at small currents and small β_0 a low equilibrium field is required. However, if this field were uniform in space, the distance to the null point would place the field null outside of the machine, which implies too large a minor radius. We overcome this by producing a non-uniform vertical field. The higher value of $B_v^\dagger$ ensures that the null occurs within the machine, and the increased flux strength in the core aids in the ohmic heating. By providing a sufficiently steep gradient in the vertical field, both the location of the vertical field null point and the major radius may be maintained fairly constant.

Operating Procedures for the Princeton Reference Design Model

The procedures presented here are particular to our design and its technology. The purpose of this exercise is twofold: first to illustrate the ideas presented in the earlier portions of this chapter and to provide a plausible scheme for the operation of this design model.

The maximum total magnetic and internal energy of this system (excluding the toroidal field energy) is

$$U = \frac{1}{2} \sum_i \sum_j L_{ij} I_i I_j + \sum_k N_k T_k = 8210 \text{ MJoule.} \quad (5.43)$$

Approximately 60% of this energy must be delivered to the system during ignition. The remaining 40% is already resident in the coil systems.

The total cycle can be thought of as consisting of the following four distinct phases:

- I. n-squared ignition phase
- II. Pressure buildup phase
- III. Fusion Burn phase
- IV. Shutdown phase.

The control function during each of these phases is best characterized by the rate of change of flux produced by the external coil systems linking the discharge. Fig. 5.15 is a schematic diagram illustrating the role of the vertical coil system and control

734465

Fig. 5.15 Schematic of vertical field flux and control flux during I-current ignition (n^2 -ignition), II-pressure buildup, III-burn phase, and IV-shutdown.

coil system in each of these phases. In phase I, the n-squared ignition phase, the plasma current is brought to its operational value of 14.6 MA while $\beta_0 \approx 0$. This is accomplished by increasing the vertical field flux linking the plasma. The IR drop of the plasma will be fully compensated and consequently, the load on the vertical field coil system is purely inductive. In phase II the pressure, and with it β_0 , will increase to their operating values. Because of this the vertical field must increase to provide equilibrium at a higher pressure. The resulting increase in vertical field flux is compensated by the control field windings keeping the current constant. Phase III, the burning phase, is characterized by no change in the vertical field flux and 82% compensation of the resistive loss. Finally, during shutdown the vertical field flux is withdrawn, and the control field flux is set to its preignition value.

Discussion

Reactor ignition requires limited losses and maximum input power. Reactor operation requires enhanced losses and restricted input power. These statements pose the principal problem of the operating cycle of a long pulse fusion reactor - conditions that are desirable during operation cannot be tolerated during ignition.

Under reactor conditions, alpha particles resulting from fusion reactions will be thermalized in several seconds.^{10,11} Consequently, any system calling for a pulse length of some tens of seconds or longer must consider the temperature stability of the chosen operating point. One finds that the low temperature intersection of the alpha power source curve with the total loss curve is unstable. A stable intersection occurs at high temperatures. For a 50:50 DT plasma and no impurities this point lies in excess of 100 keV. This temperature is uneconomical in a pressure-limited system. By enhancing the losses (addition of $\approx 5\%$ argon to fuel) we are able to reduce this equilibrium point to a more economical temperature ($T \approx 30$ keV). We consider the argon additive as a representative means to an end, the enhancement of the radiation loss. As the presence of impurity ions is undesirable during ignition, an alternative mechanism should be found.

The wide range of operating states encountered forces us to consider an equally wide range of plasma energy and particle loss rates. Fig. 5.16, after Furth and Rutherford,¹² displays the several regimes of plasma confinement. Superimposed on it (heavy line) is the current trajectory of the discharge as a function of temperature and the assumed plasma confinement (broken line). We see from the diagram the several regimes of plasma behavior that we expect to encounter. Once again the needs of the

744334

Fig. 5.16 Assumed confinement behavior during current initiation and ignition. The value of $n\tau$ assumed, (dashed line) is lower than that predicted by theory, (solid line).

ignition period (long confinement time) are not the same as those of the operating state. It is likely that we will need to develop methods of "spoiling" the good confinement predicted by theory during the burning phase of the operation.

The purpose of operating at low density during the current buildup phase is to maximize the amount of energy per particle deposited in the plasma. In this way n -squared ignition is achieved, and the maximum advantage may be made of alpha particle energy in achieving true ignition. The time scale of the current buildup, however, is not based on the desire to reach n -squared ignition - the load on the system is overwhelmingly inductive. Our estimate of an economic size of power supply has set the time scale. It has also been basic to our decision in favor of a long pulse design. A shorter pulse length will require either larger power requirements, a degraded duty factor or intermittent operation. Each of these alternatives causes increased cost. The possible advantage of the short pulse design is to eliminate the need for a divertor and fueling. This, however, would require reducing the pulse length to not much longer than the particle confinement time, and it would seem that the increased power cost would far exceed the savings realized in eliminating the divertor and fuel injectors.

References

- ¹R. G. Mills, Some Engineering Problems of Thermonuclear Reactors, Nuclear Fusion 7, 223 (1967).
- ²I. N. Golovin, Yu. N. Dnestrovsky, and D. P. Kostomarov, Tokamak as a Possible Fusion Reactor - Comparison with Other CTR Devices, Nuclear Fusion Reactor Conference Proceedings (British Nuclear Energy Society, London, 1969) pp. 194-222.
- ³A. Gibson, Permissible Parameters for Economic Stellarator and Tokamak Reactors, Nuclear Fusion Reactor Conference Proceedings, *ibid.*, pp. 233-242.
- ⁴T. H. Stix, Heating a Tokamak Plasma, Symposium on Plasma Heating and Injection (Editrice Compositori, Bologna, Italy, 1972) pp. 1-8.
- ⁵D. R. Sweetman, Ignition Ionization Conditions in Tokamak Experiments and Role of Neutral Injection Heating, Nuclear Fusion 13, 157 (1973).
- ⁶E. Hinnoy, On Multiple Ionization in High-Temperature Plasmas, Princeton Plasma Physics Laboratory Report MATT-777 (1970).
- ⁷T. H. Stix, *op. cit.*
- ⁸K. Bol, R. A. Ellis, H. P. Eubank, H. P. Furth, R. A. Jacobsen, L. C. Johnson, E. Mazzucato, W. Stodiek, E. L. Tolnas, Adiabatic Compression of the Tokamak Discharge, Phys. Rev. Letters 29, 1495 (1972).
- ⁹J. M. Greene, J. L. Johnson, and K. E. Weimer, Tokamak Equilibrium, Phys. Fluids 14, 671 (1971).
- ¹⁰R. G. Mills, Time-Dependent Behavior of Fusion Reactors, Princeton Plasma Physics Laboratory Report MATT-728 (1970).
- ¹¹S. T. Butler and M. J. Buckingham, Energy Loss of a Fast Ion in a Plasma, Phys. Rev. 126, 1 (1962).

¹²H. P. Furth and P. H. Rutherford, Experimental Tests of Tokamak Reactor Scaling, private communication.

Chapter 6
The Divertor and Vacuum System
F. H. Tenney and G. Lewin

Magnetic Characteristics of the Poloidal Divertor

Magnetic Topography

The magnetic field of the reference reactor is composed of a toroidal field and a poloidal field. These fields are axisymmetric and result from current flow in a set of suitable coils and in the plasma. The poloidal field that passes near the discharge is indicated in Fig. 6.1. It is characterized by two types of field lines: type A, confined to the volume of the discharge, and type B which extends from near the discharge to regions far from the discharge. The field line separating the regions of these two types of poloidal field lines is called the separatrix line. The separatrix line contains a null point of the poloidal field. The shape of the separatrix line surrounding the discharge volume was designed to approximate a circle of given minor radius located at a given major radius.

The total magnetic field of the reference reactor is obtained by adding the toroidal field to the poloidal field. The resulting field lines lie on surfaces of revolution about the axis of symmetry generated by the poloidal field lines. The magnetic flux defined by surfaces generated by type B poloidal field lines is the "diverted flux". The term "divertor" refers to that volume of space, not immediately adjacent to the discharge volume, that contains diverted flux. Diverted flux lying close to the separatrix surface extends deep into the divertor, as shown in Fig. 6.2, where it passes through the vacuum wall of the exhaust chamber at a major radius of about 13 meters. The amount of flux that can make such deep penetration into the divertor will be referred to as the "scrapeoff" flux and is limited by the material walls of the reference reactor, such as at points A and B in Fig. 6.2. Although the spacing between flux surfaces is accurately revealed in Fig. 6.2., the actual cross-sectional area of the flux thereby contained is not evident. The geometry of the situation is indicated in Fig. 6.3 from which it can be deduced that the total flux, F , between two flux surfaces is given by

$$F = h \cdot B_p \cdot 2\pi R \quad (6.1)$$

744005

Fig. 6.1 Poloidal magnetic field near discharge.

734445

Fig. 6.2 Magnetic flux surfaces, divertors and coils.

$$\begin{aligned}\Delta A &= hR \Delta\phi \sin\theta \\ &= hR \Delta\phi \frac{B_p}{B}\end{aligned}$$

744303

Fig. 6.3 Geometry of flux tube.

Here h is the separation of the flux surfaces at a major radius, R , where the poloidal magnetic field is B_p . The total cross-sectional area, A , through which this flux passes is given by

$$A = F/B \quad (6.2)$$

where B is the total magnetic field. It is now clear by Eq. 6.1 that the spacing between adjacent flux surfaces will vary inversely with the quantity $B_p \cdot R$. The corresponding cross sectional areas through which the flux passes, however, will simply vary inversely with the total field. Since, in the reference reactor, the poloidal field is only a small fraction of the toroidal field, A will vary essentially as the major radius. Thus the spacing between the separatrix and the outer limiting flux surface of the scrapeoff flux falls from a nominal dimension of 37.5 cm in the divertor channel to 3.7 cm at the outer part of the discharge volume (point C in Fig. 6.2.); whereas the cross-sectional area of the flux between these surfaces increases from about 2500 cm^2 to about 7500 cm^2 , as B falls from some 130 kilogauss to about 45 kilogauss at point C. Adding the flux on both sides of the separatrix and summing over both the top and bottom of the reference reactor, the entire scrapeoff flux has a cross-sectional area of about 10^4 cm^2 in the divertor channel.

The length of the field lines in passing from near the discharge volume to the exhaust chamber varies for each flux surface. In fact, the field line passing through the poloidal field null point on the separatrix closes on itself. However, the real length of interest is the distance charged particles will travel in passing along the scrapeoff flux. A 50 eV test deuteron started on the separatrix at point C in Fig. 6.2 travels about $7.0 \times 10^4 \text{ cm}$ to reach point A. The numerals along the separatrix of Fig. 6.2 indicate the revolutions about the Z axis made by the test particle. It makes 18 revolutions to reach point A.

Near the separatrix in the divertor channel the field lines make an angle of about 1.5 degrees with the circumferential direction.

Coil Configuration and Function

The coil configuration to produce the desired poloidal field of the reference reactor is shown in Fig. 6.2. The coils are circular, concentric with the Z axis, and occur in pairs equidistant above and below the $Z=0$ plane of symmetry. The small decimal near each coil in Fig. 6.2 is the required current relative to the discharge current

which is taken as unity. The discharge current is represented by a circular filament located on the plane of symmetry at a major radius of about 11.5 meters. This representation of the complex distribution of currents throughout the discharge volume is judged to produce a reasonably good approximation of the general shape of the separatrix surface and the magnetic fields outside the discharge volume. The magnetic fields inside the discharge volume due to the discharge current will not be given very well by this model, and it is not used for that purpose.

The five pairs of coils that carry current in a negative sense are called the vertical field coils. The remaining two pairs of coils are the divertor coils. The primary function of the vertical field coils is to provide the vertical magnetic field in the discharge volume necessary for magnetohydrodynamic equilibrium and radial and vertical stability of the discharge. This vertical field, together with the poloidal field arising from the discharge current, locates the null point of the separatrix. The general shape of the separatrix surface in the divertor is determined primarily by the large divertor coil just outside the exhaust chamber. The flat divertor coil influences most strongly the centering of the separatrix surface in the adjacent divertor channel.

A major advantage of this coil arrangement is that the coils can be placed outside of the neutron shielding. (See Fig. 6.4.) Furthermore, no coil is immediately surrounded by hot plasma. Thus the thermal loading on the coils is low enough to render feasible their being made superconducting. Discussion of the coil construction may be found in Chapter 13. A major disadvantage of this coil arrangement is the threading of the toroidal coils.

For a given location of the discharge current, the magnetic field configuration depends only on ratios of currents. The sensitivity of the separatrix surface to changes in the discharge current is shown in Table 6.1

Table 6.1

Discharge Current	Separatrix Surface	
	Null Shift	Channel Shift
$\Delta I/I = 1.4\%$	-6 cm	-5 cm
$\Delta R = 10$ cm	6	5
$\Delta Z = 10$ cm	0.25 (in Z)	2.3

Since the divertor channel is 75 cm wide, a radial shift of the separatrix of 15 cm may be tolerable. Thus 2% regulation of the various currents is desirable.

734274

Cross Section of the Nuclear Island

Walls and Collector Structure

As indicated in Fig. 6.2, the material walls are about 50 cm from the nominal separatrix surface in the vicinity of the discharge volume. The walls follow the limiting flux surfaces through the divertor channel up to the exhaust chamber where they suddenly flare away from the scrapeoff flux. As evident in Fig. 6.4, the width of the divertor channel affects both the size of the opening through which neutrons may leak from the discharge volume and the thickness of neutron shielding adjacent to the toroidal field coils. Neutron damage and neutron heating of the superconducting toroidal field coils produce a design pressure to maximize the space available for neutron shielding. (See Chapters 10 and 13.) On the other hand, reducing the divertor channel width increases the delicacy of keeping the separatrix surface centered in the channel and increases the plasma interaction with the walls. Scaling up the major radius of the reference reactor would provide more shielding space, but would incur an economic penalty, associated with the larger size, and possibly a reduction in both aspect ratio and current density neither of which is desirable. The difficulties of low current density are briefly discussed in Chapter 5.

The walls of the divertor channel and exhaust chamber are cooled. The heat load will be mainly line radiation from the Ar content of the plasma, but will not exceed an average of 20 watts/cm².

Early thinking provided a collector structure in the exhaust chamber upon which the plasma would impinge. Consideration of collector erosion leads to an attempt to keep plasma temperatures at the collectors to less than 10 eV or so. The present thought is to arrange for the plasma to undergo deionization just prior to entering the exhaust chamber thereby eliminating the necessity for collectors.

Functions of a Divertor

The divertor was conceived for steady state devices. The original concept¹ was to reduce the interaction between the hot plasma of a reactor and the cool ions that would exist next to a material wall. In this way it was hoped that the edge temperature of the plasma could be elevated and that impurities from the walls would be ionized and flow into the divertor before penetrating the hot plasma.

These results hinge upon the relative ease with which plasma can flow along magnetic field lines compared to flowing across magnetic field lines. An ion diffusing into the scrapeoff flux would be expected to travel a long way down the field lines toward a collecting surface while diffusing only a short distance across the field. Thus

wall bombardment by ions would be reduced and collection of ions at desired regions near the vacuum pumps enhanced. Furthermore, neutral particles from the wall would be ionized in the scrapeoff plasma and flow to the collecting regions before diffusing across the field into the discharge plasma.

It may be mentioned in passing that a relatively dense blanket of neutral gas surrounding the reacting plasma can prevent direct plasma-wall interaction as well.² Pumping particles from the plasma and impurities from the wall could be accomplished by using the gas blanket as a drag stream. However, the edge temperature of the plasma would be near the ionization energy. The merits of this approach have not been analyzed in this study. We concern ourselves here with the ramifications of the divertor concept.

Characteristics of Plasma in the Poloidal Field Divertor

Overview of Scrapeoff Plasma

The view of the scrapeoff plasma that evolved from this study is that of a relatively cold plasma blanket surrounding the reacting plasma at the separatrix and extending into the exhaust chamber where it undergoes deionization without direct contact with material surfaces. The plasma fills the entire width of the divertor channel and flows at considerably subsonic speeds in order to carry the throughput of particles. The scrapeoff plasma supports only a few hundred eV temperature difference between the hot end at the separatrix surrounding the discharge volume and the cold end in the exhaust chamber that must be at a temperature of about 1 eV to deionize. This temperature difference corresponds to over 100 MW of thermal power flowing out of the discharge volume. This thermal flow results from imposing a low temperature and moderate density boundary condition on the reacting plasma. The scrapeoff plasma must dissipate this entire thermal energy flow before deionization can take place. This dissipation is accomplished via impurity radiation (by the argon in the plasma) and via interaction with neutrals from the walls of the divertor channel and the exhaust chamber. This dissipation is sensitive to the density of the scrapeoff plasma, which will vary over two orders of magnitude from end to end from about 6×10^{12} to 10^{15} cm^{-3} . This variation is a consequence of the temperature variation together with the assumption of constant pressure along the plasma since the flow is subsonic.

The nominal operating condition for the reactor will result in the scrapeoff plasma deionizing just at the entrance to the exhaust chamber. This condition can be maintained presumably by control of the composition and injection rate of the fuel. The neutral pressure in the exhaust chamber may be effectively decoupled from the plasma flow

provided it is less than about 1 torr. This decoupling occurs by virtue of neutral gas leaving the region of deionization of the scrapeoff plasma at supersonic speeds. The production of supersonic neutral gas flow is connected with the pressure produced by the diamagnetic currents that flow in the region of deionization.

This picture of the scrapeoff plasma will be discussed in the sections that follow.

General Aspects

One of the most uncertain areas of analysis has been the behavior of the plasma that flows in the scrapeoff volume. This volume extends from immediately adjacent to the discharge volume to deep into the exhaust chamber. The scrapeoff plasma provides the boundary condition for the reacting plasma. It provides both a conduit for the flow of particles to collecting surfaces and a thermally conductive medium for the flow of heat from the discharge volume. The boundary conditions of the scrapeoff plasma can thus have a profound effect on the density and temperature profiles of the reacting plasma.

Plasma will diffuse across the separatrix from the discharge volume. Once across the separatrix the plasma will presumably flow approximately along field lines into the discharge chamber where it can be collected on some material wall or simply deionize to form a flow of neutral gas if its temperature is low enough. The key question is how well is the plasma collected?

If the entrance of the exhaust chamber behaves like a black hole, that is, a perfect absorber for particles, we might expect the scrapeoff plasma to have a low density relative to the discharge plasma, to be flowing at sonic speeds and support a high temperature on the separatrix at the discharge volume. The width of the plasma flow into the exhaust chamber will be determined by how far a particle will travel across the magnetic field lines during a single trip into the divertor. Trapping of plasma in the mirror fields of the scrapeoff flux will result in some diffusion across field lines before the plasma can leak out of the mirrors and will contribute to the width of the plasma flow.

If the entrance to the exhaust chamber only partially absorbs particles, we might expect the scrapeoff plasma to have higher densities, lower temperatures and fill the entire available width of the scrapeoff flux. The width of the plasma flow would be limited either by material walls or by the ability of the scrapeoff plasma to transport energy transverse to the magnetic flux surfaces. In either case, the temperature and density distribution of the scrapeoff plasma will depend on the interaction between the plasma and neutrals from the walls and the exhaust chamber.

Pumping Assumptions and Subsonic Flow

For the Princeton reference reactor the assumption has been made that conventional vacuum pumps would be used if possible. Hence, the analysis proceeds on the assumption of using diffusion or turbo-molecular pumps. Now the entrance of a good diffusion pump can have the pumping speed of a black hole of about $1/3$ the area of the entrance. Thus we say a diffusion pump pumps like $1/3$ of a black hole. If a cold trap is added to the diffusion pump, as is usually necessary, the pumping area of the combination pumps like $1/6$ of a black hole. Ducting further reduces the pumping speed. Thus we can take the pumping speed of the exhaust chamber near its entrance to be at most something like $1/20$ of a black hole. Hence, the directed flow of neutrals down the exhaust chamber to the pumps is very much less than the random flow of neutrals. Consequently, if the scrapeoff plasma flows into the exhaust chamber its surface will be exposed to a relatively large random flux of neutrals. If the probability for these neutrals to be ionized by the plasma is 50 percent, the steady state flow of plasma particles into the exhaust chamber can be an order of magnitude larger than the throughput. To see this, consider a model of the plasma collecting region shown in Fig. 6.5a. A flux of plasma ions, Γ_i , falls on the collector. A flux of neutralized plasma ions, Γ_c , leaves the collector and enters the plasma. We consider a steady state and take $\Gamma_c = \Gamma_i$.

A neutral entering the plasma will either be ionized, undergo charge exchange, or remain neutral and leave the plasma. If charge exchange takes place, the number of ions in the plasma will not be changed. If a slow neutral charge exchanges with a fast ion, a fast neutral and a slow ion result. This produces a loss of energy from the plasma ions. On the other hand, if a fast neutral undergoes charge exchange with a fast ion, in effect no energy is transferred, but the direction of motion of each particle is suddenly altered. Thus, a fast neutral will effectively diffuse through the plasma via charge exchange events. In so doing, it may undergo ionization.

Let p_1 be the probability that a particle of the Γ_i flux becomes ionized either before charge exchange or as a neutral after charge exchange, but before it has passed through the imaginary surface surrounding the plasma and collector. Let Γ_n be the flux of neutrals passing into the plasma through the imaginary surface. Let p_2 have the same meaning as p_1 , but in reference to particles of the Γ_n flux.

Let Γ_0 be the net injection of plasma ions that occurs outside of the collection region. Γ_0 represents the flow of ions from the discharge volume into the scrapeoff plasma and thus represents the throughput of neutral particles that eventually passcs through the pumps.

744059

Fig. 6.5 Particle flux diagrams.

In steady state, the flow of ions through the plasma is conserved. Thus,

$$\Gamma_i = \Gamma_o + p_1 \Gamma_c + p_2 \Gamma_n. \quad (6.3)$$

The total flow of neutrals through the imaginary surface from the plasma side is $\Gamma_c(1-p_1) + \Gamma_n(1-p_2)$. We define the "greyness factor", g , by the equation

$$\Gamma_o = g[\Gamma_c(1 - p_1) + \Gamma_n(1 - p_2)]. \quad (6.4)$$

Since we have taken $\Gamma_i = \Gamma_c$, it follows from Eqs. 6.3 and 6.4 that

$$\Gamma_i = \frac{\Gamma_o}{(1 - p_1)} \cdot \left(1 + p_2 \frac{(1 - g)}{g} \right) \quad (6.5)$$

We can relate the greyness factor, g , to the speed of the vacuum pumps by the following argument. Consider the closed volume of Fig. 6.5b bounded by a perfectly reflecting surface except for two holes of areas A_1 and A_2 . Let area A_2 coincide with a pumping duct that is characterized by the statement that a fraction, g_2 , of all the particles falling on A_2 from volume V will pass through the pumps. Consider a gas being pumped through volume V via A_1 and A_2 . Let n be the density of gas in V . Then if the motions of the particles in V are essentially random, the flux density of particles striking the bounding surface will be nv , where v is an average speed ($\bar{v}/4$) of the particles. If Γ is the flux of particles passing through A from outside V , the net flow of particles into V via A will be $(\Gamma - nvA_1)$. In steady state this flow must be equated to the particles passing through the pumps. Hence

$$\Gamma - nvA_1 = g_2 nvA_2. \quad (6.6)$$

We now define a "greyness factor", g_1 , for A_1 by the relation

$$\Gamma - nvA_1 = g_1 \Gamma. \quad (6.7)$$

It follows that

$$g_1 = g_2 \cdot \frac{1}{g_2 + A_1/A_2} \quad (6.8)$$

Thus if $A_1/A_2 \ll g_2$, then $g_1 \approx 1$, and A_1 acts like a "black hole", perfectly absorbing. If $A_1/A_2 \approx 1 \gg g_2$, then $g_1 \approx g_2$.

Relating this discussion to the pumping of the exhaust chamber of the divertor, we would take $g_2 < 0.05$ and associate A_2 with the cross section of the exhaust chamber perpendicular to the radial direction. The surface area of the scrapeoff plasma entering the exhaust chamber would be associated with A_1 , which would be comparable to A_2 . Hence we conclude that g_1 , which we identify with the quantity g defined by eq. 6.4, will about equal g_2 . If we assume $p_1 \approx 0$, what should we take for p_2 ? Since the scrapeoff plasma is expected to have electron densities greater than 10^{13} cm^{-3} , electron temperatures greater than 10 eV, and be tens of centimeters thick, we can take $p_2 \approx 0.5$. Eq. 6.5 then yields the estimate that the flow of ions into the exhaust chamber will exceed the throughput by an order of magnitude. Such a picture describes subsonic flow, which will be adopted for the flow of plasma into the divertor in further discussion.

Plasma Wall Interaction

The plasma interaction with the wall will depend on the magnitude of the plasma density at the wall. This is a very complicated problem. We attempt to get some estimate of the flux of plasma to the walls by means of the following crude, one dimensional model.

We consider plasma flowing along magnetic field lines in the x direction. We assume all properties of the plasma to be uniform except for the density, n , which has only a y dependence. We assume the plasma diffuses with a fixed diffusion coefficient, D . Plasma particles are injected along the x -axis at a current density of J_0 as shown in Fig. 6.6. The wall at $y = y_1$ is a perfect absorber of ions, but is assumed to emit a neutral particle for each ion absorbed. Furthermore, the emitted neutrals are assumed to be uniformly absorbed in the volume of the plasma. The plasma is collected at $x = L$ on a surface with sticking fraction, g . (This "sticking fraction" is analogous to the "greyness factor" above.) Thus, if the average speed of the particles in the $+x$ direction is $\bar{v}_x$, the collected current density of particles will be $n(y)\bar{v}_x g \equiv J_x$.

744302

Fig. 6.6 Plasma-wall interaction model.

All particles are reflected at the boundary $x = 0$. The diffusion current density, $J(y)$, is given by

$$J = -D \frac{dn}{dy}.$$

The boundary condition at $y = y_1$ is taken as

$$2n\bar{v}_y = -D \frac{dn}{dy} \equiv J_w, \quad y = y_1$$

where $\bar{v}_y$ is the average speed of the gyrating ions in the $+y$ direction (for a Maxwellian velocity distribution $\bar{v}_x = \bar{v}_y$). The uniform source strength of ions generated from the neutrals emitted by the wall is given by J_w/y_1 .

Conservation of ions in steady state flow yields the equation

$$\frac{d^2 n}{dy^2} - \frac{n}{\lambda^2} = -J_w/(y_1 D) \quad (6.9)$$

where $\lambda^2 \equiv L D / (g \bar{v}_x)$. We note that $g \bar{v}_x$ is just the net flow speed of the plasma along x . Thus $L / (g \bar{v}_x)$ is just the time it takes for the plasma to flow a distance, L , along the field.

The solution of Eq. 6.9 that satisfies the boundary conditions is

$$n(u) = \frac{J_w \lambda}{D R} \left(1 + u_1 \sinh(u_1 - u) - \left[1 - u_1 D / (2\lambda \bar{v}_y) \right] \cosh(u_1 - u) \right) \quad (6.10)$$

where: $u \equiv y/\lambda$, $u_1 = y_1/\lambda$

and $R \equiv \left(u_1 \cosh u_1 - \left[1 - u_1 D / (2\lambda \bar{v}_y) \right] \sinh u_1 \right)$.

Furthermore, we have:

$$J_w/J_o = 1/R \quad (6.11)$$

Now $J_o L$ equals the net collection of particles at $x = L$, and $J_w L$ equals the recycling of particles from the wall at y_1 . Thus, J_w/J_o is just the ratio of the recycled flux of ions to the throughput of ions collected at $x = L$. We note that for geometries of interest $y_1/L \ll 1$, and hence we can assume

$$\frac{u_1 D}{2\lambda \bar{v}_y} = \frac{g y_1}{2L} \cdot \frac{\bar{v}_x}{\bar{v}_y} \ll 1$$

Under this assumption R is a function of only u_1 . Eq. 6.11 is plotted in Fig. 6.7, and Eq. 6.10 is plotted in Fig. 6.8 for the case of $y_1/L \ll 1$.

To estimate u_1 we assume D to be given by Bohm diffusion at an electron temperature of 100 eV and in the divertor channel a field of 133 kilogauss. Hence $D = 4700 \text{ cm}^2/\text{sec}$. For a 2.5 unit mass particle at this temperature we take $\bar{v}_x = 2.48 \times 10^6 \text{ cm/sec}$. Taking $L = 5 \times 10^4 \text{ cm}$ and $g = 1/20$ we have $\lambda = 44 \text{ cm}$. Choosing $y_1 = 40 \text{ cm}$ we find $u_1 = 0.9$. Hence $J_w/J_o = 3.5$. If we choose $g = 1/50$, we find $u_1 = 0.57$ and hence $J_w/J_o = 9.0$.

The implication of this crude model is that we might expect a particle to recycle with the wall about ten times before reaching the exhaust chamber. For a throughput of particles of about $2 \times 10^{22} \text{ sec}^{-1}$ our assumptions produce about a megawatt of power going to the divertor channel walls via ion bombardment. Since the wall area is about 10^7 cm^2 , this power is less than 1 watt/cm^2 . The ion flux on the divertor channel walls calculates to $2 \times 10^{16} \text{ ions/cm}^2 \text{ sec}$. At a sputtering yield of 0.003 for 100 eV deuteron or triton on iron, we would expect a wall erosion of 0.2 mm/yr.

Neutral-Plasma Interaction

The interaction of neutrals with the plasma can lead to energy loss from the plasma via neutral line radiation or charge exchange. As mentioned in the section on pumping assumptions and subsonic flow above, slow neutrals are converted to fast neutrals

744007

Fig. 6.7 Recycling ratio.

744006

Fig. 6.8 Scrapeoff plasma density profiles.

via charge exchange with fast ions. The diffusion of neutrals through a plasma via charge exchange events is an effective way to transport energy within and out of the plasma. To estimate these effects resort is made to a simple steady state slab model.

Consider a slab of plasma of uniform temperature and density. Let a thick blanket of neutral gas of the same material as the plasma exist on both sides of the slab. Thus, the model is symmetric about the center of the slab. The neutrals from the gas are "slow neutrals", and they will enter the plasma slab to become either ionized or undergo charge exchange or simply pass through the slab. We treat the slow neutrals as if they had a single speed v_s . Let λ_{si} and λ_{sx} be the mean free paths for ionization and charge exchange of a slow neutral. Then the mean free path for either event will be λ_{TS} where

$$\frac{1}{\lambda_{TS}} = \frac{1}{\lambda_{si}} + \frac{1}{\lambda_{sx}}.$$

Let the slab exist for $0 \leq x \leq 2W$, and let $n_s(x)$ be the density of slow neutrals. We approximate $n_s(x)$ by the following expression:

$$n_s(x) = n_s(0) \left(e^{-x/\lambda_{TS}} + e^{-(2W-x)/\lambda_{TS}} \right). \quad (6.12)$$

The source of fast neutrals, $q(x)$, in the plasma will then be

$$q(x) = n_s(x) v_s / \lambda_{sx}.$$

The fast neutrals diffuse through the slab with a diffusion coefficient, D , where we take

$$D = \frac{1}{3} v_f \lambda_{fx}. \quad (6.13)$$

λ_{fx} is the mean free path for a fast neutral to undergo charge exchange. We treat the fast neutrals as if they had a single speed v_f .

Let τ_i be the average time it takes for the plasma electrons to ionize either

a slow or fast neutral. The conservation of fast neutrals is then expressed by the equation

$$-D \frac{d^2 n(x)}{dx^2} = -\frac{n(x)}{\tau_i} + q(x) \quad (6.14)$$

where $n(x)$ is the density of fast neutrals in the plasma. We solve Eq. 6.14 in the region $0 \leq x \leq W$ with the following boundary conditions. At $x = W$ the net diffusion of fast neutrals must vanish by symmetry since only volumetric sources are considered. Hence we take

$$\frac{dn}{dx} = 0, \quad x = W$$

The second boundary condition derives from treating the gas blanket outside the plasma as a perfect absorber for fast neutrals. In view of our "diffusion approximation" expressed by Eq. 6.13, we take the perfect absorber boundary condition to be

$$\frac{1}{2} D \frac{dn}{dx} = \frac{nv_f}{4}, \quad x = 0$$

The desired solution of Eq. 6.14 is expressed in terms of a length, x_0 , that is given implicitly by the following relations:

$$\begin{aligned} \tanh(x_0/\ell) &= \beta, & \text{if } \beta < 1, \\ \coth(x_0/\ell) &= \beta, & \text{if } \beta > 1, \end{aligned}$$

where:

$$\begin{aligned} \beta &\equiv 2D/(v_f \ell), \\ \ell^2 &= D\tau_i. \end{aligned}$$

The expression for $n(x)$ takes on slightly different forms if $\beta > 1$ or if $\beta < 1$.

For $\beta < 1$ we find

$$n(x) = C(x_0) \left(\frac{\ell \lambda_{TS}}{\ell + \lambda_{TS}} \left[\cosh\left(\frac{W-x}{\ell}\right) F_1(x, x_0) + \sinh\left(\frac{x_0+x}{\ell}\right) F_2(x) \right] \right. \\ \left. + \frac{\ell \lambda_{TS}}{\ell - \lambda_{TS}} \left[-\cosh\left(\frac{W-x}{\ell}\right) F_3(x, x_0) + \sinh\left(\frac{x_0+x}{\ell}\right) F_4(x) \right] \right) \quad (6.15)$$

where

$$C(x_0) \equiv \frac{n_s(o) v_s}{\lambda_{sx}} e^{-w/\lambda_{TS}} \cdot \frac{\ell}{D} \cdot \frac{1}{\cosh\left(\frac{W+x_0}{\ell}\right)}$$

and

$$F_1(x, x_0) \equiv \cosh \left[\frac{W}{\lambda_{TS}} - \frac{x_0}{\ell} - x \left(\frac{1}{\lambda_{TS}} + \frac{1}{\ell} \right) \right] - \cosh \left(\frac{W}{\lambda_{TS}} - \frac{x_0}{\ell} \right),$$

$$F_2(x) \equiv \sinh \left[(W-x) \left(\frac{1}{\lambda_{TS}} + \frac{1}{\ell} \right) \right],$$

$$F_3(x, x_0) \equiv \cosh \left[\frac{W}{\lambda_{TS}} + \frac{x_0}{\ell} - x \left(\frac{1}{\lambda_{TS}} - \frac{1}{\ell} \right) \right] - \cosh \left(\frac{W}{\lambda_{TS}} + \frac{x_0}{\ell} \right)$$

$$F_4(x) \equiv \sinh \left[(W-x) \left(\frac{1}{\lambda_{TS}} - \frac{1}{\ell} \right) \right]$$

For $\beta > 1$, the formula for $n(x)$ can be derived from Eq. 6.15 by making the following substitutions:

$$\cosh \left(\frac{W}{\lambda_{TS}} - \frac{x_0}{\ell} \right) \longrightarrow -\sinh \left(\frac{W}{\lambda_{TS}} - \frac{x_0}{\ell} \right),$$

$$\cosh \left[\frac{W}{\lambda_{TS}} - \frac{x_0}{\ell} - x \left(\frac{1}{\lambda_{TS}} + \frac{1}{\ell} \right) \right] \longrightarrow -\sinh \left[\frac{W}{\lambda_{TS}} - \frac{x_0}{\ell} - x \left(\frac{1}{\lambda_{TS}} + \frac{1}{\ell} \right) \right]$$

and the remaining hyperbolic functions containing x_0 in their argument are replaced according to the rule

$$\begin{matrix} \cosh \\ \sinh \end{matrix} (x, x_0) \longrightarrow \begin{matrix} \sinh \\ \cosh \end{matrix} (x, x_0)$$

The flux of fast neutrals, $\Gamma_f(0)$, leaving the plasma slab is just

$$\Gamma_f(0) = -D \frac{dn(x)}{dx} \quad , \quad x = 0 \quad .$$

Hence the probability, P_{nf} , that a slow neutral entering the plasma slab will result in a fast neutral leaving the plasma is just the ratio of $\Gamma_f(0)$ to the slow neutral flux incident at the slab, namely

$$P_{nf} = |\Gamma_f(0)| / (n_s(0) v_s)$$

For $\beta < 1$ we have:

$$P_{nf} = \frac{c}{\lambda_{sx}} \cdot \frac{\cosh(x_0/\ell)}{\cosh\left(\frac{W+x_0}{\ell}\right)} \cdot \left(\frac{\ell \lambda_{TS}}{\ell + \lambda_{TS}} \cdot \sinh \left[W \left(\frac{1}{\lambda_{TS}} + \frac{1}{\ell} \right) \right] + \frac{\ell \lambda_{TS}}{\ell - \lambda_{TS}} \cdot \sinh \left[W \left(\frac{1}{\lambda_{TS}} - \frac{1}{\ell} \right) \right] \right) \quad (6.16)$$

In order to calculate the probability, P_{si} , that a slow neutral entering the plasma will lead to an ionization of a neutral, we must calculate the probability that a slow neutral will leave the plasma slab. In view of our assumption expressed by Eq. 6.12 we take this probability to be $\exp(-2W/\lambda_{TS})$. Thus we have:

$$P_{si} = 1 - P_{nf} - e^{-2W/\lambda_{TS}} \quad (6.17)$$

P_{si} is plotted in Fig. 6.9 for a hydrogen plasma at a density of 10^{13} cm^{-3} . The justification for taking $p_2 = 0.5$ on page 83 above is found in Fig. 6.9 where it is apparent that the scrapeoff plasma can readily ionize an influx of neutrals if temperatures exceed 10 eV and densities are 10^{13} cm^{-3} . Consequently the presence of the scrapeoff plasma in the divertor channel is expected to act like a pump for neutrals flowing out of the exhaust chamber along the walls. After a "few bounces" a migrating neutral would be "pumped" by the plasma.

From this model of neutral interaction with a slab of plasma we conclude that the flux of fast neutrals from the scrapeoff plasma may be something like 1/3 of the slow

744006

Fig. 6.9 Ionizing probability of slow neutral.

neutral flux into the plasma. If the slow neutrals come from the walls as a result of ion and fast neutral bombardment, then the fast neutral flux will be half the ion flux to the walls.

Conduction of Heat in the Scrapeoff Plasma

The scrapeoff plasma thermally connects the discharge volume with the divertor walls and the neutral gas in the exhaust chamber. For a hydrogenic plasma in a magnetic field of several score kilogauss the thermal conductivity parallel to the magnetic field is some twelve orders of magnitude greater than the thermal conductivity perpendicular to the field. Since the ratio of the area of the first wall of the reference design reactor to the cross-sectional area of the scrapeoff flux is only about three orders of magnitude, we conclude that thermally conducted heat will flow predominantly along the field lines. Due to the axisymmetry of the entire configuration of the plasma and magnetic fields, any steady state temperature gradients will exist only in a poloidal sense. However, the magnetic field is predominantly toroidal, and so the component of the thermal gradient parallel to the field will be relatively small. Nevertheless it is the temperature differences as seen along the field lines that are associated with the conduction of heat in the scrapeoff plasma.

The view adopted here is that the thermal connection of the scrapeoff plasma to the discharge volume will have a controlling effect on the boundary conditions for the reacting plasma. In view of the subsonic flow of the scrapeoff plasma, convection of energy via plasma flow along the lines will be neglected. Hence, energy losses from the scrapeoff plasma via recycling to the walls, charge exchange with slow neutrals or radiation will have to be supplied from the discharge volume via thermal conduction if the scrapeoff plasma is to exist. It has been suggested³ that the synchrotron radiation, which will fill the vacuum chamber of the reactor, can provide a significant mechanism for transporting energy into the scrapeoff plasma. However, we will not consider this effect further in this report. Therefore, if we are to have scrapeoff plasma extending into the exhaust chamber it is to the conduction of thermal energy that we look to maintain the scrapeoff plasma above deionization temperatures.

If the scrapeoff plasma terminates on a metal surface a sheath will form to insure ambipolar flow of charge off the ends of the field lines. The electrons will maintain the sheath potential, which in turn gives energy to the ions that fall through the sheath. The escape of the hottest electrons through the sheath will constitute an energy loss from the electrons, which, together with other losses, must be made up by the

conduction of heat to the sheath region if a steady state is to be maintained. Such a view presupposes that the mean free path for electron-electron collision will be short compared to dimensions along the field lines, which are of the order of 10^4 cm. We note that our nominal values for the scrapeoff plasma electron temperature and density, 100 eV and 10^{13}cm^{-3} respectively, yield a mean free path for electron-electron collision of about 500 cm. Furthermore, the mean free path for electrons to transfer energy to ions will be about 67 times longer. This energy transfer distance is comparable to the length of the scrapeoff plasma. However, we anticipate lower temperatures and higher densities near the exhaust chamber. Dropping the temperature to 10 eV and raising the density to 10^{14}cm^{-3} yields a reduction in mean free paths by a factor of 10^3 . With this anticipated cooling of the scrapeoff plasma near the exhaust chamber in mind we proceed under the assumption that the scrapeoff plasma will provide not only thermal conduction along the field, primarily via the electrons, but that the electron and ion temperature are roughly equal.

The scrapeoff plasma may not terminate on a metal surface, but simply cool as it flows down the field lines until its temperature reaches about 1 eV at which time it will deionize. We postulate there should be no heat flow out of the region of deionization. We adopt this scheme of terminating the scrapeoff plasma on the grounds that low plasma temperatures are desirable to reduce particle erosion of surfaces, and we can hereby eliminate the necessity for special collecting plates.

We now have to match the thermal flow problem in the scrapeoff plasma to the energy flow analysis of the reacting plasma. Solutions have been found for the temperature and density profile of the discharge plasma with edge temperatures below 1 keV and total reactor thermal power held at 5000 MW. (See Appendix B for discussion of the method of calculation.) In fact, once the edge temperature is dropped to 1 keV, further reduction of edge temperature alone has little effect on other plasma parameters. Changes in edge density, however, create changes in fuel composition and fuel feed rate and result in differing amounts of thermal energy required to be dissipated by the scrapeoff plasma. Since the losses sustained by the scrapeoff plasma are density dependent, we search for a suitable combination of edge temperature and density such that the desired volume of scrapeoff plasma, terminating by deionization at the entrance to the exhaust chamber, is just adequate to dissipate the required flow of thermal energy from the discharge volume.

We approximate this complex energy flow problem by a steady state, one dimensional model. We consider a cylinder of cross section A and total length L in the

x direction along which heat is conducted with a thermal conductivity K. For K we will use expressions given by Spitzer⁴ for the thermal conductivity parallel to a magnetic field in a fully ionized plasma. The temperature is allowed to vary only along the length of the cylinder. A distribution of thermal energy sinks, S(x), is assumed. These energy sinks model the loss of energy from the scrapeoff plasma via impurity radiation and plasma interaction with walls and neutral particles. Letting Q(x) be the heat flux down the cylinder at a distance, x, along the cylinder, we have by definition of K:

$$Q(x) = -AK \frac{dT}{dx} \quad (6.18)$$

and hence by conservation of energy,

$$\frac{dQ}{dx} = -S(x) \quad (6.19)$$

We take the thermal conductivity to be

$$K = \frac{310}{Z \ln \Lambda} T^{5/2} \text{ watts/eV cm} \quad (6.20)$$

where: T is the electron temperature in eV, $\ln \Lambda$ is the "Coulomb logarithm", and Z is the effective charge of the ions.

If we take S(x) to be a constant, S, we can readily solve Eq. 6.19 for T(x). Let us consider this solution briefly. The first integration of Eq. 6.19 yields:

$$-AaT^{5/2} \frac{dT}{dx} - Q(0) = - \int_0^x S(x') dx' = -Sx \quad (6.21)$$

The second integration yields:

$$T^{7/2}(x) = T^{7/2}(0) + \frac{7}{2a} \left[-x Q(0) + \frac{S}{2} x^2 \right] \quad (6.22)$$

Now Q(0) is the flow of heat into the cylinder at the end $x = 0$. From Eq. 6.21 we see that when

$$x = L = Q(0)/S \quad (6.23)$$

the gradient of the temperature vanishes if the temperature itself is not zero. Thus for a cylinder of length, L , the input of thermal energy has been exactly dissipated by the energy sinks ($S \cdot L$). For values of x greater than L and positive temperatures Eq. 6.21 predicts a change in slope of the temperature to positive values corresponding to the conduction of heat from larger values of x toward the place $x = L$. Substituting Eq. 6.23 into Eq. 6.22 we find:

$$T^{7/2}(L) = T^{7/2}(o) - \frac{7}{4a} \cdot \frac{Q^2(o)}{S} . \quad (6.24)$$

We note that for given values of $Q(o)$ and S , it is possible to choose $T(o)$ so small that $T(L)$ will be negative. This is a non-physical mathematical result. If we require $T(L)$ to be positive on physical grounds, then $T(o)$ must be chosen large enough. Physically, if we started out with a cylinder with losses and with the end at $x = L$ thermally insulated and held at some temperature $T(L)$, then injecting a heat flow at $x = o$ would result in $T(o)$ rising up to the value given by Eq. 6.24. The lower we choose $T(L)$, the lower will be $T(o)$. If in addition we desired to choose the value of L , then we must satisfy the constraints on $Q(o)$ and S given by Eq. 6.23.

For the more general situation of $S(x)$ not being constant, we can expect that for a given input of heat, $Q(o)$, and a given value of $T(o)$ that is large enough, the temperature will monotonically fall to a positive value of $T(L)$ where L is given implicitly by:

$$\int_0^L S(x) dx = Q(o) . \quad (6.25)$$

Furthermore, at $x = L$ the temperature gradient will vanish. As we contemplate lower and lower values of $T(o)$, we expect to find lower and lower values of $T(L)$ until the physically limiting value of $T(L) = 0$ is reached.

To model the energy loss from the scrapeoff plasma we have taken the impurity radiation from the plasma, when the electron temperature, T_e , is greater than 10 eV, to be given by⁵

$$S_r = 1.6 \times 10^{-26} n_e n_r A \text{ watts/cm} . \quad (6.26)$$

Here n_e is the electron density and n_r is the density of radiating ions. We expect n_r to be proportional to n_e and hence the radiated power will be proportional to n_e^2 . For numerical calculations below we take n_r to be given by the argon impurity content of the plasma. The presence of neutral hydrogen as an additional source of line radiation is neglected. In short, the line radiation is assumed to be impurity dominated.

As discussed at the end of the section on pumping assumptions and subsonic flow above, the scrapeoff plasma flow is subsonic and hence the pressure, $n_e T_e$, will be nearly constant. Hence $S_r \sim T_e^{-2}$. For T_e less than 10 eV, S_r is reduced by multiplying the above expression (6.26) by the factor $6y^{1/2}e^{-y}/(1+y)$, where $y \equiv 10/T_e$.

The energy loss via plasma interaction with the walls and neutral particles is modeled by the following relation

$$S_p = k \cdot \frac{3}{2} T_i \Gamma_o / L \quad , \quad (6.27)$$

where Γ_o is the particle throughput, and k is the ratio of the total flux of ions and fast neutrals hitting the walls to Γ_o . We chose $k = 8$ for our numerical estimates below.

Taking $S(x) = S_r + S_p$, Eq. 6.19 is numerically integrated. Two sets of boundary conditions are used that are generated by what will be referred to as solutions A and B. These boundary conditions require some discussion.

Solutions A and B are solutions of the energy flow analysis of the discharge plasma which we will refer to as the Profiles Analysis. (See Appendix B.) The Profiles Analysis requires the specification of the temperature and density of the plasma at the boundary of the discharge region. The analysis then produces, among other things, the required steady state heat flow across the discharge plasma boundary. As mentioned above, the results of the Profiles Analysis are quite insensitive to the chosen boundary temperature provided that the temperature is below 1 keV. Solutions A and B were obtained for a boundary temperature of 500 eV. Hence their ion densities and heat flows at the discharge plasma boundary are deemed suitable boundary quantities for the numerical integration of Eq. 6.19 for all boundary temperatures below 1 keV.

The numerical integration of Eq. 6.19 starts at $x = 0$ with a specified plasma cylinder cross section, ion density and effective charge, heat flow and temperature, $T(0)$. Various values of $T(0)$ are tried until a satisfactory solution is found; namely,

one that produces a vanishing temperature gradient at a positive temperature near 1 eV. Results for boundary conditions provided by solutions A and B are shown in Fig. 6.10. Evidently the boundary conditions of solution A lead to substantially larger values of L than do those of solution B over quite a range of variation of effective charge and plasma cylinder cross section. Since the nominal length of the scrapeoff plasma measured along the magnetic field in the divertor channel is 5×10^4 cm and its cross section perpendicular to the magnetic field is 10^4 cm², solutions A and B appear to straddle a "matching solution" of the Profiles Analysis that would produce values of L equal to the nominal length of the scrapeoff plasma. However, the sensitivity of the length L to the effective charge of the plasma ions, the plasma cross section and to the value taken for S_p through the choice of L in Eq. 6.27 greatly reduces the significance of specifying in detail a "matching solution" of the Profiles Analysis. Our conclusion is that the "straddling" nature of solutions A and B is to be taken as evidence of the feasibility of tuning the operating conditions of the discharge plasma to produce the desired behavior of the scrapeoff plasma. Solution A actually underlies the data presented in Appendix A.

Our model for S_r expressed by Eq. 6.26 results in a sharp increase in radiative losses as the temperature falls, and the electron density increases near the entrance to the exhaust chamber. Calculation indicates that S_r accounts for over 80% of the total losses for the cases considered and that about half of the radiative losses occur in the distance over which the temperature falls to about 0.4 of its initial value at the separatrix. From inspection of Fig. 6.10 it is apparent that about half the radiative losses must, therefore, occur over a path length not over 10^4 cm long. Measured in a poloidal sense, this distance corresponds to about one or two meters just outside the entrance to the exhaust chamber. Thus we may expect something like 75 MW to fall on the walls near the entrance of the exhaust chamber at a density of about 30 watts/cm². This heat load is not excessive.

A criticism of the use of all the one dimensional models used to approximate plasma behavior in this chapter lies in the weakness of the assumption of uniform temperatures across the width of the scrapeoff plasma. Clearly for the scrapeoff plasma to exist at a distance from the separatrix surface energy must be transported or flow within the plasma across magnetic surfaces. The analysis of such energy flow within the plasma has not been attempted here. Some feeling for the effect of constricting the width of the scrapeoff plasma is gained from comparing the curves in Fig. 6.10 for different values of A .

744060

Fig. 6.10 Thermal flow model temperature distribution.

Deionization of the Scrapeoff Plasma

Contemplating the steady state flow of scrapeoff plasma into the divertor, it seems intuitively clear that if its electron temperature drops sufficiently low there will be a region in which the plasma will deionize. It is then of interest to establish the conditions under which this transition will occur. What densities and temperatures for both plasma and neutral gas are associated with such a transition? What processes are important in the deionization region? Our qualitative understanding is as follows.

On the downstream side of the deionization region will be neutral gas and on the upstream side plasma. The material flowing away from the discharge volume would thus undergo a kind of phase change from plasma to neutral gas in passing through the deionization region: electrons "condense" upon the ions and the "heat of fusion" is radiated away. The spatial extent of the deionization region is not evident since it will depend on the many processes involved in the deionization process. We can imagine, however, as one moves down the magnetic field from the fully ionized region toward the neutral gas an increasing number of neutrals are encountered that are being excited, ionized and undergoing charge exchange. The energy loss from the plasma associated with these processes implies a reduction in plasma temperature as one proceeds farther down the field. To keep the electrons from rapidly running out of the plasma along the field lines, electric fields must be established to form a kind of "sheath" at the end of the plasma. These fields may arise as follows: when the electron temperature has become low enough the electrons lose their ability to ionize the neutrals they encounter. Hence at some low electron temperature there will be a flow of electrons from the plasma into neutral gas where they cannot generate new electrons available to return to the plasma, but where they continue to lose energy by collisional excitation of atoms. These electrons will tend to form a cloud outside the plasma and hence will set up an electric field that acts to retard further electrons from the plasma and to attract ions out of the plasma. Ions that enter the electron cloud will tend to undergo volume recombination with the cold electrons in the presence of neutrals. Thus there is a continual conversion of plasma into neutral gas.

The neutral gas at the boundary of the deionization region will contain neutrals flowing downstream away from the deionization region and flowing upstream into the deionization region. The upstream flow must come principally from neutral-neutral collisions downstream. The upstream flow passes through the "sheath" and enters the more energetic plasma to become eventually ionized. The downstream flow, on the other hand, must come principally from recombined ions or charge exchange events upstream and constitutes the chief source of energy to be eventually thermalized in the neutral gas.

The flow of energy through the deionization region is complex in detail. Qualitatively, plasma carries energy by particle flow into the deionization region, and neutral gas carries energy by particle flow out. In addition, energy can leave the deionization region via: radiation from excitation of neutrals and the recombination of ions and electrons; particle bombardment of adjacent walls; and heat conduction. In view of the poor heat conduction of neutral gas compared to that of electrons parallel to a magnetic field we expect the "sheath" part of the deionization region to provide good thermal insulation of the plasma. In fact, for the thermal flow analysis of the previous section and for what follows below we assume the "sheath" to constitute a perfect thermal insulator.

A further qualitative aspect of the deionization region is the presence of an effective "body force" on the plasma. This force is due to the magnetic field interacting with the electric currents that arise by virtue of the gradient in plasma pressure brought about by the deionization processes.

The flow of plasma into the divertor and the neutral gas into the exhaust chamber is a problem in gas dynamics. An analogous gas flow problem would be that of the steady flame front in a gas jet or the fixed condensation zone in a nozzle. The requirement that must be satisfied is that the deionization region remain fixed in space while material flows through it. If we specify the plasma properties upstream from the deionization region, what can we then say about the properties of the neutral gas downstream? To proceed with the analysis we approximate the deionization region with a two dimensional model to which we apply the conservation laws of mass, momentum and energy flow.

The deionization region is taken to be a slab that contains the separatrix surface, here treated as a plane. Fig. 6.11 shows the assumed geometry. The x, y plane corresponds to the separatrix surface. The x axis is chosen to correspond to the toroidal sense of direction in the reference reactor. By virtue of the assumed axisymmetry for the reference reactor, all flow properties are taken to be independent of x . As indicated in Fig. 6.11, the plasma is taken to flow into the deionization region with its flow velocity, $\underline{v}^p$, parallel to the magnetic field. Both the flow vector and the magnetic field change direction in passing through the deionization region. The flow velocity of the neutral gas, $\underline{v}^g$, is not assumed to be parallel to the magnetic field. We make the assumption that the magnetic field has no z -component and that all flow properties are independent of z . Finally, there will be a current flowing in the z direction in the deionizing region that arises by virtue of the gradient of the plasma pressure.

We treat the neutral gas and each component of the plasma as a fluid. The mass flow vector for each fluid is:

$$J_k = n m v_k \quad (6.28)$$

where n , m and v_k are the particle density, particle mass and the k th component of the flow velocity. The particle momentum flow tensor for each fluid is taken to be:

$$P_{ik} = n m v_i v_k + n T \delta_{ik} \quad (6.29)$$

where T is the temperature in units of energy of the fluid and

$$\begin{aligned} \delta_{ik} &= 1 \quad \text{if } i = k \\ &= 0 \quad \text{if } i \neq k \end{aligned}$$

Momentum is also transferred to the plasma by the electric currents flowing across magnetic fields. Such forces can be treated through the electromagnetic stress tensor:

$$R_{ik} = \frac{1}{4\pi} \left(\frac{E^2 + B^2}{2} \delta_{ik} - (E_i E_k + B_i B_k) \right) \quad (6.30)$$

where $\underline{E}$ and $\underline{B}$ are the electric and magnetic fields.

Consider now a cylinder of unit cross section in the deionization region lying along the y -axis and bounded by the plasma control surface and the gas control surface. The plasma control surface lies wholly in the plasma, and the gas control surface lies wholly in the neutral gas. Equating the mass flow into the cylinder across the plasma control surface to the mass flow out of the cylinder across the gas control surface we have:

$$n_i m_i v_y^i + n_e m_e v_y^e = n_g m_g v_y^g \quad (6.31)$$

where the labels i, e and g refer to the ion, electron and neutral gas fluids respectively. Since there are no "body forces" acting in the deionization region other than electromagnetic forces, the divergence of the total momentum flow tensor must vanish in the deionization region. Thus

$$\frac{\partial}{\partial x} (P_{kx} + R_{kx}) + \frac{\partial}{\partial y} (P_{ky} + R_{ky}) + \frac{\partial}{\partial z} (P_{kz} + R_{kz}) = 0 \quad (6.32)$$

where k can become x , y or z . Since the tensors P and R are assumed to depend only on y , the first and third terms in Eq. 6.32 vanish. Therefore the quantity $(P_{ky} + R_{ky})$ is preserved in passing from the plasma control surface to the gas control surface and we have:

$$\begin{aligned} n_i m_i (v_y^i)^2 + n_e m_e (v_y^e)^2 + n_i T_i + n_e T_e + \frac{(B^p)^2}{8\pi} (\cos^2 \theta - \sin^2 \theta) \\ = n_g m_g (v_y^g)^2 + n_g T_g + \frac{1}{4\pi} \left[\frac{(B^g)^2}{2} - (B_y^g)^2 \right] \end{aligned} \quad (6.33)$$

and

$$n_i m_i v_x^i v_y^i + n_e m_e v_x^e v_y^e - \frac{(B^p)^2}{4\pi} \sin \theta \cos \theta = n_g m_g v_x^g v_y^g - \frac{1}{4\pi} B_x^g B_y^g \quad (6.34)$$

Here the left-hand side quantities are evaluated at the plasma control surface and the right-hand side quantities are evaluated at the gas control surface. The electric field does not appear in Eqs. 6.33 and 6.34 because we assume it vanishes outside the deionizing region and hence will be zero at the plasma and gas control surfaces. Finally, we note that $(P_{zy} + R_{zy})$ vanishes when evaluated at the plasma control surface. Hence it will vanish when evaluated at the gas control surface. Thus

$$(P_{zy} + R_{zy})_{\text{gas}} = n_g m_g v_z^g v_y^g = 0$$

Hence we conclude that $v_z^g = 0$. Thus the entire flow of both plasma and neutral gas is parallel to the x, y plane.

We now consider the energy flow through the deionization region. Let Q_p be the energy flowing across the plasma control surface, Q_g be the energy flowing across the gas control surface and let Q_L be the rate of energy loss from the entire deionization region between the control surfaces. By the conservation of energy we have:

$$Q_p = Q_g + Q_L \quad (6.35)$$

Q_p will consist of the transported particle kinetic energy plus the flow of potential energy represented by the ionized state of the plasma plus any heat flow. It is important to count the potential energy since it is this energy that is converted into recombination radiation. If A is the area of the plasma control surface across which the plasma flows, then we take for Q_p the following expression:

$$Q_p = A \left(\left[n_i \frac{1}{2} m_i (v^i)^2 + \frac{5}{2} n_i T_i \right] v_y^i + \left[n_e \frac{1}{2} m_e (v^e)^2 + \frac{5}{2} n_e T_e \right] v_y^e + n_e v_y^e U + q_y \right)$$

where U is the average ionization potential per electron in the plasma, and q_y is the heat flow across unit area of the plasma control surface.

For Q_g we take:

$$Q_g = A \left[n_g \frac{1}{2} m_g (v^g)^2 + \frac{5}{2} n_g T_g \right] v_y^g$$

Note, we assume there is no heat flow across the gas control surface. We will discuss Q_L below.

We assume that at the plasma control surface the magnetic field and the plasma properties are given. The problem is to find the six unknowns at the gas control surface n_g, T_g, v_y^g and B_x^g . The four equations Eqs. 6.31, 6.33, 6.34 and 6.35 do not suffice. Two more equations are needed. One additional statement is the observation that the y component of the magnetic field will be continuous across the deionization region. Hence $B_y^g = B_y^p$. A second statement is obtained by the definition of a new parameter. We consider the neutral gas pressure, $P_g \equiv n_g T_g$, to be specified. We now have sufficient equations for the unknowns.

We assume the plasma is electrically quasi-neutral and flows in an ambipolar fashion. Hence $n_e = Z n_i$ and $v_y^e = v_y^i$. Since $m_e \ll m_i$ we also consider $m_g = m_i$ and identify v_y^i with the plasma flow velocity, v_y^p . Finally we assume a "low beta" plasma, that is, the plasma pressure is small compared to the magnetic pressure. Hence the presence of the plasma will not affect the magnetic field very much. Therefore we define ϵ by the relation, $B_x^g = B_x^p (1 + \epsilon)$, and take $\epsilon \ll 1$. With the above assumptions and some algebra we can replace Eqs. 6.31, 6.33, 6.34, and 6.35 with the following four equations:

$$v^g \sin \beta = T_g \frac{\Gamma}{A P_g} \quad , \quad (6.36)$$

$$v^g \cos \beta = v^i \cos \theta + \varepsilon \frac{(B^p)^2}{4\pi} \frac{A \sin \theta \cos \theta}{\Gamma m_i} \quad , \quad (6.37)$$

$$M^2 = \mu^2 \left(\frac{\chi^2}{\tau \cos^2 \theta} - \frac{2\chi}{\phi} \tan^2 \theta + \frac{\tau}{\phi^2} \tan^2 \theta \right) \quad , \quad (6.38)$$

$$M^2 + \frac{5}{2} = \frac{1}{\tau} \left(\mu^2 + \frac{5}{2} + \frac{Z U}{(T_i + Z T_e)} + \frac{q_v A - Q_L}{\Gamma (T_i + Z T_e)} \right) \quad , \quad (6.39)$$

where we have defined the dimensionless quantities:

$$\begin{aligned} M^2 &\equiv \frac{1}{2} m_g (v^g)^2 / T_g \quad , \\ \mu^2 &\equiv \frac{1}{2} m_i (v^i)^2 / (T_i + Z T_e) \quad , \\ \tau &\equiv T_g / (T_i + Z T_e) \quad , \\ \phi &\equiv P_g / [n_i (T_i + Z T_e)] \quad , \\ \chi &\equiv 1 + (1 - \phi) / (2 \mu^2) \quad , \end{aligned}$$

and have made use of the particle throughput, Γ , with the help of the relation $\Gamma = n_i v^i A \sin \theta$. M is essentially the Mach number for the neutral gas.

We note that the unknowns v^g and T^g are embedded in the quantities M and τ that are determined by Eqs. 6.38 and 6.39 alone. Once having found v^g and T^g , Eq. 6.36 serves to determine β and subsequently Eq. 6.37 serves to determine ε . Hence we turn to the solution of Eqs. 6.38 and 6.39 and must now consider the nature of Q_L , the rate of energy loss from the entire deionization region between the control surfaces.

We start the estimation of Q_L by modeling the flow of ions and neutrals through the deionization region in the following manner. The particle throughput of ions crossing the plasma control surface is composed of counter streaming fluxes of ions. The sources of the ion flux going upstream are backscattering of ions and ionization and charge exchange of neutrals. Similarly the particle throughput of neutrals crossing the gas control surface is composed of counter streaming fluxes of neutrals. The sources of the neutral flux going downstream are backscattering of neutrals and deionization and charge exchange of ions. In addition particles may interact with the walls bounding the deionization region, but no net collection of particles by the walls is assumed. Fig.

6.12 shows a schema of this model. In particular Γ_{id} represents the incoming flux of ions that are deionized and Γ_{ni} represents the incoming flux of neutrals that are ionized. Clearly the particle throughput, Γ , must equal the difference of the deionized and ionized fluxes, that is,

$$\Gamma = \Gamma_{id} - \Gamma_{ni} \quad (6.40)$$

The losses from the deionization region will be in the form of deionization radiation, Q_{dr} , collisionally excited radiation, Q_{cr} , and energy transported to the walls by particle interaction, Q_w . Since there are Z electrons that must recombine with an ion to produce a neutral, we take

$$Q_{dr} = Z U \Gamma_{id} \quad (6.41)$$

Collisional excitation takes place in the process of ionizing neutrals and in the process of electrons losing energy before recombining. We take

$$Q_{cr} = \alpha_c U Z \Gamma_{ni} + \alpha_e T_e Z \Gamma_{id} \quad (6.42)$$

Here α_c and α_e are multiplicatives of the order unity. For the wall interaction we take

$$Q_w = \alpha_w T_i \Gamma_n \quad (6.43)$$

This expression for Q_w reflects the thought that the particle-wall interaction in the deionization region will be dominated by charge exchange neutrals which, in turn, are a consequence of the flux of neutrals, Γ_n , injected into the region. The value of α_w is quite uncertain. Since we expect Q_w to be less than the net energy transported into the deionization region by the ions, if $\Gamma_n \gg \Gamma$, we expect $\alpha_w \ll 1$. On the other hand if $\Gamma_n < \Gamma$, then $\alpha_w \sim 1$ would be possible.

Summing up we have

744054

Fig. 6.12 Particle flow for deionization model.

$$\begin{aligned}
 Q_L &= Q_{dr} + Q_{cr} + Q_w \\
 &= (U + \alpha_e T_e) Z \Gamma + \left(\alpha_i [U(1 + \alpha_c) + \alpha_e T_e] Z + \alpha_w T_i \right) \Gamma_n
 \end{aligned}
 \tag{6.44}$$

where we have used Eq. 6.40 to eliminate Γ_{id} and have set Γ_{ni} equal to $\alpha_i \Gamma_n$ where $0 < \alpha_i < 1$. Perhaps the most significant aspect of this expression for Q_L is its dependence on Γ and Γ_n .

We now assume the velocity distribution of the neutral gas at the gas control surface to be well approximated by a displaced Maxwellian distribution such that its average velocity is just equal to the neutral gas flow velocity, $\underline{v}^g$. Hence we can replace Γ_n with the expression

$$\Gamma_n = \Gamma \cdot \frac{\alpha_o(M)}{(2\pi^{1/2}M)}$$

where

$$\alpha_o(M) \equiv e^{-M^2} - \pi^{1/2} M [1 - \text{erf}(M)]$$

and $\text{erf}(M)$ is the error function of M . $\alpha_o(M)$ is plotted in Fig. 6.13. Substituting the above expressions for Q_L and Γ_n into Eq. 6.39 we have

$$M^2 + \frac{5}{2} = \frac{1}{\tau} \left(\mu^2 + \frac{5}{2} - \lambda - \frac{\Lambda \alpha_o(M)}{M} \right) \tag{6.45}$$

where:

$$\lambda \equiv (\alpha_e T_e Z \Gamma - q_y A) / [\Gamma(T_i + Z T_e)]$$

and

$$\Lambda \equiv \frac{\alpha_i [U(1 + \alpha_c) + \alpha_e T_e] Z + \alpha_w T_i}{2 \pi^{1/2} (T_i + Z T_e)}$$

We note that the term $ZU / (T_i + Z T_e)$ in Eq. 6.39, which comes from the flow of potential energy, has been cancelled by the first term of Q_L , $UZ\Gamma$, which represents the recombination radiation.

744063

Fig. 6.13 $\alpha_0(M)$ versus M .

Equations 6.45 and 6.38 are now to be solved for M and τ . With Eq. 6.45 one can plot τ as a function of M^2 for a given choice of plasma parameters. With Eq. 6.38 we plot M^2 as a function of τ . We note the parameter ϕ , containing the neutral gas pressure P_g , enters only in Eq. 6.38 in which it serves as a parameter to generate a family of curves. The intersection of these two sets of curves provides the values of M and τ that simultaneously satisfy Eqs. 6.38 and 6.45. A plot of such curves is shown in Fig. 6.14.

For the curves of Fig. 6.13 the following parameters were used: $\Gamma = 2 \times 10^{22}$ particles/sec, $A = 6.42 \times 10^5 \text{ cm}^2$, $B^p = 86.9 \times 10^3$ gauss, $\theta = 1.35$ degrees, $T_i = T_e = 1 \text{ eV}$, $n_i = 6.2 \times 10^{14} \text{ cm}^{-3}$, $Z = 1$, and $m_i = 2.5$ atomic mass units. These values model the reference reactor at the entrance to the exhaust chamber (Fig. 6.2) with A accommodating the entire scrapeoff flux. T_e cannot be much above 1 eV for the plasma to deionize. The ion density n_i is approximately two orders of magnitude larger than the ion density at the edge of the reactor discharge volume used in solution A of the Profiles Analysis mentioned in the above section on conduction of heat in the scrapeoff plasma.

From the above numbers we calculate $\mu = 1.75 \times 10^{-3}$, which indicates very subsonic plasma flow. Since $\mu \approx 1/A$ a wide range of A can be assumed without increasing the flow to sonic conditions. Furthermore, this value of μ produces very large values of χ unless ϕ is very close to unity. For example, the largest value of χ used in plotting Eq. 6.38 in Fig. 6.14 is $\chi = 600$, for which $(1 - \phi) = 0.00367$.

Equation 6.45 is plotted in curves a,b,c and d of Fig. 6.14. For curves a,b and c we have rendered $\ell = 0$ by assuming $q_y A = \alpha_e T_e Z \Gamma$. This assumption equates the heat conducted into the deionization region to the collisionally excited radiation produced by a flux of $Z\Gamma$ electrons. This is just the flux of electrons necessary to neutralize the throughput of ions, Γ . In evaluating Λ we have used $U = 13.6 \text{ eV}$, $\alpha_e = \alpha_c$ and $\alpha_w = 1$ for all cases. The particular choices of α_i and α_c for curves a,b,c and d are indicated in Fig. 6.14.

The effect of any particular value given to the α 's manifests itself in the resulting values of Λ and ℓ . Now Λ is proportional to the losses associated with Γ_n . Since Γ_n decreases monotonically with increasing M , for large values of M curves of Eq. 6.45 with different values of Λ will approach the same limiting curve determined by the value of ℓ . Thus curves a,b and c all approach the dashed curve labelled " $\ell=0$." Curve d, though having nearly the same value of Λ as curve b, approaches the limiting curve labelled " $\ell=1$ ". The value of ℓ reflects the difference between the

744447

Fig. 6.14 M versus τ from Eqs. 6.38 and 6.45.

collisionally-excited radiation induced by the flow of ZT electrons and the heat conducted into the deionization region, essentially by the electrons. If this heat cannot be entirely dissipated by radiation, it must go into heating up cold particles, primarily cold electrons; in which case one might expect the deionization region to expand until the radiant losses did account for all the heat flow. Hence we consider negative values of ℓ as unphysical. In the limit of no heat conduction into the deionization region ℓ will be limited by the circumstance that the radiant losses cannot exceed the energy brought in by the electrons. Hence ℓ should be less than $5/2$.

The major conclusion to be drawn from Fig. 6.14 is that for any curve of Eq. 6.45 we can find a curve of Eq. 6.38 with $\chi < 600$ such that the intersection of the two curves occurs for values of $M > 1$. Values of M greater than unity imply that the neutral gas leaves the deionization region in a state of supersonic flow. To achieve such values of χ the neutral gas pressure need be only slightly less than the plasma pressure, as mentioned above, which for our choice of density and temperature amounts to some 1.5 torr. In a manner suggested by "choked" flow in a nozzle, if the gas pressure in the exhaust chamber is reduced below the value of neutral gas pressure required to produce supersonic flow, we would expect the flow of plasma into the deionization region to be unaffected.

For low neutral gas pressures and hence very large values of χ there are no simultaneous solutions of Eqs. 6.38 and 6.45. This circumstance indicates the limits of validity of the "one dimensional" analysis. Physically we would expect low gas pressures in the exhaust chamber to result in complicated, oblique expansion shock patterns in the flow of the neutral gas analogous to the exit flow of an overexpanded Laval nozzle. However, the attainment of sonic flow of the neutral gas at the gas control surface results in the decoupling of the plasma flow from the state of the gas in the exhaust chamber.

The Vacuum System

According to the estimate of the preceeding section the scrapeoff plasma pressure will be about one torr and to insure the proper particle throughput of 10^{22} molecules/sec, the neutral gas pressure in the exhaust chamber need be only slightly less than one torr. Reduction of the exhaust chamber pressure to well below one torr will not affect the throughput because of the condition of sonic flow at the entrance to the exhaust chamber. From these considerations, the pressure in the exhaust chamber is not critical, and the pumping requirements are trivial.

The required pumping speed is really set by the specification of the neutral gas density of 4.6×10^{11} molecules/cm³ in the main vacuum chamber at the onset of the reignition phase of the operating cycle of the reactor. (See Chapter 5 for discussion of the operating cycle.) The situation is as follows. At the end of the current interruption phase, the plasma in the discharge volume has been converted into a neutral gas at a molecular density of 10^{13} cm⁻³ and a temperature of 600 K. This gas fills the main vacuum chamber at a pressure of 4.5×10^{-4} torr. We require that at this time the gas pressure in the exhaust chamber equal the pressure in the main vacuum chamber. The operating cycle allows 30 seconds of pumping on the combined volumes to reduce the gas density in the main vacuum chamber by a factor of about 22 in preparation for the reignition phase. The pumping speed must be adequate for this task and at the same time handle a particle flow of some 10^{22} molecules/sec. during the long burning phase of the operating cycle.

Assuming the exhaust chamber pressure to be 4.5×10^{-4} torr during the burning phase, the particle throughput of 10^{22} molecules/sec requires a pumping speed of some 6.7×10^5 liters/sec. This speed should also be adequate to produce the required reduction in gas density in 30 seconds of pumping. The total volume to be pumped amounts to some 5×10^6 liters, and its conductance is judged to be determined primarily by the divertor channels. We treat each divertor channel as a cylinder 80×3000 cm² by 800 cm long. The channels pump in parallel with a combined conductance somewhere between 3.8×10^6 liters/sec and 28×10^6 liters/sec. The first number assumes molecular flow conditions prevail, and the second number assumes viscous flow. For either assumption the conductance far exceeds 6.7×10^5 liters/sec. Hence we neglect the conductance of the divertor channels in estimating the pumpdown of the reactor volume.

The reduction in gas density in a volume, V, that is pumped with a speed, S, for a time, t, is given by $\exp(-St/V)$. Hence in 30 seconds at a speed of 6.7×10^5 liters/sec. we expect a reduction in density in the main vacuum chamber by a factor of 57. This reduction is more than is needed. Thus some neutral fuel gas may be bled into the reactor during this pumpdown time to insure the proper gas density at the onset of the reignition phase.

By the above considerations we judge the pumping speed of 6.7×10^5 liters/sec. to be an adequate requirement for the reference design model reactor. We achieve this speed with 96 vacuum pumps evenly divided between the top and bottom vacuum manifolds and equispaced around the reactor. The vacuum manifolds are connected to the exhaust chambers through ducts 6 meters long and 1.5×3.0 square meters in cross section that pass through the gaps between the toroidal field coils.

Hot neutral gas entering the exhaust chamber will be cooled by contact with the walls of the exhaust chamber although some cooling baffles may be required.

We propose to use mercury rather than oil diffusion pumps since mercury will not be affected by the 18.6 keV beta rays of the tritium. The diffusion pumps will be backed up by mercury ejector pumps. The ejector pumps will work into about 100 torr fore pressure that is required for the multistage compressors that follow in the fuel processing system described in Chapter 14. Pumpdowns from atmospheric pressure will be done by special forepumps.

Performance of the Divertor

From the above description of the scrapeoff plasma it is not clear how well the poloidal field divertor will fulfill the desired requirements outlined in the above section "Functions of a Divertor". As indicated in Fig. 6.10, the plasma temperature on the separatrix at the edge of the discharge volume may be about 300 eV, which is certainly far above any material wall temperature, but is still far short of reacting plasma temperatures. Thus the divertor described here may be said to do a poor job of permitting high edge temperatures for the reacting plasma.

The divertor is expected to keep the density of neutral atoms surrounding the discharge volume extremely small and hence to render negligible charge exchange with the reacting plasma. This expectation rests on two reasons. First, the scrapeoff plasma will be a good "pump" for neutrals. Second, the primary source of neutrals will be the divertor channel walls. Only a small fraction of these neutrals are expected to emerge from the divertor channels. The other possible source of neutrals in the discharge volume is the plasma that diffuses across the limiting flux surface (such as determined by point A in Fig. 6.2) and strikes the radiation shield surrounding the discharge volume (see Fig. 6.4). We shall call this plasma the outer scrapeoff plasma and claim that it will not interact appreciably with the radiation shield. This plasma will be collected for the most part near the entrance to the divertor channels, and the resulting neutrals ionized by other parts of the scrapeoff plasma. In effect the outer scrapeoff plasma undergoes very efficient collection with a short path along the field lines and with little or no source of ions within its volume.

The manner in which the density of the outer scrapeoff plasma is expected to fall off with distance from the limiting flux surface can be approximated by the same analysis used to discuss the plasma in the divertor channel. Thus we may use Eq. 6.9, but with its right-hand side set equal to zero implying no volumetric sources. The

solution of this equation is straightforward. We have:

$$n(u) = \frac{\lambda}{D} J(o) \left(A \cosh u - \sinh u \right),$$

where

$$u = y/\lambda,$$

$$A = (\sinh u_1 + \frac{D}{2\lambda\bar{v}_y} \cosh u_1) / (\cosh u_1 + \frac{D}{2\lambda\bar{v}_y} \sinh u_1)$$

and where $J(o)$ is the diffusion current density at the boundary of the limiting flux surface. The quantity u_1 is the measure of how far the wall (in this case the radiation shield) is from the limiting flux surface. We also find the flux, $J(u_1)$, of the outer scrapeoff plasma to the wall is given by

$$J(u_1) = J(o)e^{-u_1} \quad (6.46)$$

where we have assumed u_1 to be large enough to set $\cosh u_1 = \sinh u_1$. To evaluate λ we take $L = 10^4$ cm and $g = 1$, the values of D and $\bar{v}_x$ remaining unchanged from our previous discussion of the plasma in the divertor channel. Thus we have $\lambda = 4.4$ cm, a factor of 10 less than our previous estimate. Hence $u_1 = 50/4.4 \approx 11$. Thus $J(u_1)/J(o) \approx 1.6 \times 10^{-5}$. The significance of this ratio is that the bombardment of the radiation shield by the outer scrapeoff plasma is expected to be no more than 10^{-5} times the plasma bombardment of the divertor channel walls. Since the flux to the divertor channel walls was estimated at 2×10^{16} ions/sec cm², the flux to the radiation shield is estimated to be less than 2×10^{11} ions/sec cm². This figure does not constitute an appreciable bombardment.

It is difficult to estimate the efficacy of the divertor to keep the impurity atoms that have been sputtered from the divertor channel walls from entering the reacting plasma. The classical expectation is for impurity ions of higher charge state than the bulk of the plasma ions to diffuse across the magnetic field toward the high pressure region of the plasma. Furthermore, the spatial distribution of the impurities tends to become very peaked relative to the bulk plasma. If $n_p(r)$ is the density of a hydrogenic plasma, then the equilibrium density, $n_z(r)$, of impurity ions of charge, Z , is proportional to $(n_p)^Z$. The implication is that the relative concentration of impurities at the separatrix around the discharge volume must be kept extremely small compared to the relative concentration of impurities in the discharge volume itself. The difficulty of this task is illustrated by the following example.

Consider a cylindrical hydrogenic plasma of radius a with a density distribution given by

$$\begin{aligned}
 n_p(r) &= n_p(o)(1-k r^2/a^2) \\
 &= \frac{n_p(a)}{1-k} \cdot (1-k r^2/a^2)
 \end{aligned}$$

Then the average impurity density in equilibrium with this plasma distribution is

$$\begin{aligned}
 \langle n_Z \rangle &\equiv \frac{2}{a^2} \int_0^a n_Z(r) r dr \\
 &= \langle n_p \rangle \cdot \frac{n_Z(a)}{n_p(a)} \cdot \frac{2}{Z+1} \cdot \frac{1}{(1-k)^{Z-1}} \cdot \left(\frac{1-(1-k)^{Z+1}}{1-(1-k)^2} \right)
 \end{aligned} \tag{6.47}$$

Hence for $n_p(a) = 0.2 n_p(o)$ and for an iron impurity with $Z = 26$ we find that

$$\frac{n_Z(a)}{n_p(a)} = 4 \times 10^{-17} \frac{\langle n_Z \rangle}{\langle n_p \rangle};$$

that is, the relative impurity concentration at the edge of the plasma is an impossibly low figure to achieve even for $\langle n_Z \rangle / \langle n_p \rangle \sim O(1)$. For $n_p(a) = 0.8 n_p(o)$, a rather flat density distribution, we find:

$$\frac{n_Z(a)}{n_p(a)} = 0.017 \frac{\langle n_Z \rangle}{\langle n_p \rangle}$$

Thus, for example, if $\langle n_Z \rangle / \langle n_p \rangle \approx 10^{-3}$, then the divertor must keep $n_Z(a)/n_p(a) \approx 10^{-5}$, a requirement that begins to seem possible.

The above argument suggests that it will be impossible for a divertor to maintain low impurity concentrations in a plasma with a central density much different from the density at the separatrix. However, the argument does not indicate the time scale required to build up the impurity concentration. We can estimate this time scale as follows. According to our model of the poloidal field divertor the plasma will interact with the walls primarily in the divertor channel. The total plasma hitting the wall is taken to be $(J_w/J_o)\Gamma_o$, where (J_w/J_o) is the recycling ratio and can be estimated from Fig. 6.6; Γ_o is the particle throughput. Let γ be the ratio of impurity atoms entering the scrapeoff plasma per plasma ion hitting the wall. Let a fraction, α , of these impurity atoms find their way out of the divertor channel and cross the separatrix into the reacting plasma. Then the number of impurity atoms, N_Z , that enter the reacting plasma in a time t is given by:

$$N_Z = \Gamma_o (J_w/J_o) \gamma \alpha t \tag{6.48}$$

In terms of the total number of plasma ions, N , in the reacting plasma we can define a particle confinement time, τ , by writing $\Gamma_0 = N/\tau$. Thus we have:

$$t/\tau = (N_Z/N) \cdot (J_0/J_w)/(\alpha\gamma).$$

Thus for $N_Z/N = 10^2$, $(J_w/J_0) = 10$ and $\lambda = 10^{-3}$ we have

$$t/\tau = 1/\alpha.$$

For the reference design reactor discussed in this study we have $\tau = 3.8$ seconds and desire $t = 5760$ seconds. Hence in this example we would require $\alpha = 0.6 \times 10^{-3}$. This value is not an impossible number to hope for. To make a meaningful estimate of α requires a more refined analysis of the scrapeoff plasma than has been done so far. Electric fields associated with the thermal gradients and the deionization of the scrapeoff plasma may act to inhibit the flow of impurities toward the reacting plasma. A more detailed study, both theoretical and experimental, of the flow of the scrapeoff plasma is certainly required.

References

- ¹L. Spitzer, Jr., The Stellarator Concept, Phys. Fluids 1, 253 (1958).
- ²B. Lehnert, Screening of a High-Density Plasma from Neutral Gas Penetration, Nuclear Fusion 8, 173 (1968).
- ³H. K. Forsen and J. C. Sproult, The Use of Synchrotron Radiation to Provide Ionization of Wall-Originated Impurities in a Thermonuclear Reactor, Nuclear Fusion 12, 126 (1972).
- ⁴L. Spitzer, Jr., Physics of Fully Ionized Gases, 2nd ed. (Interscience Publishers, New York, 1962).
- ⁵E. Hinnov, On Multiple Ionization in High-Temperature Plasma, Princeton Plasma Physics Laboratory Report MATT-777 (1970).

Chapter 7

Fuel Injection

S. L. Gralnick

Introduction

The long pulse mode of tokamak reactor operation requires the maintenance of a quasi-steady fuel ion density profile (constant in time for periods much longer than particle containment times). This is accomplished by injecting an appropriate mixture of deuterium, tritium and impurity additive (argon) atoms to replace those that are consumed by fusion reactions and those lost by diffusion out of the system. To do this we have chosen the technique of high velocity ballistic injection of solidified fuel. Although deep penetration of the reactor plasma is a formidable technical task, it is within the realm of plausibility. Alternative, yet to be explored, schemes relying on edge-deposited fueling may relieve this problem. It is our feeling that pellet injection has two advantages over its principal competitor, high energy neutral beam injection: less energy is required to achieve the same fuel mass penetration, and the pellet evaporation process inhibits the formation of energetic charge-exchange neutrals, which are likely to be a major problem in fueling schemes relying on neutral atom injection.

The reactor profiles code (Appendix B) does not calculate mass diffusion for an arbitrary distribution of sources and sinks and an arbitrary dependence of the diffusion coefficient. Rather, a model based on a constant diffusion coefficient chosen to give several hundred Bohm-times confinement and based on a source function (ions/cm³-sec deposited) that is constant across the plasma is used. The solution of the diffusion equation under these assumptions is parabolic, and this density profile along with the electron temperature profile found by the profiles code provides a target plasma for the injection problem. In an actual device the steady state density profile will depend on the actual distribution of sources, and it is highly unlikely that the source distribution may be tailored at will. The purpose of this chapter is primarily to review the problem of injection, the penetration of a target plasma to a specified depth with a specified mass of fuel. We have not attempted to meet the exact fuel ion requirements of the profiles code. Comparisons of the fuel deposition profile generated here and the fuel consumption profile of the reactor code will, however, be provided.

The fuel requirements in a tokamak fusion reactor, characterized by a small fractional burn, are principally determined by the rate at which particles diffuse out of the system. The consumption due to fusion reactions is less than 10% of this amount. In

a cylindrical plasma based on a uniform density source, the volumetric requirement is a quadratic function of the radius due to the r^2 dependence of the cross-sectional area. If the small requirement due to fusion reactions is ignored the ideal fuel requirement profile would be

$$F = S(2\pi R)(\pi r^2) \quad (7.1)$$

where R is the major radius of the torus, and S is the density source function. Since the fuel deposition resulting from ballistic injection of solid pellets will tend to be peaked at larger values of r , this is a convenient demand curve to attempt to satisfy. An additional factor that must be considered in designing a fuel injection system is the outward shift of the mass center of the plasma. It should be emphasized that the reactor profiles code is based on a cylindrical model. A first order calculation of the shift has been performed based on the equilibrium theory of Greene et al.¹ The resulting shifts of the magnetic surfaces (see Chapter 4) are quite large due to the larger values of β needed in an economic power reactor, and we infer from this that the maximum penetration depth will be considerably smaller than the nominal minor radius of the plasma.

In what follows we shall present an approximate treatment of the interaction of a solid pellet of fuel with the reactor plasma based on the recent work of Gralnick.² The effects of the magnetic field discussed by Rose³ and of possible electrostatic shielding treated in the very early work by Tonks⁴ that are not included in Gralnick's treatment will be discussed here in a brief, qualitative manner. Based on this analysis and the reactor profile we are able to determine fuel pellet diameters and required injection velocities. With this information in hand we then proceed to "design" a fuel injection system for the Princeton Reference Design Reactor.

Interaction of Fuel Pellet with Reactor Plasma

We shall consider the passage of a pellet of "evaporating" material through a slab of uniform density and temperature plasma. (The term evaporation as used here refers to the surface ionization of the fuel pellet as described in Ref. 2). It is assumed that the initial pellet radius and velocity are known, that the thickness of the slab, Δ , is large compared to the initial pellet radius and that the characteristics of the reactor plasma do not change appreciably over a length, Δ . We wish to determine the pellet radius and velocity when it leaves the slab and the time of flight through the slab if the slab is totally penetrated; and we wish to determine the amount of fuel mass that is deposited in the slab.

$$\Delta m = \frac{4}{3} \pi \rho_s (r_i^3 - r_f^3) \quad (7.2)$$

r_i and r_f are the initial and final radii of the pellet and ρ_s is the mass density. The several fuel constituents are assumed to form an homogeneous solid material of density,

$$\rho_s = \rho_D \left(f_T \frac{m_T}{m_D} + f_D + f_A \frac{m_A}{m_D} \right) \quad (7.3)$$

For the fuel composition used in the reference design (see Appendix A) and for a density of solid deuterium⁵ of 0.18 g/cm³,

$$\rho_s = .379 \text{ gm/cm}^3 \quad (7.4)$$

The desired information may be obtained from the solution of the equation of motion of the pellet

$$m_p(t) \frac{dU_p}{dt} = - C_D \cdot (1/2 \rho_* U_p^2) \cdot A_p(t) \quad (7.5)$$

where m_p is the pellet mass, U_p is its velocity, A_p is the projected cross section normal to the direction of motion, C_D is the drag coefficient and ρ_* is the average plasma mass density, and the evaporation rate equation

$$\frac{dr_p}{dt} = - f(n_e, T_e). \quad (7.6)$$

The mass of the pellet, m_p , and the surface area, A_p , are expressed in terms of the pellet radius, r_p . The low velocity limit for the drag coefficient⁶ is given by

$$C_D = \frac{\exp(-(s^2/2))}{\sqrt{\pi} s^3} (1 + 2s^2) + \frac{4s^4 + 4s^2 - 1}{2s^4} \text{erf}(s) \approx \frac{1}{2\sqrt{\pi} s} \quad (31/3) \quad (7.7)$$

where s is the molecular speed ratio, $U_p/(2RT_*)^{1/2}$. The equation of motion may be written as

$$r_p \frac{dU_p}{dt} = - \frac{\rho_*}{\rho_s} 31 \sqrt{\frac{RT_*}{8\pi}} U_p \quad (7.8)$$

The subscript p refers to the pellet, and a subscript $*$ refers to the plasma average ion species. A first integral is readily obtained giving

$$r_p \frac{dx}{dt} + \xi(x) = - r_i U_i \quad (7.9)$$

where

$$\xi(x) = \int_{x(t_i)}^{x(t_o)} \left[\frac{\rho_*}{\rho_s} \left[31 \sqrt{\frac{RT_*}{8\pi}} + \frac{n_e}{n_*} \left(\frac{8kT_e}{\pi m_e} \right)^{1/2} \cdot \frac{2kT_e}{W_o} \delta \right] \right] dx \quad (7.10)$$

The electron density and temperature appear in the second term as it is the electrons that are responsible for the surface ionization of the pellet. W_o is the total ionization energy, and δ is an attenuation factor representing the effect of the newly ionized material in shielding the pellet. For moderate changes in δ over the thickness of the plasma slab an appropriate average value may be used. After performing the integration in Eq. 7.10, Eq. 7.9 may be integrated. The solution is of the form

$$x = \left(\frac{D}{C} + x_i \right) \cdot \left(1 + \frac{B}{A} t \right)^{-\frac{C}{B}} - \frac{D}{C} \quad (7.11)$$

where

$$\frac{D}{C} = \frac{r_i U_i}{\frac{\rho_*}{\rho_s} \left(31 \sqrt{\frac{RT_*}{8\pi}} + \frac{n_e}{n_*} \sqrt{\frac{8kT_e}{\pi m_e}} \cdot \frac{2kT_e}{W_o} \delta \right)} - x_i \quad (7.12)$$

$$\frac{B}{A} = - \frac{\rho_*}{\rho_s} \frac{\sqrt{\frac{8kT_e}{\pi m_e}} \cdot \frac{2kT_e}{W_o} \delta}{r_i} \quad (7.13)$$

and

$$\frac{C}{B} = - \left[1 + \frac{31 \sqrt{\frac{RT_e}{3\pi}}}{\sqrt{\frac{8kT_e}{\pi m_e}} - \frac{2kT_e}{W_0} \delta} \right] \quad (7.14)$$

Determination of the Admittance Factor

The effect of the high density plasma formed at the surface of the fuel pellet has been treated extensively in Ref. 2. This layer of material consists of an inner dense boundary layer in which absorption occurs and an outer region of cool plasma that is transparent to the incident high energy particles. We shall confine our attention here to the inner absorbing boundary layer in which the flux of incident energy carried by electrons is attenuated according to

$$F = F_0 \exp \left(- \frac{\Delta}{\lambda} \right) \quad (7.15)$$

where Δ is the thickness of the boundary layer,

$$\Delta = r_0 - r_p \approx U_g (\Delta t) \quad (7.16)$$

U_g is the expansion velocity at the surface, and λ is the average absorption mean free path for energy. Since the boundary layer is usually very dense and cold

$$\lambda \leq \Delta \quad (7.17)$$

The average density in the layer is given by

$$\bar{n} = \frac{\int G \, dt}{\frac{4\pi}{3} r_p^3 \left(\frac{r_0}{r_p} - 1 \right)} \quad (7.18)$$

where

$$G \equiv \frac{F_0 \delta \, 4\pi r_p^2}{W_0} \quad (7.19)$$

We shall now replace dt by

$$dt \approx \frac{r_p \left(\frac{r_o}{r_p} - 1 \right)}{U_g} \quad (7.21)$$

and assume that δ is approximately constant in time so that

$$\int \delta \, dt \approx \delta t \quad (7.21)$$

Some manipulation then gives the following transcendental equation for δ

$$\frac{\ln(\delta)}{\delta} = - .555 \times 10^{-5} \frac{r_p n_e \ln \Lambda}{U_g T_e^{1/2}} \quad (7.22)$$

in which the energy exchange mean free path has been used. Typically U_g is of the order of 10^6 cm/sec, the value used here.

Fuel Injection in PRDM Plasma

The plasma density profile in the PRDM is given by (see Chapter 4)

$$n_i = \frac{a^2 S}{4 D \alpha} \left[1 - \alpha \left(\frac{r}{a} \right)^2 \right] \quad (7.23)$$

This quadratic is generated as the solution of the diffusion equation for constant S and D . We modify this profile to account for the shift of the magnetic surfaces and use

$$n_* = \frac{a^2 S}{4 D \alpha} \left[1 - \alpha \left(\frac{x}{a - \Delta r_{\max}} \right)^2 \right] \quad (7.24)$$

where x is the distance measured from the magnetic axis which is shifted by an amount $\Delta r_{\max}$, from the original or nominal position. For the design profile $\Delta r_{\max}/a = .61$ and $\alpha = .95$,

consequently

$$n_* = \frac{a^2 S}{3.8 D} \left(1 - 6.25 \left(\frac{x}{a} \right)^2 \right) \quad (7.25)$$

The quantity (x/a) runs from 0 to 0.39. We shall now assume that the 56 shell grid used in the profiles code represents the same number of equal thickness slabs of uniform plasma. The slab thickness is given by

$$\Delta = \frac{a(1 - \Delta r_{\max}/a)}{56} = 2.26 \text{ cm} , \quad (7.26)$$

and the characteristic properties of these slabs are to be found in Chapter 4.

All the necessary information to determine the fuel deposition profile of a pellet is now available. Based on the plasma characteristics and the initial velocity and radius of the pellet, Eqs. 7.12, 7.13 and 7.14 may be evaluated for the first slab of plasma encountered. The admittance factor is evaluated based on the pellet radius at the entrance to the shell, and the time of flight in the shell is found from the solution of the equation of motion,

$$t_f = \left(\frac{x_i + \Delta + D/C}{x_i + D/C} \right)^{-B/C} / (1 + B/A) \quad (7.27)$$

This is valid for $t_f \leq A/B$, the time required for total evaporation. The pellet radius at the shell exit is then found from

$$\frac{r_o}{r_i} = 1 - \frac{B}{A} t_f , \quad (7.28)$$

and consequently the fraction of the pellet mass deposited in the shell is

$$\frac{\Delta M}{M_o} = 1 - \left(1 - \frac{B}{A} t_f \right)^3 . \quad (7.29)$$

The final velocity of the pellet is

$$U_f = - \left(\frac{C}{A} \right) \left(\Delta + \frac{D}{C} \right) \left(\frac{\Delta + D/C + x_i}{D/C + x_i} \right)^{B/C} \quad (7.30)$$

The fuel requirement profile of the reactor is

$$C^i = \left(S + \frac{S_\alpha^i}{2} \right) \cdot V_i \quad (7.31)$$

where C^i is the total fuel required in the i^{th} shell, V_i is the volume of the shell, S is the diffusion source strength and S_α^i is the alpha production rate per cm^3 in the shell. This is given in Fig. 7.1. For comparison several fuel deposition profiles are shown on the same diagram. The conclusion that we wish to draw from these data is that it is possible to inject a solid fuel pellet to the reactor mass centroid and that by using a reasonable combination of pellets of several sizes and velocities, a fuel deposition profile of fairly arbitrary shape may be provided.

Fuel Pellet Fabrication and Acceleration

The subject of fuel fabrication and the acceleration of fuel pellets to hypervelocities will be treated only briefly in this report. Experience has been obtained with smaller sized pellets, 1-10 μ in diameter. Velocities in excess of 10^6 cm/sec have been obtained. Three possible techniques, all of which have been employed in hypervelocity impact studies, will be considered. The first of these, electrostatic acceleration of frozen droplets is actively being developed with fusion applications in mind.⁷ This technique relies on the formation of a liquid jet by passage of the desired gas through a cryostat and carefully designed nozzle. Mechanical vibrations transmitted to the nozzle will cause the jet to break up into a series of droplets that are emitted into an evacuated chamber and freeze due to the evaporative loss of the heat of fusion. Charge is then placed on the resulting pellets, and an accelerating voltage is used to produce the desired high velocity. The acceleration of pellets larger than 1 mm in radius in this manner will be quite difficult if this technique is employed, as the accelerating voltage required (see Fig. 7.2) will be very large.

Hypervelocity projectiles in the 1-10 μ range have also been achieved by relying on condensation shocks in an appropriately designed expansion nozzle.⁸ This technique involves the acceleration of the gas to high velocity prior to the condensation point being reached. Yet another gas-based technology is involved in the use of a

744384

Fig. 7.1 Requirement curve for PRDM and several fuel deposition curves generated by hypervelocity ballistic injection.

744186

Fig. 7.2 Required accelerating voltage for fuel pellets of differing radii.

so-called gas gun to accelerate an already-formed solid projectile.⁹ Both of these techniques involve the injection of a large amount of driver gas into the reactor system. This problem and its effect on reactor operation have not been considered.

Discussion of Reactor Fueling

It is apparent from the analysis presented here that providing a steady state supply of fuel for reactor operation is not going to be easy. We have demonstrated that if only the shielding provided by the outer layers of a macroscopic fuel pellet were considered, deep penetration could be achieved if a sufficiently large accelerating potential were provided. The effects of the strong reactor magnetic field and possible electrostatic effects have not been considered here and, if as claimed,^{3,4} large reductions in the evaporation velocity result from these effects, the problem will be alleviated. Other alternatives, however, must also be explored.

Figure 7.2 shows that the fuel requirements of this device could be met by injecting 300 1 mm. pellets per second at 5×10^6 cm/sec. The accelerating potential required to do this however would be in excess of 100 million volts. We feel that this is beyond being a reasonable requirement. Although the velocity needed can and has been achieved the difference between the 10 million volt accelerator which we have provided and the more than 100 million volts required represents the technological gap which has to be overcome if the expected beneficial effects of the magnetic field are not realized.

References

- ¹J. M. Greene, J. L. Johnson, K. E. Weimer, Tokamak Equilibrium, *Phys. Fluids* 14, 671 (1971).
- ²S. L. Gralnick, Solid Deuterium Evaporation in a Fusion Plasma, *Nuclear Fusion* 13, 703 (1973).
- ³D. J. Rose, On the Fusion Injection Problem, unpublished.
- ⁴L. Spitzer, D. J. Grove, W. E. Johnson, L. Tonks, W. F. Westendorp, Problems of the Stellarator as a Useful Power Source, U. S. Atomic Energy Commission Report NYO-6047 (1954).
- ⁵H. D. Megaw, Density and Compressibility of Solid Hydrogen Deuterium at 4.2 K, *Phil. Mag.* 28, 129 (1939).
- ⁶S. Schaaf and P. Chambre, Flow of Rarified Gases, Fundamentals of Gas Dynamics, H. Emmons, ed. (Princeton University Press, Princeton, N. J., 1968) p. 703.
- ⁷C. D. Hendricks, Macroscopic D₂ Pellets for Refueling of Fusion Plasmas, to be published.
- ⁸H. G. Stever, Condensation Phenomena in High Speed Flows, Fundamentals of Gas Dynamics, op. cit., pp. 526-573.
- ⁹G. P. T. Wilenius and P. L. Cowan, The Constant Base Pressure Light Gas Gun, Third Hypervelocity Techniques Symposium (Denver Research Institute, University of Colorado, and Arnold Engineering Development Center, Denver, Colorado, 1964).

Chapter 8

Magnetics

U. R. Christensen

Introduction

This chapter covers the broad aspects of the magnetic configuration of the reactor. The magnetics in a toroidal device can be divided into two distinct field systems, the toroidal and the poloidal systems. These two field systems are mutually orthogonal and can therefore be treated independently.

Toroidal Field

The toroidal confinement field is a fundamental parameter of the reactor. Its magnitude must be as high as is technologically possible. Since normal conductors consume prodigious amounts of power, superconducting coils will be used for this purpose.

The maximum flux density for reliable performance has been assumed to be 16 tesla (160,000 gauss) in conformance with the present state of the art (see Chapter 13). This maximum density will appear at the smallest inside radius of the coil system, 3.94 meters. The RB product of the toroidal field will therefore be 63 (Tm). At the nominal plasma center of 10.5 meters, therefore, the flux density will be 6 tesla (60,000 gauss).

The total ampere-turns necessary to produce this field is 315×10^6 amperes, and the enclosed flux is 1410 webers. The single turn self inductance is 5.0×10^{-6} henries, and the mutual inductance with the poloidal field coils is zero. The magnetic energy stored in the toroidal coil system is 250×10^9 joules. The size and shape of the toroidal field coils are discussed in Chapter 13.

Poloidal Field

The Poloidal Coil System

There are three sets of poloidal coils. The first set provides for an axial or vertical field that serves to hold the plasma torus in equilibrium; the second set helps guide the diverted field lines through the divertor, and the third set of poloidal coils provides for a constant change of flux during the operating phase, which in turn produces a toroidal E field within the plasma to overcome the IR drop.

In this study, the diagrams of the poloidal field lines are generated by filamental loops. This is true also for the plasma ring where the filamental loop is

situated at the centroid of the distributed plasma current. When using the proper distributed plasma current the geometry of the poloidal field lines would thereby be modified to some extent, but since programs that calculate the distributed current are still in the formative stage, it was deemed sufficient for this study to proceed with the filament approach. Ultimately, the geometry of the poloidal field lines must be established on the basis of a properly distributed plasma current.

The Vertical Field

The vertical field coils are located around the plasma torus and as close to the torus as the shielding blanket will permit (see Fig. 8.1). The main purpose of these coils is to provide for an axial cross field that will exert an inward Lorentz force on the toroidal plasma current. This force is adjusted to be in equilibrium with the hoop forces of the plasma torus.

Another purpose of this set of coils is to establish a stagnation point of the separatrix on the inside of the plasma ring that will contribute to an inward force on the plasma. Since the arrangement is such that the radii of the coils are all greater than the radius of the stagnation point, this relation contributes to a larger aspect ratio.

And finally, a third purpose served by the vertical field coils is to supply the main energy for the ohmic heating of the plasma as well as to produce the plasma current by its inductive coupling.

The necessary vertical field to keep the plasma in equilibrium is expressed by

$$B_{eq} = - \frac{I}{R} \left(\ln \frac{8R}{a} - \frac{5}{4} + \beta_0 \right) 10^7 (T)$$

Stability of the Plasma Ring

The stability we are concerned with here is the vertical stability and the radial stability of the ring. The plasma ring is vertically stable if dB_v/dr is greater than zero, and it is radially stable if dB_v/dR is less than dB_{eq}/dR . B_{eq} is the necessary flux density to keep the plasma in equilibrium. Mathematically stated we must have

$$0 < \frac{\partial B_v}{\partial R} < \frac{\partial B_{eq}}{\partial R}$$

Fig. 8.1 Magnetic flux surfaces,
divertor and coils.

to obtain the two stabilities (see figure below).

By assuming the following scaling laws

$$I = I_0 \frac{R_0}{R}, \quad a = a_0 \sqrt{\frac{R}{R_0}}, \quad \text{and} \quad \beta_\theta = \beta_{\theta 0} \sqrt[3]{\frac{R_0}{R}}$$

we have by differentiation

$$\frac{\partial B}{\partial R} = \frac{I_0 R_0}{R^3} \left(2 \ln \frac{8 \sqrt{R_0 R}}{a_0} - 3.5 + \frac{7}{3} \beta_{\theta 0} \sqrt{\frac{R_0}{R}} \right) 10^{-7} \text{ (T/m)}$$

Setting $I_0 = 1$, $R = R_0 = 11.4$, $A_0 = 3.2$, and $\beta_{\theta 0} = 1.77$ we have numerically $dB_{eq}/dR = 0.603 \times 10^{-8}$ and from the computer we have $dB_v/dR = 0.276 \cdot 10^{-8}$, which tells us that the design is stable. The vertical field is obtained by omitting the field contributed by the plasma current. This field pattern is shown in Fig. 8.2.

Coil Arrangement and Ampere-Turns

The location of the vertical field and divertor coils is largely determined by compromising the various factors such as the uniformity of the field, the proximity to the plasma, and the compactness of the overall design. Once an estimate of the coil location has been made, a computer program optimizes the ampere-turns of the vertical field coils. The factors that enter into this optimization, presuming the current centroid of the plasma current has been determined, are the location of the stagnation point of the separatrix, the strength of the vertical field, and the derivative of the vertical field with respect to the radius. One of the critical variables that must be maximized is the minimum radius of the separatrix. This dimension allows for the amount

744326

Fig. 8.2 Field pattern in absence of plasma current.

of blanket thickness that can be placed between the Dewar of the toroidal field coils and the wall of the vacuum vessel.

Table 8.1 provides the following information:

Location of coil centroid
Ampere-turns
Cross-sectional area based on $j = 15.0 \text{ MA/m}^2$
Number of turns, based on $I = 10,000 \text{ A}$.

Divertor Field Coils

There are two sets of divertor coils. Both serve to shape and guide the field lines near the separatrix. The main set guides the field lines into a region that is available in the interior of the toroidal coil system. The other set of divertor coils serves to maximize the smallest radial location of the separatrix by pulling the field lines toward them, thereby allowing as much space as possible for the toroidal field coils.

Control Field Coils

During the steady phase of the reactor operating cycle, the current in the plasma ring will require an E-field to compensate for the ohmic voltage drop. This E-field is obtained from a constantly changing flux passing through the center of the reactor. The poloidal coils that produce this field are located all around the exterior of the toroidal field coil system (see Fig. 8.3). Since the flux from these coils will be varying in strength, any stray field from these coils will upset the poloidal field pattern on the inside of the toroidal coil system. It is therefore necessary that the fields produced by the sustaining field coils be zero or near zero in the inner region. By prescribing a certain NI distribution for these coils, this can be accomplished to any degree depending on the spacing of the individual coils. The variation of the NI distribution is done by a combination of variable spacing and a variable NI of the coils. Table 8.2 gives the coordinate of the centroid of each coil and the NI necessary to produce one weber.

Inductance Matrix

The inductance matrix of the four coil systems is shown in Table 8.3. For a particular system these inductances are determined by assigning a fractional number of turns to each coil proportional to the NI of the coil adjusted to make the sum of the turns within a system equal to one.

Table 8.1

NO	R m	Z m	NI/I	NI MA	a x b m ²	T
1	6.8	3.7	-0.380	-5.548	0.370	555
2	9.0	5.0	-0.200	-2.920	0.195	292
3	12.0	5.0	-0.296	-4.322	0.288	432
4	14.3	3.7	-0.147	-2.146	0.143	215
5	15.8	1.3	-0.135	1.971	0.131	197
6	11.5	8.0	0.330	4.818	0.321	482
7	5.4	3.9	0.157	2.292	0.153	229

734274

Fig. 8.3 Cross section of the nuclear island.

Table 8.2

No	R m	Z m	NI/ ϕ A/wb
1	2.5	0.5	40630
2	2.5	1.5	40630
3	2.5	2.5	40630
4	2.5	3.5	40620
5	2.5	4.5	40740
6	2.5	5.5	40640
7	2.5	6.5	43310
8	2.6	7.5	42950
9	2.93	8.43	33580
10	3.48	9.26	22010
11	4.28	10.05	10880
12	5.40	10.63	14480
13	6.72	11.00	7660
14	8.23	11.10	4930
15	9.95	10.87	3070
16	11.70	10.20	1780
17	13.30	9.16	1240
18	14.75	7.65	923
19	15.98	5.85	762
20	16.88	3.72	659
21	17.37	1.30	628

Table 8.3

Single Turn Inductance Matrix
(Microhenries)

	P	D	V	C	T
P	23.44	7.58	12.81	1.16	0
D	7.58	15.83	7.81	1.16	0
V	12.81	7.81	13.96	1.16	0
C	1.16	1.16	1.16	1.16	0
T	0	0	0	0	5.03

Single Turn Current Matrix
(Megamperes)

14.6	-33.88	14.25	64.0	315.
------	--------	-------	------	------

P - Plasma

D - Divertor Coil System

V - Vertical Field Coil System

C - Control Coil System

T - Toroidal Field Coil System

Chapter 9

Choice of Materials

F. H. Tenney

The Question of Temperature

As stated at the beginning of this report, our study has focused on the design of a first generation fusion power reactor. We therefore have tried to use materials, processes and techniques that were at hand or as nearly so as possible. One immediate question is what is the maximum design temperature for the reactor? High temperatures imply higher thermal efficiencies, less waste heat, more economy. High temperatures also imply materials that can retain their strength at high temperatures. The refractory metals, niobium, molybdenum and vanadium, immediately come to mind as possible candidates. Nb is useful at 1500°C. On the other hand there is only a limited Nb industry or technology at present for fabricating the large plates and tubes that would be needed in our reactor. The same is true for vanadium. There is an industry for fabricating molybdenum, but "moly" is notorious for unreliable welds. Thus it appears that the refractory metals are not yet available for large-scale construction. On the other hand stainless steel is available, is well known, strong, workable and weldable. True its useful temperature is limited to about 640°C. The theoretical thermal efficiency obtainable with stainless steel is therefore about 70% as compared to niobium's 85%. Thus the penalty in thermal efficiency for using stainless steel is not so great. Making a tentative choice of stainless steel as our principal structural material, questions other than temperature must be considered.

Question of Tritium Breeding

It has long been thought that the first fusion reactors would burn a deuterium-tritium mixture since of all the possible fusion fuels it has the lowest ignition temperature and requires the least confinement time for the plasma. Since tritium is radioactive with a half-life of some 12 years, it does not occur naturally in nature in sufficient quantity for use in a reactor. To use a DT fuel, the reactor must breed its own supply of tritium. Our next design question, then, is how to breed the tritium. The known sources of tritium are the isotopes Li-6 and Li-7 via the following neutron-induced reactions:

A common design scheme has been to use liquid lithium metal in the breeding blanket of a conceptual fusion reactor both as a breeding material and as a coolant. Our choice of stainless steel rules out this possible design since liquid lithium corrodes stainless steel, particularly at the elevated temperatures contemplated. On the other hand the molten salt flibe, a mixture of F, Li and Be, is reasonably compatible with stainless steel. If then one were to use flibe as a tritium breeding material, three further questions immediately arise, namely: will the breeding be adequate; how can one extract the tritium from the flibe; and can flibe be used as a coolant in the reactor blanket? We consider these question in turn.

It turns out that a satisfactory breeding blanket can be made with flibe (see Chapter 10). The F constituent in the flibe is detrimental to the breeding process since it acts as a neutron absorber. However the Be constituent is a neutron multiplier and offsets the action of the F. Uncertainty in the fluorine neutron cross sections creates an uncertainty in the blanket's breeding performance.

The tritium can be readily extracted from the flibe since the TF formed is relatively insoluble. It is necessary, however, to remove the flibe from the blanket for the tritium extraction as discussed in Chapter 14. Envisioning a flow of flibe into and out of the breeding blanket, we come to the last question. Can flibe be used as coolant?

Question of Coolant

One of the peculiar aspects of the coolant question is the strong toroidal magnetic field that permeates the blanket. Hence any liquid metal or molten salt that is expected to flow radially in the blanket will constitute an electrical conductor moving across a magnetic field. Just as in an electric generator, an induced voltage will appear in the moving conductor, and, if possible, an electric current will flow. The current flow will produce a drag on the moving conductor. This drag manifests itself as an increase in pressure drop necessary to pump a liquid metal or molten salt across a magnetic field. Perhaps more serious would be the possible generation of corrosive electrolytic action between the molten salt and the conduit walls. On balance it seemed to us that we had better limit the flow of flibe to a minimum and, in turn, cool the flibe by some other means. Both water and helium come to mind as possible coolants.

The choice between water and helium rested on the questions of safety and tritium extraction from the coolant. In the event of a leak in the cooling tube, steam would react with the flibe, possibly producing corrosive effects, whereas helium would be

inert. However, the larger concern revolved around the extraction of the tritium from the coolant.

Since the leakage of tritium from a DT burning fusion reactor is the principal hazard to the environment, great care must be exercised to keep this leakage low. Since tritium can leak through hot metals, the leakage path of most concern was from the flibe through the cooling tube walls into the coolant and thence to the steam generators and the steam turbines. Once the tritium entered the turbine loop it would be difficult to keep it from reaching the atmosphere. In the case of an emergency loss of plant load, the high pressure steam would have to be vented somehow. If the tritium content of the steam were too high, venting to the atmosphere would be prohibited. Thus, there is a premium on keeping the tritium in the primary cooling loop to a minimum.

To extract tritium from water involves an isotope separation of T from H. To extract tritium from helium is more straightforward. As discussed in Chapter 14, one can add a trace of oxygen to the helium to form T_2O , adsorb the "water" on molecular sieves and eventually electrolyze it to extract the tritium. There is no need for an isotope separation stage. On this point, then, helium as a coolant of the flibe was chosen over water.

Question of Swelling

The above considerations led us to the following choice of materials: stainless steel for the structural material, flibe for the blanket breeding material and helium as the primary coolant. The design of the reactor proceeded on this choice with the nagging observation that the expected swelling of stainless steel under large fluences of energetic neutrons might be as high as 50%! This large dimensional change was particularly disconcerting in view of the expected severe and rapid embrittlement that all structural materials were expected to experience in a DT fusion reactor. As discussed in Chapter 12, the embrittlement follows from the formation of helium in the metal as a consequence of (n, α) reactions made by the energetic neutrons. Consequently, the discovery that the steel, PE-16, had a markedly lower amount of swelling than stainless steel led to choosing PE-16 as the structural material for the breeding blanket and interior structures. Some properties of PE-16, flibe and helium, are shown in Tables 9.1 through 9.3 and accompanying figures.

Choice of Superconductor

The choice of Nb_3Sn as the superconducting material was dictated largely by the choice of 160 kilogauss for the maximum strength of the toroidal magnetic field. Of

the two readily available superconducting materials, Nb_3Sn and NbTi , only Nb_3Sn can reach such high fields and remain superconducting with reasonable current densities. Of course, not all the coil current elements operate in such a high magnetic field, but Nb_3Sn appears to be the cheaper material. It is true that NbTi has a larger allowable strain than Nb_3Sn , but the mechanical design problems do not require the greater ductility of NbTi for their solutions. See Chapter 13 for a detailed analysis of the choice of materials for all superconducting coils.

Choice of Shield Materials

The last major choice of materials was for the shield. Such shields are nominally to be made of heavy shielding concrete, loaded with iron scrap. The principal requirement for the shield is that it contain a high density of heavy metal (for attenuation of energetic neutrons), hydrogen (for thermalization), and boron (for final neutron absorption). Heavy concrete satisfies this list and, in addition, is relatively cheap and easy to handle.

Table 9.1. PE-16 DataProperty

Density (g cm^{-3})- 20°C	8.02
-660°C	7.93
Specific Heat (watt sec/g °C)	0.544
Melting Range (°C)	1310-1355
Electrical Resistivity	
(micro ohm cm)-200°C	115.2 ^a
-700°C	124.5 ^a
Coefficient of Linear Thermal Expansion (°C ⁻¹)	17.0 x 10 ⁻⁶ ^b
Thermal Conductivity vs Temp.	See Fig. 9.1
Stress Rupture Properties	See Fig. 9.2

a - Data for annealed material

b - Data for fully heat treated material

All data from: H. Wiggin and Co., Ltd.
Publication 3349B, Sept. 1969
Hereford, England

Table 9.2. Properties of Molten Flibe (47 mole % LiF)

<u>Property</u>	<u>Value</u>	<u>Ref.</u>
Melting Point ($^{\circ}\text{C}$)	363	1
Molecular Weight	36.90	-
Density (g cm^{-3})	1.94^{a}	2
Volume Expansivity ($^{\circ}\text{C}^{-1}$)	$2.2 \times 10^{-4}^{\text{a}}$	2
C_v , Heat Capacity ($\text{watt sec/g}^{\circ}\text{C}$)	1.97^{b}	3
C_p , Heat Capacity ($\text{watt sec/g}^{\circ}\text{C}$)	2.35^{b}	3
Thermal Conductivity ($\text{watt/cm}^{\circ}\text{C}$)	0.008	3
Isothermal Compressibility (cm^2/dyne)	$2.3 \times 10^{-12} \exp(10^{-3}T \text{ K})^{\text{c}}$	3
Vapor Pressure vs Temp.		Fig. 9.3
Viscosity vs Temp.		Fig. 9.4
Electrical Conductivity vs Temp. ^d		Fig. 9.5
LiF-BeF ₂ Phase Relationships		Fig. 9.6

a - 700°C (1292°F) value

b - Valid over range 363°C - 900°C

c - Factor of 3 uncertainty

d - The electrical conductivity of eutectic flibe has been taken equal to that of peritectic flibe. Peritectic flibe data are plotted Fig. 9.5.

References:

1 - ORNL-TM-4047, Dec. 1972

2 - ORNL-TM-4076, Dec. 1966

3 - ORNL-TM-2316, Aug. 1968

Table 9.3. Helium Property Data

Density (g cm^{-3})	1.785×10^{-4} 1.663×10^{-4}	(0°C , 1 atm) (20°C , 1 atm)
C_p Heat Capacity (watt sec/ g°C)	5.1953	

Thermal Conductivity vs Temp.

Fig. 9.7

Enthalpy vs Temp.

Fig. 9.8

Viscosity vs Temp.

Fig. 9.9

744327

Fig. 9.1 Thermal conductivity vs temperature.

Ref. for 316 SS data: Standard Handbook for Mechanical Engineers, 7th edition (McGraw-Hill Book Co., Inc., New York, 1967) pp. 4-92.

744333

Fig. 9.2 Larson-Miller plot for irradiated PE-16.

Ref: Henry Wiggin and Co., Ltd.,
 Hereford, England, Publication 3349B,
 September 1969.

744336

Fig. 9.3 Vapor pressure of Eutectic flibe (47 mole % LiF).

Ref: ORNL-TM-3913 (December 1965).

744328

Fig. 9.4 Viscosity vs temperature.

Ref: ORNL-TM-3789 (January 1965).

744332

Fig. 9.5 Electrical conductivity
vs temperature.

Ref: ORNL-TM-2316. (August 1968).

744324

Fig. 9.6 Phase diagram of the system $\text{LiF}-\text{BeF}_2$.

Ref. R. A. Romberger, J. Braunstein, and R. E. Thomas, J. Phys. Chem. 76, 1154 (1972). Reprinted with permission from above reference. Copyrighted by the American Chemical Society.

744331

Fig. 9.7 Helium thermal conductivity
vs temperature.

Ref: Handbook of Chemistry and Physics,
48th edition (Chemical Rubber Publishing Co.,
Cleveland, Ohio, 1967) Section E-3.

744330

Fig. 9.8 Helium enthalpy vs temperature.

Ref: Argon, Helium, and the Rare Gases, G. A. Cook, ed. (Interscience Publishers, New York, 1961) Vol. 1, p. 295.

744329

Fig. 9.9 Helium viscosity vs temperature.

Ref: Handbook of Chemistry and Physics,
48th edition, op. cit., Section F-42.

Chapter 10

Nucleonics

W. G. Price, Jr.

Introduction

Each D-T reaction in a fusion power reactor will produce a neutron with a kinetic energy of 2.25 pJ (14.06 MeV), 80 per cent of the fusion energy released. This chapter will consider the implications of this neutron production for the Princeton Fusion Reactor Reference Design.

The most obvious consequence is that a way must be found to stop and capture the neutrons - both to obtain their energy for the power cycle and to protect the plant and its operators from the radiation load. A second (and far more important) consequence is that the tritium fuel to be consumed in the plasma can be produced within the plant by neutron-induced transmutations.

To understand the magnitude of the nucleonic problems, consider that the neutrons are produced at a rate of 1.22×10^{21} per second, giving a nominal current through the vacuum wall of 0.781×10^{14} n/cm²·s (the total scalar flux due to this source is about 8.66×10^{14} n/cm²·s at the vacuum wall). The power carried out of the plasma by the neutrons is 2752 MW. The production of these neutrons entails the consumption of tritium atoms at the same rate, corresponding to 0.527 kg per day. Comparing this to the production rate of tritium in fission reactors - about 2 grams per 1000 MWe per year¹ - shows the necessity of producing this fuel within the fusion power plant.

The basic concept of the fusion power reactor consists of a toroidal plasma chamber, bounded by a solid vacuum wall, and surrounded by a set of cryogenic superconducting magnets. To accommodate the neutrons, a tritium-breeding, energy-absorbing blanket and a shield are placed between the wall and the coils. A principal constituent of the blanket is the element lithium, with isotopes Li-6 and Li-7, which can be transmuted to tritium through the reactions $\text{Li-6} (n, T) \text{He-4}$ and $\text{Li-7} (n, n' + T) \text{He-4}$.

Unfortunately, these are not the only neutron reactions. The neutron kinetic energy is transferred to the blanket through a combination of scattering collisions and captures, producing high energy recoil atoms, which are the cause of radiation damage. Furthermore, the neutrons that are captured in materials other than the lithium cause a transmutation of nuclides, leading to shifts in alloy composition, radioactivity in the structure, and release of high energy gamma rays. This latter is the only beneficial

aspect, for although it adds the problems of gamma transport and shielding, it constitutes a very considerable extra source of energy.

Design of Blanket and Shield

The blanket surrounding the toroidal plasma chamber must be designed to contain a large amount of lithium, to allow the easy extraction of heat energy and tritium, and to be self supporting under its gravity and vacuum loads.

For the reasons described in Chapter 9 the blanket in the Reference Design is made up of molten flibe (mixed LiF and BeF_2), PE-16 (a nickel-based structural alloy), and helium. This is taken to be an adequate design, not necessarily optimal but suitable for a first generation reactor.

The main magnet shield, placed between the blanket and the magnets, is assumed to be a conventional heavy concrete shield containing extra iron and boron-bearing minerals. It essentially serves as a backstop for the blanket. In the central region where the divertor channel passes close to the magnets, the shielding problem is much more severe. Here, a special shield composed of iron and lithium hydride is used.

The details of the physical design of the vacuum wall and blanket are covered in Chapters 11 and 12. Basically, this region consists of a large toroidal tank of flibe surrounding the main reaction chamber (except where gaps are left for plasma to pass into the divertor. (See Fig. 10.1.) The inner boundary of the blanket is a cellular wall containing no flibe because of the extra heat load it receives from the plasma X-rays. The coolant tubes in the blanket proper are closely packed adjacent to this wall and become more widely spaced as the distance from the wall increases. The thickness of the blanket (80 cm) was chosen to prevent the escape of all but about 1 percent of the incoming neutrons.

For the purposes of the nucleonic studies of the Reference Design, most of the heterogeneity of the blanket has been ignored; the relative proportions of the materials have been used to prepare a model with homogeneous materials in several regions. The volume fractions of PE-16, helium, and flibe in each of these regions (as calculated from the actual tube layout) are shown in Fig. 10.2. This volume fraction profile of the blanket is assumed to be independent of the location on the torus.

The vacuum wall, the cooling tubes, and the support structure in the breeding blanket are all made of the alloy PE-16, with the nominal composition:

Nickel	43. w/o
Iron	39. w/o
Chromium	18. w/o

744018

Fig. 10.1 Detail of the reactor cross section, showing blanket and shielding.

744232

Fig. 10.2 Schematic representation of the layered blanket model.

The density of this material is assumed to be 7.81 Mg/m^3 , corresponding to the mean tube wall temperature of 870 K (600°C). The molten flibe used is the eutectic, 53.1 m/o BeF_2 and 46.9 m/o LiF , with an assumed density of 1.894 Mg/m^3 corresponding to the mean temperature of 1070 K (800°C). The helium coolant is treated as a void in the nucleonic studies.

The blanket is terminated by a 5 cm void gap and a 5 cm layer of polyethylene, the latter serving as a minimal reflector for thermal neutrons leaking out of the blanket. The void space is provided for thermal insulation between the blanket and the shield and as a barrier to tritium migration into the shield.

The main magnet shield sits between the blanket and the coils except in the critical central region. Its thickness will vary from place to place, depending on the actual coil locations, and in fact some of the vertical field coils may be embedded in it. The nominal thickness of 50 cm should provide an additional attenuation factor of about 300 for the neutron flux. The use of concrete for this shield was suggested by a number of factors: the desire to have a removable shield made of segmented, solid pieces; the irregular shapes required for this; the cheapness of concrete; the possibility of using it as a structural component; and the known effectiveness of appropriate concretes as radiation shields.

The composition chosen for the main shield is a homogeneous mixture corresponding to 5 v/o helium for cooling, 5 v/o steel for structural support, and 90 v/o of Iron-Portland high density concrete.² The latter is a special shielding mix loaded with iron punchings and dosed with boron. The nominal density is 6.0 Mg/m^3 , and the principal constituents are:

Iron	87.50 w/o
Hydrogen	0.33
Oxygen	5.83
Boron	1.00
Other	5.34

It should be noted that it is the performance, not the composition, of the shield that is important. The materials used were chosen to provide "large" amounts of iron (to stop gammas and high energy neutrons), hydrogen (to thermalize the neutrons), and boron (to capture the neutrons), but no attempt has been made to optimize the proportions.

In the central region of the reactor, the shielding problem is much more severe. The gaps in the blanket that allow the plasma to escape from the reaction chamber also allow large neutron and gamma currents to pass. Unfortunately, while the plasma will be pulled up into the divertor, the neutrons and gammas can be expected to hit the vacuum wall, just at its closest approach to the cryogenic superconducting magnets. Thus, the central region shield needs to be designed for maximum attenuation with minimum thickness.

Working toward the plasma from the magnet, this special shield consists of 2 cm of lead (as a gamma shield), a 1 cm gap allowed for helium coolant flow, and a homogeneous mixture of 45 v/o iron and 55 v/o lithium hydride up to the vacuum wall (or the back of the blanket). There is assumed to be enough cooling to keep the temperature of the shield low; so the material densities are taken to be 11.34 Mg/m^3 for the lead, 7.68 Mg/m^3 for the iron, and 0.75 Mg/m^3 for the LiH. Presumably, this shield would be constructed with alternating layers of LiH and plates of (austenitic) iron.

Distribution of Neutrons and Gamma Rays

The suitability, from the nucleonic standpoint, of the blanket and shields described above has been investigated in a series of numerical studies of a simplified reactor model. (The term nucleonic is used because the concern is not centered on the fusion neutrons or their resultant gamma rays, but rather on the interaction of these with the nuclei in the reactor materials.)

There are three principal areas of concern to the nucleonic analysis. The first is the problem of determining the amount of tritium produced in the blanket. It is obvious that a design cannot be acceptable unless the tritium production rate exceeds the consumption rate, and that the amount of the excess (to allow for process losses, sale, etc.) is an important factor in ranking competing concepts. This ratio of tritium production to consumption is known as the tritium breeding ratio, T , and is equal to the average number of tritons produced per D-T source neutron. For the reference design reactor T is estimated to be 1.04, which would allow a doubling of the tritium inventory (without compounding) in four months.

The second principal problem for the nucleonic analysis is to find the amount and distribution of energy deposition in the blanket. Through a series of collisions the 2.25 pJ D-T neutrons are slowed down, transforming their directed energy into thermal energy in the blanket. Furthermore, many of the nuclear reactions by which the neutrons are finally absorbed are exothermic, and in the reference design an average 1.48 pJ

(9.25 MeV) per D-T neutron is liberated in this way. Most of this energy is initially released in the form of gamma rays, which thus form a substantial part of the radiation flux in the reactor. A side effect of the energy deposition process is a severe radiation damage problem.

The third area to be considered is the production of radioactive nuclides in the reactor by transmutation of the original materials. The concern for this radioactivity depends on the half-life of the nuclides: very short-lived isotopes constitute a problem for the emergency cooling system; isotopes with half-lives of days or weeks constitute a hazard during routine maintenance operations; while isotopes with long half-lives will pose an ultimate waste disposal problem. Fortunately, the structural material used in the reference design has no extremely hazardous activation products (although some cobalt-60 and nickel-63 will prevent simple disposal), and there are, of course, no fission products to worry about.

All of the nucleonic analyses will be based upon a series of calculations giving the distribution of neutrons and gamma rays in energy and direction of motion throughout the blanket and shields. Once this distribution is known, any of the measures of neutron or gamma ray interaction can be easily found by multiplying it times the appropriate reaction probability and integrating.

The distribution has been calculated by application of a computer code that approximates the solution of the linear transport equation:

$$\underline{\Omega} \cdot \underline{\nabla} \psi(\underline{r}, E, \underline{\Omega}) + \Sigma_t(\underline{r}, E) \psi(\underline{r}, E, \underline{\Omega}) = \int dE' \int d^2 \underline{\Omega}' \Sigma(\underline{r}, E' \rightarrow E, \underline{\Omega}' \rightarrow \underline{\Omega}) \psi(\underline{r}, E', \underline{\Omega}') + S(\underline{r}, E, \underline{\Omega}) \quad (10.1)$$

where $\underline{r}$, E , and $\underline{\Omega}$ are the position, energy, and direction of motion, ψ is the particle distribution function, S is the source function, and the Σ 's are the probabilities of interaction. This equation specifies the balance condition for the flow of particles through an infinitesimal volume, $d^3 r dE d^2 \underline{\Omega}$, of phase space. The computer code solves a finite difference, multigroup, discrete ordinates approximation: that is, the continuous phase space is partitioned into a set of spatial mesh intervals, energy groups, and angular ordinates, and a difference equation specifies the balance in each of these partitions. The resulting set (for the whole region) of coupled linear equations can be solved easily (in principle) on a digital computer.

The code used in these studies is a Plasma Physics Laboratory variant of ANISN,³ a well known and widely used program for this approximation. (ANISN was obtained from the Radiation Shielding Information Center at Oak Ridge National Laboratory, which has supplied much other valuable information.) The modifications⁴ to ANISN have principally involved improvements in the input and output options to allow direct coupling with other codes.

To conform to the one-dimensional capability of ANISN, the plasma, blanket, and main shield of the reference design have been modeled as infinite concentric cylinders, rather than as the layered non-circular toroid shown in Fig. 10.1. This introduces three sources of errors: 1) the source load on the first wall is a single nominal value; 2) streaming in gaps in the blanket is not treated; and 3) particle transport in the doubly curved geometry is not properly treated. Nevertheless, the final flux distributions are probably more useful than these would imply.

The neglect of the curvature of the axis of the plasma is justified partly because it is much smaller than the curvature around the plasma but mostly because it is self-canceling on the average: particles on the inside and outside of the reactor see the curvature with opposite signs. Adopting the view that ANISN gives an "average" flux distribution also alleviates the other two problems since a varying normalization of the wall load can be applied to simulate variations in the source function around the minor circumference.

The cylindrical reactor model is made up of thirteen concentric layers, each considered to be a homogeneous material. The materials and volume fractions composing each layer are listed in Table 10.1; these are converted to smeared atomic densities in Table 10.2.

Particle transport through the layers is calculated by ANISN on a variable spatial mesh containing 116 intervals in the whole model. The number of intervals in each layer is also indicated in Table 10.1. Generally, the interval size is 0.5 cm near the layer boundaries, ranging up to 2 cm in the middle of the layers. These dimensions conform to the following recommendation:⁵

$$\Delta R \text{ near a boundary} \leq \{ 1.0 + \Sigma_{gg}^s / \Sigma_g^t \} / 4 \times \Sigma_g^t \quad (10.2)$$

where $\Sigma_g^t = \max_j \Sigma_j^t$, and Σ_{gg}^s is for the same group.

Table 10.1. Material composition of the blanket model.

Zone Number	Inner Radius	Thickness	# of Intervals	Composition (by volume %)			
2	3.60 m	0.06 m	6	16.4% PE-16,	83.6% Helium,	0.0% Flibe	
3	3.66	0.10	13	5.7% "	21.2% "	73.1% "	
4	3.76	0.00362	2	100.0% "	0.0% "	0.0% "	
5	3.76362	0.12	11	4.5% "	16.7% "	78.8% "	
6	3.88362	0.00362	2	100.0% "	0.0% "	0.0% "	
7	3.88724	0.20	16	3.8% "	14.2% "	82.0% "	
8	4.08724	0.00362	2	100.0% "	0.0% "	0.0% "	
9	4.09086	0.30552	21	1.9% "	6.9% "	91.2% "	
10	4.39638	0.00362	2	100.0% "	0.0% "	0.0% "	
11	4.40	0.0475	2	VOID			
12	4.4475	0.0025	1	100.0% SS-304			
13	4.45	0.05	7	1.5% SS-304, 98.5% Polyethylene			
14&15	4.50	0.50	29	5.0% SS-304, 5.0% Helium, 90.0% Concrete			

Using: * * * * *

PE-16 @ 7.81 Mg/m³; 43.% Nickel, 39.% Iron, 18.% Chromium (by weight)

Flibe @ 1.894 " 53.1% BeF₂, 46.9% LiF (mole fractions)

Concrete @ 6.0 " 87.5% Fe, 0.33% H, 1.0% B, 5.83% O, 5.34% Other (by weight)

Polyethylene @ 0.93 " Chains of (CH₂)_n

SS-304 @ 7.8 " 9.5% Nickel, 70.% Iron, 19.% Chromium, 1.5% Manganese

Table 10.2. Atomic densities (atoms/bn·cm) in the blanket model.

<u>Zone #</u>	<u>2</u>	<u>3</u>	<u>4</u>	<u>5</u>
Fluorine	.0	.034383	.0	.037064
Beryllium	.0	.011926	.0	.012855
Lithium	.0	.010533	.0	.011354
Lithium-6	.0	.000797	.0	.000859
Lithium-7	.0	.009736	.0	.010495
Nickel	.005650	.001964	.034450	.001550
Iron	.005387	.001872	.032847	.001478
Chromium	.002670	.000928	.016283	.000733

<u>Zone #</u>	<u>6</u>	<u>7</u>	<u>8</u>	<u>9</u>	<u>10</u>
Fluorine	.0	.038570	.0	.042897	.0
Beryllium	.0	.013377	.0	.014878	.0
Lithium	.0	.011815	.0	.013141	.0
Lithium-6	.0	.000894	.0	.000994	.0
Lithium-7	.0	.010922	.0	.012147	.0
Nickel	.034450	.001309	.034450	.000655	.034450
Iron	.032847	.001248	.032847	.000624	.032847
Chromium	.016283	.000619	.016283	.000309	.016283

<u>Zone #</u>	<u>11</u>	<u>12</u>	<u>13</u>	<u>14-15</u>
Nickel	.0	.007601	.000114	.000380
Iron	.0	.058881	.000883	.053898
Chromium	.0	.017166	.000257	.000858
Manganese	.0	.001282	.000019	.000064
Carbon	.0	.0	.0401	.0
Hydrogen	.0	.0	.0802	.010755
Oxygen	.0	.0	.0	.011858
Boron	.0	.0	.0	.003008

The smoothness of the calculated flux indicates that this mesh is easily fine enough.

The source of neutrons from the plasma has been represented as a shell source adjacent to the first wall of the blanket. Since the fusion neutrons have kinetic energies near 2.25 pJ, the source is concentrated in the top group of the multigroup set. The normalization on the source is one neutron leaving the plasma, and the results are eventually scaled by the wall load.

The angular distribution of the source has been obtained from a separate ANISN calculation. In this case, the one-group flux at the outside of an infinite cylinder of vacuum was determined based upon a distributed source of isotropic emitters located within the cylinder. The variation of the source strength with the radius (plotted in Fig. 10.3) is taken from the plasma condition profiles computed for a cylindrical model of the reference design (see Chapter 4). These profiles are expected only to approximate the actual plasma conditions, because they do not incorporate any toroidal effects; but it is felt that the use of either a line source or a uniform source would be even less realistic.

The angular distribution of the flux is calculated in the S-N approximation, i.e., restricting the angles of motion to a discrete set. N was chosen to be 8, striking a balance between accuracy (S-4 is not fine enough for deep penetration) and expense (S-8, in cylindrical geometry, requires the treatment of 24 discrete angles). The nature of this quadrature is illustrated in Fig. 10.4, while Table 10.3 contains the specific cosines and weights used as input to ANISN. This table also contains the angular distribution of the assumed shell source.

The angular distribution of the flux is affected not only by the source distribution and the curved geometry but also by the anisotropy of scattering collisions in the reactor material. ANISN allows the expansion of the scattering angle distribution in Legendre polynomials to describe this. The calculations for the reference design have used P-3 cross sections for both neutron and gamma scattering, this being commensurate with the S-8 transport calculations.

ANISN treats the energy dependence of radiation transport in the multigroup approximation, requiring the input of scattering cross-section matrices for each material (and each Legendre order). The cross sections used for the reference design take the appearance of a normal 73 group set, but are constructed to provide a directly coupled calculation of the transport in 52 neutron groups and 21 gamma groups. This is achieved by locating the gamma groups "below" the thermal neutron group, and treating neutron-induced gamma production like downscattering. The group structures for the

734335

Fig. 10.3 Neutron source distribution from the plasma model.

744099

Fig. 10.4 Representation of the eighth-order angular quadrature.

Table 10.3. Parameters of the eighth-order angular quadrature.

Direction Number	R-Axis Cosine	Direction Weight	Source at R=3.5m
1	-.9759000	.0	.0
2	-.9511897	.0604938	.0
3	-.7867958	.0453704	.0
4	-.5773503	.0453704	.0
5	-.2182179	.0604938	.0
6	+.2182179	.0604938	.0000179
7	+.5773503	.0453704	.0005788
8	+.7867958	.0453704	.0014033
9	+.9511897	.0604938	.0022083
10	-.8164965	.0	.0
11	-.7867958	.0453704	.0
12	-.5773503	.0462962	.0
13	-.2182179	.0453704	.0
14	+.2182179	.0453704	.0000719
15	+.5773503	.0462962	.0013029
16	+.7867958	.0453704	.0024958
17	-.6172134	.0	.0
18	-.5773503	.0453704	.0
19	-.2182179	.0453704	.0
20	+.2182179	.0453704	.0005143
21	+.5773504	.0453704	.0029561
22	-.3086067	.0	.0
23	-.2182179	.0604938	.0
24	+.2182179	.0604938	.0041675

Table 10.4. Gamma 21-group energy structure.

Group Number	Upper Energy
1	14.0 MeV
2	12.0
3	10.0
4	8.0
5	7.5
6	7.0
7	6.5
8	6.0
9	5.5
10	5.0
11	4.5
12	4.0
13	3.5
14	3.0
15	2.5
16	2.0
17	1.5
18	1.0
19	0.4
20	0.2
21	0.1 - 0.001

Table 10.5. Neutron 52-group energy structure.

Group Number	Upper Energy	Upper Lethargy	Group Number	Upper Energy	Upper Lethargy
1	1.4918 +7 eV	-0.4	27	1.1080 +6	2.2
2	1.3499 +7	-0.3	28	1.0026 +6	2.3
3	1.2214 +7	-0.2	29	9.0718 +5	2.4
4	1.1052 +7	-0.1	30	8.2085 +5	2.5
5	1.0000 +7	0.0	31	7.4274 +5	2.6
6	9.0484 +6	0.1	32	6.7205 +5	2.7
7	8.1873 +6	0.2	33	6.0810 +5	2.8
8	7.4082 +6	0.3	34	5.5023 +5	2.9
9	6.7032 +6	0.4	35	4.9787 +5	3.0
10	6.0653 +6	0.5	36	4.5049 +5	3.1
11	5.4881 +6	0.6	37	3.0197 +5	3.5
12	4.9659 +6	0.7	38	2.0242 +5	3.9
13	4.4933 +6	0.8	39	1.3569 +5	4.3
14	4.0657 +6	0.9	40	6.7379 +4	5.0
15	3.6788 +6	1.0	41	2.4788 +4	6.0
16	3.3287 +6	1.1	42	9.1188 +3	7.0
17	3.0119 +6	1.2	43	3.3546 +3	8.0
18	2.7253 +6	1.3	44	1.2341 +3	9.0
19	2.4660 +6	1.4	45	4.5733 +2	10.0
20	2.2313 +6	1.5	46	1.6702 +2	11.0
21	2.0190 +6	1.6	47	1.1442 +1	12.0
22	1.8268 +6	1.7	48	2.2603 +1	13.0
23	1.6530 +6	1.8	49	8.3153 +0	14.0
24	1.4957 +6	1.9	50	3.0590 +0	15.0
25	1.3534 +6	2.0	51	1.1254 +0	16.0
26	1.2246 +6	2.1	52	4.1399 -1	17.0

gammas and the neutrons are listed in Tables 10.4 and 10.5. Note that there is only one group provided for all neutrons with energies below 0.0663 aJ (0.414 eV), so that the interpretation of the "thermal" group flux depends on the nature of the cross sections provided. For the reference design calculations the dominant absorption cross section has been adjusted to represent interactions with a Maxwellian distribution at about 1029 K (756°C); the thermal group flux is thus the total flux normalization for this distribution.

An inconsequential limitation on the cross section sets used is the restriction to downscattering only. Since the neutron and gamma energies of interest are so far above thermal, the only possible "upscattering" process would be gamma-induced neutron emission, as in the Be-9 (γ, n) 2He-4 reaction. The cross section for this is less than 2 millibarns, however, and the neutron production (figured after the fact) is only about 0.15% of that from the (n,2n) reaction. Thus the neglect of this effect seems amply justified, especially in view of the great simplification of the transport calculations made possible by the downscattering-only structure.

Most of the cross-section sets used in these calculations have been taken from a library⁶ provided to the Plasma Physics Laboratory in early 1972 by Dr. Don Steiner of ORNL; a few were assembled at PPL. With a few exceptions, these sets contain: a) 52-group neutron transport cross sections collapsed with a CTR spectrum from the 100-group library DLC-2C⁷ (derived without resonance self-shielding from ENDF/B- II); 21-group gamma transport cross sections (produced by MUG⁸); and 52-group to 21-group gamma production cross sections extracted from the data collection DLC-12.⁹

Given the cross-section sets, the atomic densities, and the source, ANISN calculates the scalar and angular flux distributions. Fig. 10.5 shows the neutron flux for the reference design versus radius and energy in a 3-D projection. Figures 10.6 through 10.8 show the flux in more conventional forms. (All of these have been normalized to the nominal wall load of 0.781×10^{14} D-T n/cm² · s, corresponding to 2752 MW of D-T neutron power uniformly distributed on the walls of an 11.0 m by 3.6 m torus.)

The neutron flux spectra at the first wall, in the middle of the blanket, and in the shield are plotted in Fig. 10.6. In Fig. 10.7 these spectra are integrated to show the fraction of the flux with energy greater than E. The CTR spectra are characterized by: a 14 MeV component (source streaming); a high-energy range fed by inelastic scattering; an intermediate-energy range with smooth slowing down; and a thermal-energy range showing the effects of strong absorption. The lithium-6 in the blanket and the boron-10 in the shield cause the latter effect, which extends to

734293
Fig. 10.5 View of the neutron flux
versus space and energy.

734304

Fig. 10.6 Neutron flux (differential) spectra at several points.

734299

Fig. 10.7 Neutron flux (integral) spectra at several points.

surprisingly high energies. The CTR spectra are notably broader than the "LMFBR" spectrum (from an FFTF mockup¹⁰), due to the much higher-energy source and the moderating properties of the blanket. This implies that comparisons of neutronic effects based on total flux, or even flux above some cutoff, should be made with great caution. Reactions with very high thresholds will occur at a greater rate in a CTR, as will reactions with large thermal ($1/v$) cross sections; only those with significant cross sections in the intermediate range can be easily correlated to fast reactor total fluxes alone.

Figure 10.8 shows the spatial dependence of the source group current and the total neutron scalar flux. Note that for the nominal wall load of 0.781×10^{14} D-T n/cm²·s the total scalar flux at the first wall is 8.66×10^{14} n/cm²·s (a ratio of 11.1 to 1). Although the "14 MeV" current is strongly attenuated in the blanket (by a factor of 2500), it can be seen that the fluxes in the lower energy groups are progressively flatter. This effect, due to the interaction of slowing down and leakage, leads to a softening of the spectrum in the outer parts of the blanket, and a greatly reduced attenuation of the total scalar flux.

The thermal flux is highly peaked at the outer edge of the blanket due to back-diffusion from the polyethylene placed between the blanket and the shield. This 5 cm layer is approximately two thermal neutron diffusion lengths thick and should be nearly as effective as an infinite layer in "reflecting" thermal neutrons back into the blanket. Its purpose, of course, is to increase the flux, and therefore the tritium production, in the blanket. An overall measure of the effectiveness of the blanket in stopping the neutrons is that the net leakage across its outer wall is only 0.0182 neutron per source neutron. A comparable measure of the energy absorption is that only 0.00609 pJ is carried out of the blanket by these neutrons. A comparison of these numbers shows that the average energy of the leaking neutrons is 0.335 pJ (2.09 MeV), confirming the efficiency of the thermal reflector. The neutron flux is further attenuated by the concrete shield. At $r = 5.0$ m (the end of the main coil shield) the leakage is only 2.6×10^{-5} neutrons per source neutron, with a mean energy of 0.25 pJ (1.54 MeV).

The gamma fluxes in the reference design are shown versus space and energy in Fig. 10.9, while individual spectra from the vacuum wall, the middle of the blanket, and the shield are shown in Fig. 10.10. The energy distribution seems to be almost independent of position, with only small transients at the wall and the polyethylene layer. A comparison of the gamma flux spatial distributions with the neutron flux distributions shows a correlation with the thermal and near-thermal neutrons,

734298

Fig. 10.8 Spatial distribution of total neutron and gamma fluxes and 14 MeV current.

GAMMA FLUX vs. SPACE AND ENERGY

734294

Fig. 10.9 View of the gamma flux
versus space and energy.

734308

Fig. 10.10 Gamma flux (differential) spectra at several points.

attributable to the thermal neutron capture component of the gamma source.

These gamma flux calculations are unfortunately limited in their accuracy due to the unreliability of the gamma production cross sections being used. In particular, there are no (n, γ) production cross sections for fluorine, no $(n, n' \gamma)$ production cross sections for manganese, and no gamma production cross sections at all for beryllium. Thus the magnitude of the gamma flux is certainly underestimated, although the spectrum (in these broad groups) is probably not too bad.

Since the nucleonic calculations are all normalized to a nominal average wall load, some comments about the true wall load distribution are in order. The flux and current of D-T neutrons at the first wall can be calculated from the fusion reaction rate distribution by tracing straight-line trajectories to the wall. Unfortunately the process of ray-tracing and wall interception is rather difficult in the odd geometry of the reactor, and the source depends in a complicated (and still uncertain) way on both the major radius and the height above the mid-plane.

The curves in Fig. 10.11 show the estimated distributions of the current and the flux of DT neutrons at the first wall. The "wall load" may be taken to be the current, which is close to nominal at the high point of the wall (major radius $R = 11$ m), peaks about 10% higher at the mid-plane ($R = 11.6$ m), and falls to about one-half nominal near the divertor. The peaking of the current but falling off of the flux near $R = 11.6$ m indicates that the neutron distribution is more nearly perpendicular to the wall in this region, and hence more penetrating.

Results of Nucleonic Analysis

Once the neutron and gamma distributions have been calculated, they can be combined with various cross sections to obtain the reaction rates, etc., which characterize the design. A basic check on the consistency of the neutron distribution is provided by balance tables showing the production (Table 10.6) and absorption (Table 10.7) of neutrons in the blanket.

These tables are normalized to an external source of one DT neutron and indicate the net leakage into the shield of only 0.0182 neutron per source neutron. Within the blanket, however, 0.2801 extra neutrons are produced by $(n, 2n)$ reactions. Most of these occur in the beryllium, because of the unusually low threshold (only 1.85 MeV) in its cross sections. The $(n, 2n)$ reaction rate falls off faster than the total flux, reflecting a dependence on the high energy component of the flux.

The absorption of neutrons, which must balance with the source, production, and leakage, is dominated by lithium-6. This is due to its very high $(n, \alpha)T$ cross

744461

Fig. 10.11 Wall load distribution of neutron flux and neutron current.

Table 10.6. Neutron sources in the blanket (per source neutron).

Zone Number	Neutron production by (n,2n) reactions								Net In- Leakage
	Fe	Cr	Ni	Li-6	Li-7	Be	F*	Sum*	
2	.024201	.006275	.001352	.0	.0	.0	.0	.031828	.043915
3	.007021	.001794	.000382	.000550	.005035	.109665	.018732	.124447	.232687
4	.002435	.000614	.000129	.0	.0	.0	.0	.003178	.013257
5	.002106	.000526	.000110	.000252	.002183	.065049	.007945	.070226	.268121
6	.000748	.000185	.000038	.0	.0	.0	.0	.000971	.008132
7	.000705	.000173	.000035	.000116	.000955	.036983	.003387	.038967	.277695
8	.000117	.000028	.000006	.0	.0	.0	.0	.000151	.002779
9	.000060	.000014	.000003	.000025	.000196	.010017	.000668	.010315	.133689
10	.000006	.000002	.000003	.0	.0	.0	.0	.000011	.001406
All	.037399	.009611	.002058	.000943	.008369	.221714	.030732*	.280094*	.981681

* The fluorine (n,2n) reaction was not incorporated in the neutron balance

Table 10.7. Neutron sinks in the blanket (per source neutron)

Zone Number	Neutron loss by absorption reactions								(Source -Sink ÷Sink)
	Fe	Cr	Ni	Li-6	Li-7	Be	F	Sum	
2	.021592	.010600	.043341	.0	.0	.0	.0	.075533	+.002780
3	.009189	.004666	.019458	.269370	.001048	.012482	.041053	.357266	-.000369
4	.004528	.002370	.009513	.0	.0	.0	.0	.016411	+.001462
5	.005220	.002782	.010666	.282892	.000537	.008273	.027899	.338269	+.000231
6	.002599	.001409	.005076	.0	.0	.0	.0	.009084	+.002092
7	.003391	.001849	.006340	.282286	.000314	.005084	.017368	.316632	+.000095
8	.000883	.000485	.001555	.0	.0	.0	.0	.002923	+.002395
9	.000602	.000325	.001026	.135383	.000126	.001466	.005042	.143970	+.000236
10	.000565	.000205	.000644	.0	.0	.0	.0	.001414	+.002122
All	.048569	.024691	.097619	.969931	.002025	.027305	.091362	1.261502	+.000216

section and is quite desirable. Note that the total absorption is 1.2615 neutrons per source neutron, with 0.1709 going into the PE-16 and 0.0914 into the fluorine. It is this parasitic absorption that imperils the tritium breeding and makes the secondary neutron production so important.

The precise neutron balance in these tables reflects the precision of the calculational methods applied to data of uncertain accuracy. In addition to the problem of variation in the cross sections, there are two factors that would affect the neutron balance but which have not been incorporated in these calculations. The first is that the $(n,2n)$ reaction in fluorine was not incorporated in the transport cross sections, so that advantage has not been taken of an estimated 0.03 secondary neutrons per source neutron. The second is that resonance self-shielding factors were not applied to the PE-16 cross sections, so that the parasitic absorption has been overestimated by a few percent. Rather than revise all the various other reaction rates to reflect the greater availability of neutrons, these effects have been mentally reserved to offset any detrimental factors which may have similarly been left out.

The most important result of the neutron distribution analysis is the blanket tritium breeding ratio. This is found by evaluating the (n,T) reaction rates in Be, Li-6, and Li-7 from the cross sections plotted in Fig. 10.12. Table 10.8 shows the tritium production by material and zone, with a total of 1.067 tritons for each neutron that strikes the blanket. (To obtain the net reactor breeding ratio a small reduction will be made to account for DT neutrons that miss the blanket.)

Tritium is produced principally through the reaction Li-6 (n,T) He-4, which has a large $1/v$ thermal cross section (940.25 barns at 0.025 eV) and a resonance peak of 2.71 barns at 247 keV. As Fig. 10.13 shows, this reaction falls off through the blanket with the flux but rises sharply at the back wall, due to the thermal flux peak in the polyethylene reflector. Only about 0.0073 tritons per neutron are produced in that flux peak, however, so that the reflector turns out to be of little value.

Since the tritium production in the Li-6 alone is insufficient to maintain the fuel supply, the contribution of the Li-7 is very important. The pertinent reaction here is Li-7 $(n,n' + T)$ He-4, i.e., inelastic scattering and disintegration of the target. There is a threshold in this cross section at 2.82 MeV, so that the Li-7 is most effective in the region of hardest spectrum near the first wall. An important feature of this reaction is that the neutron is not absorbed; it is still free to slow down and be captured by the Li-6. Thus while the Be provides not quite enough extra neutrons to compensate for parasitic losses, the Li-7 provides enough bonus tritium to enable successful breeding.

744020

Fig. 10.12 Cross sections for tritium production in lithium and beryllium.

Table 10.8. Tritium production in the blanket (per source neutron).

Zone	Be	Li-7	Li-6	Total
2	.0	.0	.0	.0
3	.001996	.048669	.269238	.319903
4	.0	.0	.0	.0
5	.000814	.026913	.282809	.310536
6	.0	.0	.0	.0
7	.000334	.014497	.282238	.297069
8	.0	.0	.0	.0
9	.000063	.003742	.135369	.139174
10	.0	.0	.0	.0
All	.003207	.093821	.969654	1.066682

744236

Fig. 10.13 Plot of $\text{Li}(n,T)$ α reaction rates versus space.

The gross blanket breeding ratio must be reduced slightly because of the divertor channels that pass through the blanket; obviously any DT neutrons that pass into these will not produce any tritons. The fraction of neutrons lost in this way can be estimated by considering a point near the plasma center as an isotropic emitter and finding the solid angle subtended by the divertor channels. For the reference design this angle is relatively independent of the chosen reference point and indicates a loss of about 2% of the DT neutrons. Making an additional allowance of 1/2% for neutrons lost in the injection ports, etc., gives a net blanket source of 0.975 DT neutrons per fusion. Multiplying this by the gross multiplication yields the nominal blanket breeding ratio of 1.040 tritons per fusion.

The breeding of tritium implies a consequent consumption of the lithium in the blanket as a fuel. The loss rate in this reactor is about 1.003 kg of lithium-6 per full power day; operation for the proposed 30 years at 3/4 load factor will consume 8.24 Mg of lithium-6. The nominal blanket composition can be maintained by a daily feed of 1.10 kg/day of lithium enriched to 92% in lithium-6; however, it is not clear that this is the most economic scheme. Alternatively, the initial loading of flibe might be enriched to 10.2% and allowed to burn out to 4.8% without adding any lithium; this would trade "capital" charges against "fuel" charges. The exact strategy of lithium-6 replenishment will have to wait for a more detailed economic analysis of the supply of lithium.

The uncertainty in the breeding ratio has so far been assessed only very roughly. First, since the Li-6 (n,T) cross section is known very well its variations are neglected. Next, a 20% uncertainty in the parasitic loss cross sections could produce a ± 0.058 variation in the number of Li-6 tritons, while a 20% uncertainty in the Li-7 (n,n' + T) cross section would cause a ± 0.019 variation in the Li-7 tritons. Finally, a 20% uncertainty in the Be(n,2n) cross section would lead to a triton variation of ± 0.034 . Taking the square root of the sums of the squares of these suggests an uncertainty of about ± 0.076 in the 1.067 breeding ratio. This indicates that the reference blanket design is marginal and only conditionally acceptable, but the sensitivity results presented below show that the condition of successful breeding should be easy to meet in a more refined blanket design.

A program named SWAN¹¹ has been written to calculate, by variational methods, the worth in a blanket of specified materials and to allow optimization of the blanket composition according to those worths. ANISN is used as a subsidiary module to calculate fluxes and importance functions with respect to a desired nucleonic property (e.g., tritium production rate). The effect on the breeding ratio of small changes in the

blanket composition have been evaluated with SWAN and are shown in Figs. 10.14 and 10.15. The "effectiveness function" measures the sensitivity of the breeding ratio to small changes in atomic density; when multiplied by a density change distribution and integrated over volume it predicts the corresponding breeding ratio change. Thus, for example, the predicted effect of a 1% increase in the flibe density is an increase of 0.00173 in the breeding ratio.

More useful in evaluating material changes is the "replacement effectiveness function" that measures the sensitivity to material substitutions. It is evaluated by multiplying the effectiveness function of the substituting material by the ratio of new/old atomic densities and then subtracting from this the effectiveness function of the material being removed. Figure 10.15 shows the importance of the blanket regions nearest the plasma and the insensitivity of the deeper regions, the benefits of replacing flibe with beryllium, and the detrimental effect predicted for substitutions of graphite. (The latter surprising effect is explained in the following manner: graphite has been used in most blanket designs to compensate for the low stopping power of molten lithium, but this function is provided by the fluorine in the flibe; thus graphite would effectively only decrease the lithium-to-structure ratio.) Results like this from SWAN will be used to investigate ways of boosting the tritium breeding ratio.

Blanket Heating

The second principal reason for performing the nucleonic analysis is to determine the blanket heating rate and distribution. The total amount of heat produced per DT neutron, which strongly influences the desired fusion reaction rate, will depend on the blanket materials and arrangement. Furthermore, the provisions for energy removal (e.g., the cooling tube pattern and the helium flow rate) must be matched to the actual heating distribution.

Fluence-to-kerma (kinetic energy released in materials) factors have been used to deduce the total and local energy deposition in the blanket from the neutron and gamma fluxes. Microscopic kerma factors are calculated as the product of a neutron (or gamma) interaction cross section times the mean kinetic energy of the resulting particles; when multiplied by the material density they therefore give the probable energy deposition rate (i.e., power) density.

Most of the kerma factors used in this study were obtained from Dr. Don Steiner of ORNL along with his basic cross section library.⁶ (The neutron kermas were based on the work of Ritts, Solomito, and Steiner;¹² the gamma kermas were based on

734292

Fig. 10.14 Breeding effectiveness functions for constituents of flibe.

744235

Fig. 10.15 Replacement effectiveness functions for potential blanket materials.

standard photon interaction models.) Since kermas were not available for all the constituents of PE-16, heating rates in this material were calculated as though it were 100% iron. (Because the kerma factors for nickel are expected to exceed those for iron, this should lead to an underestimate of the total energy deposition).

Although the use of kerma factors with the fluxes is quite straightforward, the resulting total power and power distributions may have somewhat reduced accuracies. One source of error in the total power may be a failure to maintain energy conservation when calculating the gamma production cross sections (in particular, it is suspected that energy is "produced" by the fluorine cross sections). Even when total energy is conserved, it may be improperly distributed. Thus, for example, some cross sections provide for no gamma production, but do incorporate the appropriate capture energies in the neutron kerma factors; consequently localized heating is overestimated. Despite these qualifying comments, the heating calculations are felt to be sufficiently accurate for this study. The excess of total energy over the kinetic energy of the source neutrons is principally determined by the neutron capture reactions and seems reasonable. The ratio of flibe heating to structure heating is probably high, but the combined heating distribution (which determines the cooling tube layout) should be fairly good.

The calculated total blanket power is 4562 MW, of which 2752 MW is due to thermalization of the kinetic energy of the DT neutrons, while 1810 MW is released by mass conversion during neutron absorption. These figures correspond to a release of 3.734 pJ per 2.25 pJ source neutron, an energy multiplication of 1.66. Of this total, 2.717 pJ are deposited in the flibe (1.81 pJ from neutron interactions plus 0.91 pJ from gammas) and 1.016 pJ are deposited in the PE-16 (only 0.16 pJ from neutrons and 0.85 pJ from gammas).

The calculated power production densities in flibe and PE-16 are shown as functions of depth in the blanket in Fig. 10.16. These have been normalized to the nominal wall load (current) of 0.781×10^{14} DT neutrons/cm².s; the estimated wall load distribution from Fig. 10.11 can be used to adjust the power density profiles at different wall positions.

Capital cost considerations provide an obvious incentive to increase the power component resulting from neutron absorption. Less obvious are the incentives to keep the breeding ratio from exceeding its required value: first, overproduction of tritium implies substantial handling and storage problems; second, stored tritium decays away. Assuming excessive breeding is achievable (e.g., through the use of beryllium as a neutron multiplier), there is consequently an interest in being able to increase the

734305

Fig. 10.16 Power densities in the flibe and the PE-16.

parasitic absorption: tritium may be traded for energy by controlling the neutron absorption. In particular, since neutron capture by sodium releases an unusually large amount of energy (1.115 pJ (6.96 MeV) from the capture itself and 0.750 pJ (4.68 MeV) from the subsequent decay) it is suggested that breeding be limited by the incorporation of sodium in the blanket. This should be easy to do, since NaF apparently can be added to the flibe with no adverse affects, giving a LiF-NaF-BeF_2 molten salt blanket.

Although most of the neutron-derived reactor power is developed in the blanket, a small fraction appears in the concrete shield, and a tiny but important part in the coils. The heating of the main concrete shield is calculated (using kerma factors as before) to be 55 MW, roughly 1% of the reactor power. About 3/4 of this is supplied by the gammas leaking from the blanket, since most of the neutrons are absorbed in the boron and release only 0.447 pJ each. Heating rates beyond the shield should be negligible.

Knowledge of the heat load on the various coils is important because of the expense of cryogenic refrigeration. The component of the heating due to radiation leakage has been estimated, as before, using kerma factors and the flux at a depth of 50 cm in the shield (roughly the closest approach of the coils to the blanket). The power density in solid iron at that point would be about 180 W/m^3 . Applying this (with a metal volume fraction of 90%) to the total volume of the vertical field coils gives a heating rate of 24 kW. Similarly an additional 4 kW heating is estimated in those parts of the toroidal field coils that pass close to the blanket. The principal heat load, however, appears to be due to leakage through the special shielding where the divertor approaches the toroidal field coils. This shield (discussed below) is very thin and allows an estimated load of 63 kW on the coils. The total radiation heating of the coils is thus about 91 kW, which must be removed at an "efficiency" of 300:1 by the cryogenic refrigeration system.

Radiation Damage

It is obvious that radiation damage will pose a severe problem for the structural materials in the blanket. A number of damage indices have been computed from the neutron flux distribution, for use in the mechanical design process. The implications of this radiation damage problem are discussed in Chapter 12.

The most obvious indication of the magnitude of the damage is the flux level, $0.866 \times 10^{15} \text{ n/cm}^2 \cdot \text{s}$ at the first wall. This is an order-of-magnitude higher than typical flux levels in a fission reactor (due mainly to the order-of-magnitude reduction

in energy release per neutron). The first wall fluence after 30 years' operation at a 3/4 load factor would be $6.15 \times 10^{23} \text{ n/cm}^2$, far exceeding most material test irradiations. Furthermore, as shown in Figs. 10.6 and 10.7, the spectrum is very broad, with both high- and low-energy components.

Most radiation damage analyses have attempted to correlate the damage against the flux of neutrons with energies exceeding some appropriate reference value. In order to make use of this type of correlation, partial fluxes of this kind have been calculated and normalized to the total flux; in Fig. 10.17 these flux fractions are plotted versus blanket position for the commonly used reference energies of 13.5 MeV, 3.68 MeV, 1.0 MeV, and 0.1 MeV. (The partial fluxes are obtained as the product of the total flux times this flux fraction). Also shown is the average energy of the neutron spectrum. It can be seen that the high energy component of the flux is decreasing faster than the total flux, but even at the rear of the blanket the high energy component is still quite large, about $10^{12} \text{ n/cm}^2 \cdot \text{s}$ for $E > 0.1 \text{ MeV}$. The hardening of the spectrum at the front of the shield is due to the sudden depletion of the lower-energy flux and the transient to the new "typical" spectrum, as seen in Fig. 10.5. Since the total flux drops considerably in the shield, radiation damage may not be a significant problem there.

Another index of radiation damage that is used to correlate test results is the concept of "Displacements Per Atom", the average number of times an atom is displaced from its lattice position. Atoms may be knocked loose either directly, as the result of interaction with high-energy neutrons (becoming "Primary Knock-on Atoms"), or indirectly, in cascade chains recoiling from these PKA'S.

Doran¹³ has calculated energy-dependent "displacement cross sections" for iron, chromium and nickel, using ENDF/B cross sections for the PKA'S and slowing down models for the cascades. When multiplied by the atomic density and the flux and integrated over energy they give the DPA per unit time. This has been done using the calculated blanket flux and densities appropriate to PE-16, to obtain the estimated DPA per year shown in Fig. 10.18. A similar calculation for a stainless steel structure gave practically the same distribution, with a peak of 27.2 rather than 26.8 DPA/year for PE-16 at the first wall. Note that the displacement rate falls off by almost three orders-of-magnitude through the blanket.

The final damage characteristics calculated are the production rates of hydrogen and helium in the structure. Since most (n,p) and (n, α) reactions have high thresholds, the interstitial production of these gases is expected to be a very much more severe problem for a fusion reactor structure than it has been for fission reactors.

734297

Fig. 10.17 Neutron spectral hardness indices in the blanket and shield.

744024

Fig. 10.18 Helium and hydrogen production and DPA distributions in the blanket.

Figure 10.18 also shows the expected atomic parts per million of H and He in the blanket structure, calculated with ENDF/B-II cross sections for the (n,p) and (n, α) reactions in nickel, iron and chromium. Both distributions fall off quickly through the blanket in the same way that the highest-energy component of the flux falls off.

The hydrogen production has an extraordinary peak of 1296 ppm (2.9% after 30 years); it is presumed, however, that it will quickly diffuse out of the metal. On the other hand the helium will probably be trapped, and thus its production is of major concern. The peak rate is 275 ppm/year at the first wall, more than an order of magnitude greater than current experience. As if this were not bad enough, there is concern that substantial additional amounts may be produced by reactions other than the direct (n, α).

Special Magnet Shield

The problem of shielding the toroidal field coils where the divertor swings past was raised earlier, in connection with coil heating rates. Radiation damage to these coils is potentially a much more serious problem. Neutron penetration through a possible shield has been analyzed for a preliminary version of the reference design reactor model; it is felt that the models are sufficiently similar that the analysis is still valid.

On the inside of the reactor the TF coils lie between 3.15 and 3.95 m major radius; the wall of the divertor is at 4.30 m. The 35 cm between is divided into: a) 5 cm of thermal insulation for the cryogenic coil; b) 2 cm of lead at 11.34 Mg/m³ for gamma shielding; c) 1 cm for a helium coolant channel; and d) 27 cm of actual shield. A homogeneous shield region was taken to consist of 45 v/o iron (at 7.68 Mg/m³) and 55 v/o lithium hydride (at 0.76 Mg/m³) for the flux calculation. Although these proportions have not been optimized for this specific problem, they should provide excellent attenuation of high energy neutrons and absorption of slow neutrons.

A $P_3 S_8$ calculation of the transport into an infinite cylinder with this composition was performed, using the following realistic (but imprecise) two-part source. One source component was obtained by estimating the direct load of DT fusion neutrons streaming up the divertor; the other had the spectrum of the flux at the first wall of the blanket and was normalized using the blanket albedo and the shadowing effect of the divertor opening.

The calculation gives a total flux level of 7.7×10^{11} n/cm²·s at the coil face, of which 4.3×10^{11} is due to neutrons with energies over 10 keV and 1.0×10^{11} is due to neutrons with energies over 1 MeV. The implied

$E > 10$ keV fluence of 1.3×10^{19} n/cm² per full power year will cause significant increases in the resistivity of the normal-conductor substrate of the coils¹⁴ and may affect the superconductor itself. The uncertainty about these damage effects puts strong pressure on the reactor designer to increase the thickness of the special divertor shields (despite their expense). Because of the short relaxation (e-folding) length for the flux in this shield (about 7 cm) any such increase would greatly reduce the potential problems.

Induced Radioactivity

The last major topic of this chapter on nucleonics will be the transmutation and radioactivation of the materials in the blanket. Concern about afterheat problems, personnel exposure and eventual waste disposal require an evaluation of the inventory of radioactive materials in the blanket. A Transmutation Chain Analysis Program (TCAP)¹⁵ has been written to solve the production and decay equations for the large number of nuclides involved.

Using standard notation, this basic equation for each nuclide, i , can be written

$$\frac{d}{dt}N_i = \sum_j (N_j \phi \sigma_{j \rightarrow i}) - N_i \phi \sigma_i - N_i \lambda_i \quad (10.3)$$

The full set of these equations can be represented by a single matrix equation,

$$\frac{d}{dt}\underline{N} = \underline{M} \cdot \underline{N} \quad (10.4)$$

where the vector $\underline{N}(t)$ is composed of the nuclide densities at time t and $\underline{M}$ is a constant transfer matrix. The formal solution of such a matrix differential equation is¹⁶

$$\underline{N}(t) = \exp(\underline{M}t) \cdot \underline{N}(0) \quad (10.5)$$

using the definition of the matrix exponential

$$\exp(\underline{M}t) \equiv \underline{I} + \underline{M} \frac{t}{1!} + \underline{M}^2 \frac{t^2}{2!} + \dots \quad (10.6)$$

Approximate nuclide densities at intervals of Δt can be found by accumulating the sum

$$\exp(\underline{M}\Delta t) = \sum_{J=1}^K \underline{M}^J (\Delta t)^J / J! \quad (10.7)$$

until all the elements of the K -th term are sufficiently small, and then using the recursion relation

$$\underline{N}(m\Delta t) = \exp(\underline{M}\Delta t) \cdot \underline{N}((m-1)\Delta t) \quad (10.8)$$

A practical problem with this procedure is that the series for $\exp(\underline{M}\Delta t)$ will converge very slowly if any of the elements of $(\underline{M}\Delta t)$ are large, or effectively if any of the nuclides have half-lives much shorter than Δt .

To avoid this problem while still allowing the specification of arbitrary data to a generalized program, a special screening process has been built into TCAP: nuclides with very short half-lives are set aside to be treated at each time step $m\Delta t$ as being in equilibrium with their precursors. A transfer matrix is formed for the remaining nuclides, and then densities at each time interval are found by applying first the matrix exponential and then the equilibrium equations. Decay rates and decay powers for each nuclide are obtained from these densities.

Separate calculations have been performed for the PE-16 structure and for the flibe. Only the major constituents of the flibe - Li-6, Li-7, Be-9, and F-19 - are assumed present initially. The composition of the PE-16 supplied to TCAP includes a few of the minor constituents neglected in the flux calculation: the composition by weight is 43% nickel, 35% iron, 17% chromium, 3% molybdenum, 1% titanium, and 1% aluminum. All the stable isotopes are assumed in their proper proportions.

TCAP calculates single-zone homogeneous densities using a one-group, volume-averaged flux. Accordingly the cross sections must be energy-averaged by weighting with the flux spectral distribution and also volume-averaged by weighting with the material density and total flux distributions. For the flibe calculations, each cross section was weighted with the flibe volume fraction distribution in the blanket; for the PE-16, each was weighted with the structure volume fraction distribution (as used in the transport calculation). The blanket averaged densities were then initialized to

total mass divided by total volume. The volume weighting of the cross sections of the second-generation nuclides with the PE-16 distribution is perhaps unreliable, but due to the uncertainties in the cross sections themselves it is not felt that greater sophistication is warranted.

The energy-dependent microscopic cross sections have been taken from a preliminary version of the CASDAR library¹⁷ being produced at ORNL. This library contains cross sections obtained principally from ENDF/B-II, UCRL-50484,¹⁸ and the ORNL GAM-THERMOS library, all converted to a standard 123-group structure. The coverage of activation cross sections is quite complete, though of uncertain accuracy for all but the most common reactions. For this reason the precision of the activities calculated by TCAP must not be confused with great accuracy in the evaluation.

The decay modes and the half-lives of the nuclides have been taken from the "Table of the Isotopes"¹⁹ by Lederer, Hollander and Perlman. In order to calculate decay heating, the energies of decay gammas were taken from this same source, while average (as opposed to maximum) beta energies were obtained from the "Radiological Health Handbook".²⁰

Decay Power

The implications of the radioisotope production will be taken up under three principal categories: afterheat generation; shielding of personnel; and long term waste disposal. The activation of the structure was calculated assuming reactor operation for 30 years at three-quarters power terminated by 30 days at full power. Because of the short half-lives of the activities in the flibe these have been calculated as equilibrium values at full power.

Only six radionuclides have significant production rates in the flibe: He-6, Li-8, N-16, O-19, F-18, and F-20. All but the F-18 have half-lives under half a minute. The total immediate decay power in the flibe is 204 MW, but this decreases extremely quickly since it is dominated by the N-16 with a half-life of 7.2 seconds. After a few minutes only the fluorine-18 remains (109.7 minute half-life) with an initial activity of 664 MCi (24.55×10^{12} /sec) and decay power of 4.70 MW. It is also possible to determine from the TCAP output the rate of gain or loss of the elements of primary concern for the chemical processing of the flibe; these figures are given in Table 10.9. One implication is that the "salt doctor" must be prepared to compensate for about 71 (atom) moles per day of excess fluorine, due to the burnout of the beryllium. Note that helium will be produced at a rate of about 279 moles each day.

Table 10.9. Element transmutation rates in the flibe.

	<u>Gains</u>	<u>Losses</u>
	(atom moles/day)	
Fluorine	4.74	36.8
Beryllium	0.	39.8
Lithium-6	0.	165.3
Lithium-7	0.58	14.9
Oxygen	29.5	
Protium	5.00	
Helium	279.	
Tritium	180.	

Some 94 nuclides were carried along in the transmutation analysis of the 1.031 Gg of structural material, but only about a dozen are important in the sense of having either half-lives longer than a few days or appreciable activities. Fig. 10.19 is a chart showing initial power versus half-life for these. The total afterheat power, which is initially 48.0 MW, decays to 42.5 MW after 10 minutes. It continues to decline slowly - to 7.79 MW after 1/2 year, 4.08 MW after 1 year - until after about 5 years it is effectively equal to the decay power of the cobalt-60: 2.35 MW initially with a half-life of 5.26 years. The afterheat power curves for the flibe, the PE-16 and their sum are shown in Fig. 10.20. Here the short term dominance of the N-16 and the long term dominance of the Co-60 can both be seen on the split time scale. The interest in the afterheat is of course not so much with the power level as with the amount of energy released and its potential impact if the blanket cooling should be interrupted. Four components of the decay heating have been evaluated: 1) 2.58 GJ from total decay of all but the F-18 in the flibe; 2) 14.09 GJ from the first hour's decay of the F-18; 3) 40.25 GJ from the first hour's decay of the Mn-56 in the structure; and 4) 108.6 GJ from the first hour's decay of all other radionuclides in the PE-16. Although this is a great deal of energy, the blanket temperature increase that it represents is quite modest. Assuming distributed gamma heating and good conduction, the full blanket heat capacity is available. This amounts to 0.561 GJ/K for the PE-16, plus 4.35 GJ/K for the flibe, thereby holding the temperature change to only 30.3 K. (Of course the blanket cannot sustain 40 MW of decay without cooling for too long, but the time available to activate a backup system is measured in hours.) To put the emergency afterheat problem in further perspective, consider that it will require about 10 seconds to "turn off" the plasma, thus dumping about 50 GJ into the blanket after shutdown is initiated.

A genuine problem resulting from radionuclide decay after shutdown is the potential exposure of maintenance personnel to excessive radiation. The dose rate just outside the concrete shield enclosing the blanket has been estimated as follows. The total gamma decay power 15 minutes after shutdown is 35.5 MW, and the blanket outer surface area is about 1910 m^2 ; thus the average gamma energy fluence is $1.16 \times 10^{13} \text{ MeV/cm}^2 \cdot \text{s}$. Using a mass attenuation coefficient of $0.05 \text{ cm}^2/\text{g}$, a buildup factor of 50, and a dose conversion factor of 1 mrad/hr per $800 \text{ MeV/cm}^2 \cdot \text{s}$, the dose rate outside the 80 cm concrete shield is only 27.4 mrad/hr. Since the dose limit for occupational exposure is 1250 mrad per quarter, it can be seen that useful work can be accomplished if necessary very close to the blanket shortly after shutdown.

The dose outside the blanket shield is very much higher when the reactor is operating because of the leakage of neutrons and directly-produced gammas. Evaluating the

744405

Fig. 10.19 Power at shutdown versus half-life for blanket radionuclides.

744260

Fig. 10.20 Decay power after shutdown for the flibe and the PE-16.

dose rate for a position corresponding to the nominal wall load gives 65.6 rad/hr gamma dose plus 3625 rem/hr neutron dose. A substantial additional biological shield will be required around the reactor (probably outside the main coils and support structure) because of this, with access limited to periods of reactor shutdown.

Radioactive Waste

It is very important to avoid creating an exaggerated public impression of the "radioactive cleanliness" of fusion reactors. Because of the intense high-energy flux there will be a very large inventory of ordinary activation products. The safety and disposal problems associated with these may be qualitatively simpler than those of fission reactor wastes, but they are not at all negligible.

The total calculated activity at shutdown for the 2030 MWe reactor is 19.61 GCi. Although this is considerable, it is difficult to assign a safety hazard potential to it because of a lack of plausible dispersal mechanisms. A large part (6.45 GCi) of the total activity is due to the short-lived isotopes in the flibe, while another 0.664 GCi is from the fluorine-18 with a half-life of 109.7 minutes. The remainder is from the actual structure of the reactor, which is presumably inert. After cooling for about a day (eliminating most of the radionuclides) the activity is of the order of 9 GCi. Subsequent decay can be followed on an individual nuclide basis.

Figure 10.21 shows activity versus decay time for the longer-lived radionuclides in the blanket (plus the F-18). The various cobalt isotopes, produced from nickel, dominate the activity - first Co-58, then Co-57, and finally the Co-60. Iron-55 has a large decay rate, exceeding that of the Co-60 for the first 17 years, but since it decays by electron capture it is relatively innocuous. The large amount of Co-60 is the principal obstacle to the "recycling" of the reactor structural material. On the average, there is 0.148 Ci/g of structure; considering the flux peaking there may be of the order of 0.5 Ci/g near the first wall. It is unfortunate that this long-lived nuclide is also one of the most hazardous: two gammas, one of 0.213 pJ and one of 0.187 pJ (1.33 and 1.17 MeV) are emitted in the main decay branch of $\text{Co-60} \rightarrow \beta^- + \text{Ni60}$. Since these are quite penetrating, materials contaminated with Co-60 must be shielded and handled remotely. It should be emphasized that the cobalt isotopes are produced by neutron interactions with nickel, so that any "steel" will be subject to this problem.

Eventually the cobalt will decay, leaving nickel-63 as the dominant activity, with a half-life of 92 years. In the very long term the residual activity will be due to activation of the minor constituents of the structural alloy, e.g., the molybdenum in the PE-16.

744022

Fig. 10.21 Activities of the long-lived radionuclides in the structure.

The activity in this 2030 MWe fusion reactor blanket can be compared to twice that in the core of a "typical" 1100 MWe water-cooled fission reactors²¹ provided that the qualitative differences in these activities are noted. As described above, the fusion reactor inventory is 19.61 GCi at shutdown, 1.56 GCi after 1 year, and 0.11 GCi after 10 years. In the two fission reactors there are initially 27.6 GCi of fission products, 6.9 GCi of actinides, and only 21.2 MCi of ordinary activation products, for a total of 34.5 GCi. After 1 year these decay to 292 MCi, 10.3 MCi and 1.23 MCi respectively (totaling 152 MCi), while after 10 years they are down to 35.8 MCi, 6.54 MCi and 0.26 MCi, a total of 42.6 MCi. Beyond this the long half-lives of the remaining fission products and actinides will hold the fission reactor inventory above that in the fusion reactor.

Concluding Remarks

In order to fulfill the requirement that this reactor study be comprehensive, an attempt has been made to evaluate all of the "nucleonic" aspects with a "reasonable" degree of accuracy. It is felt that the uncertainties in the results reported here are commensurate with the preliminary nature of the reactor model; as the physical design is refined the nucleonic evaluations will be refined also.

The principal deficiencies in this analysis have been noted as they arose, but will be summarized here. First, the use of an infinite cylindrical model for the flux calculations prevents a proper determination of the wall load distribution and, more unfortunately, eliminates the effects of the open divertor channels. Second, uncertainties in the high energy cross-section sets imply uncertainties in the lithium-7 component of the tritium production and in the distribution and magnitude of the gamma flux. Third, many of the damage and activation cross sections are based on expert guesswork rather than careful experimentation, particularly at high energies.

Overall, it is felt that the tritium breeding, the most critical result, is reasonably reliable; that the power generation is open to more question; and that the radionuclide inventory figures should be taken as guidelines only.

References

¹The Safety of Nuclear Power Reactors (Light Water-Moderated) and Related Facilities, U. S. Atomic Energy Report WASH-1250 (1973) p. 4.17.

²Reactor Handbook, 2nd edition, ed. C. R. Tipton, Jr. (Interscience Publishers, New York, 1960) Tables 51.84 and 51.95.

³W. W. Engle, Jr., A User's Manual for ANISN, Oak Ridge Gaseous Diffusion Plant Report K-1693 (1967).

⁴W. G. Price, Jr., A Revised Version of the ANISN Code, Princeton Plasma Physics Laboratory Report MATT-1035 (1974).

⁵Revised WANL ANISN Program User's Manual, WANL-TMI-1967; incorporated in ANISN, A One-Dimensional Discrete Ordinates Transport Code, Radiation Shielding Information Center, Computer Code Collection Number CCC-82 (1972).

⁶D. Steiner, private communication.

⁷100-Group Neutron Cross-Section Data Based on ENDF/B, Radiation Shielding Information Center, Data Library Collection Number DLC-2C (1972).

⁸J. R. Knight and F. R. Mynatt, MUG: A Program for Generating Multigroup Photon Cross Sections, Union Carbide Corporation Computing Technology Center Report CTC-17 (1970).

⁹A Compendium of Neutron-Induced Secondary Gamma-Ray Yield and Cross-Section Data, Radiation Shielding Information Center, Data Library Collection Number DLC-12 (1972).

¹⁰T. J. Yule and E. F. Bennett, Measured Spectra in a Number of Uranium- and Plutonium-Fueled Reactor Assemblies, Nuclear Science and Engineering 46, 236 (1971).

¹¹E. Greenspan, W. G. Price, Jr., and H. Fishman, SWAN: A Code for the Analysis and Optimization of Fusion Reactor Nucleonic Characteristics, Princeton Plasma Physics Laboratory Report MATT-1008 (1973).

¹²J. J. Ritts, M. Solomito, and D. Steiner, Kerma Factors and Secondary Gamma-Ray Sources for Some Elements of Interest in Thermonuclear Blanket Assemblies, Oak Ridge National Laboratory Report ORNL-TM-2564 (1970).

¹³D. G. Doran, Neutron Displacement Cross Sections for Stainless Steel and Tantalum Based on a Lindhard Model, Nuclear Science and Engineering 49, 130 (1972).

¹⁴G. M. McCracken and S. Blow, The Shielding of Superconducting Magnets in a Fusion Reactor, Culham Laboratory Report CLM-R120 (1972).

¹⁵W. G. Price, Jr., TCAP, A Transmutation Chain Analysis Program, unpublished.

¹⁶S. J. Bell and R. K. Adams, MATEXP, A General Purpose Digital Computer Program for Solving Ordinary Differential Equations by the Matrix Exponential Method, Oak Ridge National Laboratory Report ORNL-TM-1933 (1967).

¹⁷D. Jenkins, private communication.

¹⁸W. E. Alley, R. W. Gell, and R. M. Lessler, Semiempirical Neutron-Induced Reaction Cross Sections, Lawrence Livermore Laboratory Report UCRL-50484 (1968).

¹⁹C. M. Lederer, J. M. Hollander, and I. Perlman, Table of the Isotopes, Sixth Edition (John Wiley and Sons, Inc., New York, 1968).

²⁰Radiological Health Handbook, Revised Edition, (U.S. Department of Health, Education, and Welfare, Public Health Service, Rockville, Maryland, 1970).

²¹The Safety of Nuclear Power Reactors (Light Water-Moderated) and Related Facilities, op. cit., p. 4.8.

Chapter 11

Heat Transfer and Energy Conversion Systems

P. W. Davison[†] and K. C. Sokolosky^{*}

Introduction

As indicated in the title, this chapter is concerned with the process of converting the energy produced by the plasma and deposited in the materials surrounding it into electrical energy. The major portion of the chapter is devoted to the reactor heat transfer systems. This is because many features of these systems are unique to the fusion reactor concept in general and the toroidal fusion reactor in particular. In the conceptual designs of these systems, however, emphasis has been placed on using materials, techniques, and components within the current state of the art wherever possible even to the extent of some sacrifice in overall energy conversion efficiency. For the balance of the plant it has been possible to use systems and components that have been or are being developed for use in High Temperature Gas-cooled Reactor (HTGR) power plants.

Note that in this chapter the inherently cyclic nature of the plasma fusion power source (see Chapter 5) has been neglected. In Chapter 18 a scheme is presented by which the plant electrical output may be maintained at full power despite the periodic interruption of the plasma confinement.

Heat Transfer Systems

Reactor Heat Transfer Characteristics

In designing the reactor heat transfer system, four separate, though interrelated, regions were considered because of the differences in their characteristics. They are indicated on Fig. 11.1: the divertor and internal radiation shield wall, which are not in contact with the flibe, and, thus, can be cooled with helium having low (66°C or 95°C) inlet temperatures; the vacuum wall which is also cooled with helium, but with an inlet temperature high enough that the tube wall temperature remains above the eutectic solidus temperature (360°C); the flibe portion of the blanket, which is cooled with helium subject to the same inlet temperature limitation as the vacuum wall; and the outer radiation shield, which is made of concrete and cooled with low temperature (95°C max) helium. The physical characteristics of these regions are given in Table 11.1.

[†]Westinghouse Electric Corporation

^{*}New England Electric System

734274

Fig. 11.1 Cross Section of the Nuclear Island

Table 11.1. Physical Characteristics of the Reactor Coolant System

<u>Characteristic</u>		<u>Divertor</u>	<u>Radiation Shield</u>	<u>Vacuum Wall</u>	<u>Breeding Blanket</u>	<u>Concrete Shield</u>
Thermal Power	MW	134	693	323	4100	55
Helium Inlet Temperature	°C	66	95	360	360,460 ^a	66
Helium Outlet Temperature	°C	638	460	460	638	95
Helium Mass Flow Rate	kg/s	45.1	365.4	621.8	2207,987 ^a	365.4
Cooling Tube Material		PE-16	PE-16	PE-16	PE-16	SS-304
Max. Tube Mid-Wall Temperature	°C	680	680	680	680	NA
Cooling Tube Wall Thickness	cm	0.08	0.08	0.236,0.3 ^b	0.142 ^b	0.09
Cooling Tube Inner Diameter	cm	2.380	2.380	2.004,2.304 ^c	2.256	2.378
Average Tube Loading	kW/m	3.10	10.66	4.78	12.32	NA
Total Mass of Tubes	Mg	20.9	31.4	94.0	278	NA
Total Number of Tubes		5,448	24,720	28,128	37,458	NA
Total Length of Tubes	km	42.5	64.0	64.0	326.9	NA

Note: a) the cutlets of the radiation shield and the vacuum wall are fed to the inlet of part of the blanket.

b) wall contacting hot flibe have a 22.5 year corrosion allowance at 1.1 mils per year.

c) the vacuum wall "tubes" are really square channels with these internal dimensions.

The neutronic performance and power deposition characteristics for the various regions, except for the divertor, are given in Chapter 10, and for the divertor in Chapter 6. The power deposition per unit volume as a function of location is given in Fig. 11.2 for the inner radiation shield wall, the vacuum wall and the flibe blanket. As will be noticed, there is a variation of almost two decades between the inner and outer regions of the blanket. Also, power is generated in the structural materials and at about twice the amount per unit volume as for the flibe portion of the blanket. In addition, the toroidal configuration of the machine introduces a variation in the power deposition around the minor perimeter; a rough calculation discussed in Chapter 10 gives a factor of about two in variation of the wall loading per unit area. The above are unique features of this power source, and they have a significant influence on the design of the heat removal system.

The heat transfer equations used are given in Table 11.2. They were derived from several sources as indicated in the footnotes. The symbols used in the equations are identified in Table 11.3. The use of these equations and the heat transfer parameters employed are discussed in subsequent sections.

Radiation Shield Wall and Divertor

These two regions of the reactor have been considered together, even though they are physically separated, because their heat transfer characteristics are very similar. In both, the power deposition is largely on the front surfaces, and the heat flux is at a high enough level that special considerations must be given to handling it. Also, neither of these regions is in contact with the flibe, and thus, the inlet temperature of the helium coolant is not limited by the freezing point of the molten salt.

Radiation Shield Wall

Sixteen percent of the power generated in the plasma is in the form of electromagnetic radiation, mainly low energy X-rays. This power is deposited in a very thin layer ($< 0.1 \text{ cm}$)⁴ and will cause thermal stresses because of the thermal gradient between the front portion and back portion of the wall. In order to protect the load bearing vacuum wall from these thermal stresses, a relatively thin non-load bearing radiation shield wall is placed between the vacuum wall and the plasma. This wall also protects the vacuum wall from low energy ion bombardment and, in the event of a malfunction, runaway electrons from the plasma (see Chapter 18). This wall does not,

744401

Fig. 11.2 Power density calculated in the neutronics analysis.

Table 11.2. Heat Transfer Equations

Helium Heat Transfer

1. $M = \frac{P_{th}}{C_p \Delta T_R} = \frac{L q'_m}{C_p \Delta T_R}^*$
- 2.a $\Delta T_f = \frac{d}{L} \frac{\Delta T_R}{4 N_S}^*$
- 2.b $\Delta T_f = \frac{Q/A}{h}$
- 2.c $h = 0.021 \frac{K_S}{d} \left(\frac{\rho_s v_B d}{\mu_s} \right)^{0.8} \left(\frac{C_p \mu_s}{K_S} \right)^{0.4} **$
- 3.a $\frac{\Delta p}{p} = \frac{32}{\pi^2} \frac{f R_G T_{ave}}{p^2} \frac{M^2 L}{d^5}^*$
- 3.b $\frac{P_p}{P_{th}} = \frac{\Delta p}{p} \left(1 - \frac{1}{\gamma} \right) \frac{T_{in}}{\Delta T_R}^*$
- 3.c $f = 0.046 N_{Re}^{-0.2} = 0.046 \left(\frac{\pi d \mu}{4 M} \right)^{0.2} ***$
- 4.a $\frac{1 + \ln V}{V} = 1 - \left(\frac{16 K \Delta T}{q_m''' D^2} \right)_{Flibe}^*$
- 4.b $V = \frac{D^2}{D_c^2};$
- 4.c $D = d \left(\frac{\sigma + p}{\sigma} \right) + 2t_c$
- 4.d $V_{flibe} = 1 - V$
- 4.e $V_{ss} = V \left[1 - \left(1 - \frac{2 t_c}{D} \right)^2 \left(\frac{\sigma}{\sigma + p} \right)^2 \right]$

(continued)

Heat Generation

$$5. \quad q_m' = \frac{\pi}{4} D^2 F(q_m''') ,$$

$$F(q_m''') = \frac{1-V}{V} (q_m''')_{\text{Flibe}} + \left[1 - \left(1 - \frac{2 t_c}{D} \right)^2 \left(\frac{\sigma}{\sigma + p} \right)^2 \right] (q_m''')_{ss}$$

Temperature drop in Tube Walls

6. Due to heat flux from Flibe

$$\Delta T = \frac{(q_m')_{\text{Flibe}}}{2\pi K_{ss}} \ln \left(\frac{D}{d} \right) \quad ***$$

7. Due to heat generated in tube wall

$$\Delta T = \frac{(q_m''')_{ss}}{8 K_{ss}} \left(D^2 \ln \left(\frac{D}{d} \right) - \frac{D^2 - d^2}{2} \right) \quad ***$$

* Ref. 1

** Ref. 2

*** Ref. 3

Table 11.3. Symbols Used in Table 11.2.

C_p	= specific heat at constant pressure, $W \cdot \text{sec}/(\text{gm}^\circ\text{C})$
d	= inside diameter of tube, cm
D	= outside diameter of tube, cm
D_c	= outside diameter of heat transfer cell, cm
f	= friction factor (as defined in Ref. 3, p. 145)
h	= heat transfer coefficient, $W/(\text{cm}^2\text{C})$
K	= thermal conductivity, $W/(\text{cm}^\circ\text{C})$
K_s	= helium thermal conductivity at tube wall, $W/(\text{cm}^\circ\text{C})$
K_{ss}	= thermal conductivity of tube wall material, $W/(\text{cm}^\circ\text{C})$
L	= Length, cm
M	= mass flow rate per tube, gm/sec
N_S	= Stanton number, a heat transfer modulus
N_{Re}	= Reynolds number, a flow modulus
p	= pressure, atm.
P_p	= ideal pumping power, watts
P_{th}	= thermal power removed, watts
q'_m	= average power per unit length of tube, W/cm
q_m'''	= average volumetric heating rate, W/cm^3
Q/A	= heat flux, W/cm^2

(continued)

R_G = gas constant, $\text{atm}\cdot\text{cm}^3/\text{°C}$

t_c = tube wall thickness allowance for corrosion, cm

t_w = tube wall thickness, cm

T = temperature, K

T_{ave} = average temperature, K

T_{ex} = exit temperature, K

T_{in} = inlet temperature, K

v_B = bulk velocity, cm/sec

V = material volume fraction

V_c = coolant channel volume fraction (tube plus coolant)

V_{Fl} = flibe volume fraction

V_{He} = helium volume fraction

V_{ss} = tube wall material volume fraction

γ = ratio of specific heats

ΔT = temperature change, $^{\circ}\text{C}$

ΔT_f = film temperature drop in helium, $^{\circ}\text{C}$

ΔT_{Fl} = temperature rise in flibe, $^{\circ}\text{C}$

ΔT_R = temperature rise in helium = $T_{\text{ex}} - T_{\text{in}}$, $^{\circ}\text{C}$

ρ = density, gm/cm^3

(continued)

ρ_s = helium density at tube wall temperature, gm/cm³

ρ_{ss} = density of tube wall material, gm/cm³

σ = allowable stress, atm.

μ = viscosity, poise

μ_s = helium viscosity at tube wall temperature, poise

however, protect the vacuum wall from neutron and γ -ray induced radiation damage (see Chapter 10).

As indicated in Fig. 11.3, the radiation shield wall consists of a number of tubes running parallel to the major circumference of the reactor. These tubes are made of the Nimonic alloy PE-16 and are welded together for their own mechanical support and to prevent streaming of radiation between them. This wall is supported from the vacuum wall by means of thin strips of stainless steel running parallel to the axes of the tubes. The number of tubes required was obtained by dividing the tube outside diameter into the minor perimeter of the wall and multiplying by 24 - the number of reactor sectors.

The axes of the cooling tubes were placed parallel to the major axis of the reactor for two reasons. First, there is a thermal difference of 365°C between the inlet end and outlet end of the tubes, and it is considered more desirable to have this gradient parallel to the major axis than perpendicular to it. Second, and more important from the fabrication point of view, there is a variation of a factor of three in the length of the major perimeter between the inner and outer portions of the wall. If the cooling tubes were run perpendicular to the major axis, either the cooling tube diameters would have to be varied by a factor of three along their lengths, or the number of tubes would have to be varied by a factor of three between the inner and outer portions of the wall. Both of these solutions pose serious fabrication problems. With the tubes running parallel to the major axis, the variation in tube length can be corrected for by orificing at the inlet header. While it would be desirable to avoid this complication, it is a straightforward fabrication process that is frequently used in complex heat exchangers.

The outside diameter of the tubes was chosen to match that of the cooling tubes in the flibe blanket. Since no allowance need be made for the tube wall loss due to corrosion by the molten salt, the tube wall thickness is smaller than for the blanket tubes. The tube wall thickness given has been determined on the basis of the allowable stress and the hoop strain due to the 50 atmosphere helium coolant pressure; no allowance has been made for erosion due to low energy ion bombardment.

The helium inlet temperature is 95°C , the outlet temperature of the concrete shield. This arrangement is used for two reasons; first, the higher inlet temperature gives a higher outlet temperature for the same outlet tube midwall temperature, and second, it allows the 55 MW of energy developed in the concrete shield to be used productively. The helium outlet temperature was determined using the equations in Table 11.2 with the constraint that the tube midwall temperature not exceed 680°C (1256°F). In

Fig. 11.3 Detail cross-section of Radiation shield wall, vacuum wall and inner part of the flibe blanket.

order to bring the helium temperature up to the required value of 638°C , the outlets from the radiation shield wall tubes are fed into tubes in the inner sections of the flibe blanket (see p. 273).

Divertor

While the cooling of the divertor system is, of course, a necessary requirement, the heat removed from this portion of the reactor is relatively small, 134 MW or 3.9% of the total, and thus only a general conceptual design has been developed for this part of the reactor cooling system. The requirements for cooling the walls of the entrance of the divertor, since they have a direct "view" of the plasma, are the same as for the radiation shield wall, and thus this portion of the cooling system has been designed as a continuation of that for the radiation shield wall. In the main channel of the divertor, the bulk of the energy will be deposited in the form of line radiation on the inner surfaces of the walls. The heat fluxes are expected to be low enough that the heat can be removed by helium circulating in tubes thermally connected to the outer surfaces of the walls; these tubes could be arranged to run either horizontally or vertically. The number of cooling tubes required (shown in Table 11.1) is estimated, based on a vertical arrangement. The exhaust of the divertor will contain a high temperature, low density gas that must be cooled to a temperature compatible with the input requirements of the vacuum system; this cooling will be accomplished by a very low impedance gas-to-gas heat exchanger.

The helium flow through the divertor cooling system is as follows: the low temperature helium (66°C) is directed first through the gas to gas heat exchanger and the walls of the exhaust chamber, and then into the tubes cooling the walls of the throat. If the variation in heat flux through the walls is great enough to require it, the helium will be first directed to the tubes in the high heat flux regions, and the outlet will be located in the low heat flux regions. (This arrangement is used for cooling the flibe blanket to obtain a lower temperature drop in the helium film and, thus, a higher outlet helium bulk temperature for a given wall temperature.) It is expected that the heat fluxes will be low enough that the desired helium outlet temperature (638°C) can be achieved without using the scheme of routing the outlet to the back portion of the flibe blanket as in the case of the radiation shield wall.

Vacuum Wall

With respect to heat transfer, the design of the vacuum wall cooling system is very similar to that of the radiation shield wall. The wall cooling channels run

parallel to the major circumference of the reactor. The helium inlet temperature must be higher to avoid freezing the flibe on the blanket side of the wall. The helium outlet temperature is limited by the maximum allowed wall temperature of 680°C . The helium is combined with that from the radiation shield wall and then passed through the inner sections of the blanket to bring the final outlet temperature up to 638°C .

The mechanical design of the vacuum wall had to consider the mechanical loads imposed by the vacuum being on the inner side and the molten salt at a pressure slightly higher than atmospheric on the other. In addition, the thermal stresses had to be considered. The selected design is shown in Fig. 11:3.

Flibe Blanket

All of the tritium bred for fuel and about three quarters of the thermal power produced by the reactor come from the flibe blanket. Therefore, particular attention was given to the design of this portion of the heat transfer system. Because of the interaction between the mechanical arrangement (the heat transfer aspects) and the neutronics characteristics (the heat generation and breeding aspects) of the blanket, the design was performed in a series of iterations. Some discussion of the preliminary calculations will be given, but the main emphasis in this section will be on the selected design.

Preliminary Calculations

Preliminary calculations were performed to furnish inputs to the initial plant design and to ANISN calculations (see Chapter 10) of the neutronic performance of the blanket. These calculations of necessity required many assumptions, approximations and simplifications.

The blanket was assumed to be the annulus of a cylinder of length equal to 2π times the major radius of the vacuum wall with no end effects; the inner radius was equal to the minor radius of the outer part of the vacuum wall. That is, no attempt was made to include in any detail the effects of the geometrical arrangement of the actual blanket. Also, only a crude approximation was made of the cooling tube supports and other structures; these were estimated and included as a smeared-out average of 1% of the total blanket volume for ANISN calculations and material weight estimates.

For the heat transfer calculations, the method used by Hopkins and Melese-d'Hospital¹ was adapted for use with flibe and stainless steel (the Nimonic alloy PE-16 was selected later) cooling tubes. In their study, lithium and niobium tube walls

were used, and the volumetric heating rates were about the same in both materials. Thus, the lithium and niobium could be lumped together in their heat transfer calculations. However, in the case of flibe and stainless steel, the volumetric heating rate of the latter is between two and three times that of the former and thus the two materials were treated separately.

The blanket was considered as made up of a number of independent unit cells. Each cell, as indicated in Fig. 11.4, consisted of a cooling tube at the center of a cylinder of flibe. The diameter of the cell, D_c , was determined from the volumetric heating rate in the flibe, q_m''' , and the heat transfer parameters using the following relationship:

$$D_c^2 \left[\ln D_c - (\ln D + 1/2) \right] = \frac{D^2}{2} - 8 \left(\frac{k \Delta T}{q_m'''} \right)_{\text{flibe}} \quad (11.1)$$

This relationship, which was obtained from Eq. 4a in Table 11.2, applies to the case of a long cylinder with an adiabatic outer surface, uniform heat generation in its volume and uniform heat removal through its inner surface. These conditions are met to a good approximation in the actual blanket if the flibe is stagnant. As can be seen in Fig. 11.4, in an array of unit cells they must overlap and/or have gaps between them. The spacing between centers of the cells was selected so that the volume of the overlaps equaled the volume of the gaps, which is a reasonable approximation for heat removal calculations. The equilateral triangular arrangement shown was selected since the sizes of the individual gaps and overlaps are less than for a square array. There may be a better arrangement, but it was not considered worthwhile to seek it because of the preliminary nature of the calculations. The tube spacings for the triangular array were obtained by setting the cross-sectional area of the triangle equal to one half that of the cylinder, which gives

$$y = \left(\frac{\pi^2}{12} \right)^{1/4} D_c = 0.9523 D_c, \quad z = \left(\frac{3}{4} \right)^{1/2} y = 0.8247 D_c \quad (11.2,3)$$

Because of the variation of q_m''' in the z direction, (see Fig. 11.2) the value of D_c used to calculate z was obtained using the q_m''' at a point midway between rows of tubes.

The results obtained in one of the preliminary calculations are shown in Fig. 11.5. The heat flux per centimeter of tube length, q_m' (Eq. 5 of Table 11.2), and the tube spacings, y and z , are plotted as a function of distance from the front of the vacuum wall. The key heat transfer parameters are given in Table 11.4 except that the

744474

Fig. 11.4 Cooling tube unit cell model.

744476.

Fig. 11.5 Tube spacing vs distance from vacuum wall.

Table 11.4. Key Heat Transfer Parameters of Preliminary Design

Reactor Thermal Power	5000 MW
Reactor Major Radius	10.5 m
Reactor Minor Radius	3.2 m
Blanket Thickness	76 cm
Neutron Energy Current Density	2 MW/m ²
Average Helium Pressure	50 atm.
Helium Inlet Temperature	360°C
Helium Outlet Temperature	620°C
Stanton Number	0.003
Max. Tube Wall Temperature	663°C
Max. Allowable Stress in SS (Type 316)	734 atm.
Thermal Conductivity of Flibe	0.0084 w/cm°C
Max. Temperature Rise in Flibe	200°C
Flibe Corrosion Allowance in Tube Wall Thickness	0.0028 cm/yr
Tube Wall Thickness	0.215 cm
Tube Outer Diameter	2.25 cm

temperature rise in the flibe was 400°C. The cooling tube outside diameter is 2.25 cm and inside diameter 1.82 cm, and the tubes are run parallel to the axis of the plasma. The results were used to obtain a preliminary layout of the cooling tubes and the material volume fractions for another ANISN calculation.

A study was also made for optimizing the blanket cooling system design. The bases used for optimization were: helium pumping power, flibe volume fraction, materials costs and fabrication costs. The parameters varied were: cooling tube diameter, coolant tube wall thickness, coolant pressure and heat load per tube in high heat flux regions.

If the Table 11.2 Eqs. 1, 2a, 3a, 3b, 3c, and 5 are combined, the following equation results:

$$\frac{P_p}{P_{th}} = 1.92 \times 10^{-3} \frac{R_G}{C_p^{1.8}} \left(1 - \frac{1}{\gamma}\right) \frac{\mu^{0.2}}{N_S^{2.8}} \frac{T_{ave} T_{in}}{\Delta T_f^{2.8}} \left(\frac{\sigma + p}{\sigma_p}\right)^2 \frac{D^{1.6}}{(1 - 2t_c/D)^2} F(q_m''')^{1.8} \quad (11.4)$$

and the symbols are as defined in Table 11.3. Although this equation is more complicated than the individual ones in Table 11.2, it does show more explicitly the dependence on the variables at our disposal - that is, the temperatures, pressure and tube diameter. It is interesting to note that Eqs. 3a and 3b in Table 11.2 seem to indicate an inverse fifth power relationship between P_p/P_{th} and tube diameter. However, the above equation indicates a direct 1.6 power relationship except for small diameters when the term $1/(1 - 2t_c/D)^2$ approaches infinity faster than the $D^{1.6}$ term approaches zero. The latter is because of the corrosion allowance, t_c , which is independent of tube diameter. Even if this allowance is made zero, the $(1-V)/V$ term in $F(q_m''')$ causes P_p/P_{th} to increase as D approaches zero.

The variation of P_p/P_{th} with pressure is also seen to be different from that implied by Eqs. 3a and 3b. While it is close to an inverse square relation at low pressures, it flattens out as σ approaches the allowable stress, σ_a . The term $(1-1/\gamma)$ decreases slightly with increasing pressure but this is only a 6% change in going from 10 to 300 atmospheres. The variation of P_p/P_{th} with σ is similar to that with p . However, increases in σ can only be obtained by reducing the maximum tube midwall temperature, and as a consequence, reducing the helium exit temperature or the film drop, ΔT_f , or by reducing the design life, or by changing to another material for the tube walls.

Reducing the temperatures, T_{ave} and T_{in} , and increasing the film drop, ΔT_f , would reduce P_p/P_{th} . However, the minimum inlet temperature is set by the freezing point, or solidus temperature, of the flibe. The average temperature is set by the inlet

and outlet temperature, and the latter should be as high possible to achieve a high steam temperature and good thermal efficiency in the turbine generators. Likewise, the film drop is set by the exit gas temperature and the wall temperature for the selected allowable stress. Thus, while there are possibilities of trade-offs among these various parameters, they are somewhat complex, and it was decided to fix the inlet, outlet and maximum wall temperatures and use an approach suggested by Fraas.⁴ That is to arrange each tube in two or more runs so that the inlet portion is in the front, high power density, part of the blanket, and the outlet is in the back, low power density, part. This would give low values of P_p/P_{th} where P_{th} is high and high values of P_p/P_{th} where P_{th} is low. By combining Eqs. 2a, 3a, 3b and 3c of Table 11.2 and assuming that the mass flow rate, M , the tube diameter and the Stanton Number, N_S , are the same in both sections of the tube we obtain

$$\left(\frac{P_p}{P_{th}}\right)_2 = \left(\frac{P_p}{P_{th}}\right)_1 \left[\left(\frac{\mu_2}{\mu_1}\right)^{0.2} \frac{(T_{ave} T_{in})_2}{(T_{ave} T_{in})_1} \frac{(\Delta T_f)_1}{(\Delta T_f)_2} \right] \quad (11.5)$$

All of the temperature terms and the viscosity term in the numerator are larger than those in the denominator; thus, $(P_p/P_{th})_2$ is always larger than $(P_p/P_{th})_1$. To obtain $(P_p/P_{th})_{total}$ we can use Eq. 1 of Table 11.2 and the relation

$$\left(\frac{P_p}{P_{th}}\right)_{total} = \left[\left(\frac{P_p}{P_{th}}\right)_1 (P_{th})_1 + \left(\frac{P_p}{P_{th}}\right)_2 (P_{th})_2 \right] \times \frac{1}{(P_{th})_{total}} \quad (11.6)$$

with $M_1 = M_2 = M$ to get

$$\left(\frac{P_p}{P_{th}}\right)_{total} = \left(\frac{P_p}{P_{th}}\right)_1 \times \left[\frac{(\Delta T_R)_1}{(\Delta T_R)_{total}} + \frac{(\Delta T_R)_{total} - (\Delta T_R)_1}{(\Delta T_R)_{total}} \times \left(\frac{\mu_2}{\mu_1}\right)^{0.2} \times \frac{(T_{ave} \times T_{in})_2}{(T_{ave} \times T_{in})_1} \frac{(\Delta T_f)_1}{(\Delta T_f)_2} \right] \quad (11.7)$$

At first glance, it would appear that there would be a minimum in this function as $(\Delta T_R)_1$ is varied between $(\Delta T_R)_{total}$ and zero. However, as will be seen below, this is not the case, and $(P_p/P_{th})_{total}$ drops monotonically. This can be understood qualitatively when it is realized that we are transferring more and more of the tube to the low power density region. We are paying for it, though, by having to add more tubes to remove the total thermal power required. These effects are shown in Fig. 11.6, which was calculated for a 1.50 cm OD tube with the parameters given in Table 11.4 except a corrosion allowance, t_c , of 0.014 cm was used.

The number of tube welds per KWe was calculated by determining the thermal power removed by each tube, multiplying it by the thermal to electric conversion

744105

Fig. 11.6 Fabrication costs and helium pumping power vs temperature rise in inlet section of tube.

efficiency, assumed to be 0.38, and dividing the result in kilowatts into the number of welds per tube, assumed to be 2. Using Eq. 1 from Table 11.2 and values of 5.195 watt-sec/gm $^{\circ}\text{C}$ for C_p and 260°C for $(\Delta T_R)_{\text{total}}$, we get

$$\text{Number of Welds/ k W}_e = \frac{2 \times 10^3}{0.38 C_p M (\Delta T_R)_{\text{total}}} = \frac{3.9866}{M} \quad (11.8)$$

The mass flow rate, M , can be obtained by combining equations 1, 2a, 4c and 5 in Table 11.2 to give

$$M = \frac{\pi}{16} \left(\frac{\sigma}{\sigma + p} \right) \left(1 - \frac{2 t_c}{D} \right) \frac{D^3 F(q_m''')_1}{N_{S p} (\Delta T_f)_1} \quad (11.9)$$

The value of $F(q_m''')$, is 43 W/cm^3 based on ANISN calculations.

The tube material per kW_e was calculated by determining the total length of each tube, multiplying by the mass per unit length and dividing by 0.38 times the thermal power removed by each tube. By combining equations 1, 2a, 4c and 6 from Table 11.2 and using Mass/unit length = $\frac{\pi}{4} (D^2 - d^2) \rho_{ss}$ where ρ_{ss} is the density of stainless steel, we obtain

$$\text{Mass/kW}_e = \left[1 - \left(\frac{\sigma}{\sigma + p} \right)^2 \left(1 - \frac{2 t_c}{D} \right)^2 \right] \frac{\rho_{ss} \times 10^3}{0.38 F(q_m''')_1 (\Delta T_R)_t} \left[(\Delta T_R)_1 + (\Delta T_R)_2 \frac{(\Delta T_f)_1}{(\Delta T_f)_2} \right] \quad (11.10)$$

The results of these calculations are also shown on Fig. 11.6.

While this set of calculations is not thorough enough to draw any final conclusions, they do indicate the possibility of an optimum tradeoff between helium pumping power and fabrication costs for $(\Delta T_R)_1$ in the region of 150 to 200°C .

A similar set of calculations was made in which the tube outside diameter was the independent variable, and $(\Delta T_R)_1$ was fixed at 200°C . The other parameters were as given in Table 11.4 except that a temperature rise of 400°C in the flibe and a corrosion allowance of 0.084 cm were used. The results of these calculations are shown in Fig. 11.7. Also shown in the figure is the flibe volume fraction V_{F1} , for the complete blanket. Here an optimum tradeoff in the neighborhood of 2.5 cm is indicated.

Another set of calculations was made with the helium pressure as the independent variable. The ΔT_R was 200°C (no second section), tube outside diameter 2 cm and a corrosion allowance of 0.063 cm . A correction should have been made for the fact that the film drop, ΔT_f , in the helium would have to be reduced as the tube wall thickness

744404

Fig. 11.7 Blanket cooling system parameters vs tube outer diameter.

increased (because of the increased power produced in the tube wall and the longer heat conduction path for the power from the flibe) to maintain the same midwall temperature; or the allowable stress should have been reduced. However, because of the preliminary nature of these calculations, this correction was not made. If it had been, the falloff of P_p/P_{th} with increasing pressure would have been less sharp. The results of this set of calculations are shown in Fig. 11.8. The flibe volume fraction is not included because under the conditions used it is independent of pressure. The stainless steel volume fraction, V_{ss} , does vary and thus is included. The mass of stainless steel/kWe was calculated using the following:

$$\text{Mass/kW}_e = \left[1 - \left(\frac{\sigma}{\sigma + p} \right)^2 \left(1 - \frac{2t_c}{D} \right)^2 \right] \frac{\rho_{ss} \times 10^3}{0.38 F(q_m''')} \quad (11.11)$$

The tradeoffs among the various parameters do not seem very clear-cut in this instance. The fabrication cost items do not vary markedly and tend to cancel each other, though the tube welds should probably be given the greater weighting factor. This would indicate a weak minimum at about 100 atmospheres. The main tradeoff is between V_{ss} , which we should like to be small to improve tritium breeding, and the P_p/P_{th} . Another factor, which favors lower pressures but has not been included here, is the cost of the rest of the heat transfer system - large ducts between the blanket and the steam generators and the large steam generators themselves. Based on these considerations a pressure of 50 atmospheres was tentatively selected.

The above calculations were made using a constant value of the Stanton Number, a heat transfer modulus, $N_S = 3 \times 10^{-3}$. This approximation is a good one for a limited range of conditions. This is shown in Fig. 11.9 where the previous calculation of P_p/P_{th} vs. $(\Delta T_R)_1$ is compared with one made using $N_S = \pi d^2 h / (4MC_p)$ with h being determined from Eq. 2.c of Table 11.2. It was found that N_S ranged from 2.76×10^{-3} for $(\Delta T_R)_1 = 260^\circ\text{C}$ to 3.84×10^{-3} (in the second section) for $(\Delta T_R)_1 = 103^\circ\text{C}$. When this equation for N_S is substituted in the equation 11.4 above for P_p/P_{th} as a function of temperature, pressure, and diameter, we obtain:

$$\frac{P_p}{P_{th}} = 535 R_G \left(1 - \frac{1}{Y} \right) \frac{\mu_s^{1.6}}{K_s^{2.1} C_p^{0.4}} \left(\frac{T_s}{T_B} \right)^{2.8} \frac{T_{ave} T_{in}}{\Delta T_f^{3.5}} \left(\frac{\sigma + p}{\sigma p} \right) \left(\frac{2D^3}{1 - 2t_c} \right)^2 \left(F(q_m''') \right)^{2.5} \quad (11.12)$$

744101

Fig. 11.8 Blanket cooling system parameters vs helium pressure.

744102

Fig. 11.9 Pumping power vs temperature rise in inlet section.

The results of calculations using this formula and the one for $N_S = 3 \times 10^{-3}$ for variations in D are shown in Fig. 11.10 for the parameters given in Table 11.4 except that the helium outlet temperature was 563°C , and the corrosion allowance was zero. Also included is a curve calculated with the above equation for a corrosion allowance of 0.063 cm. As can be seen, the approximation of $N_S = 3 \times 10^{-3}$ is quite good over a limited range near 1.8 cm but gives significantly misleading results outside that range. Also, the effect of the corrosion allowance can be seen. It is most significant at very small diameters, and drops off slowly above 1 cm. These results indicate a sharper minimum in the P_p/P_{th} curve, and it is located at a smaller diameter than for the $N_S = 3 \times 10^{-3}$ curve. However, if the latter curve is compared with Fig. 11.7, it will be noticed that adding the second section to the tube (to bring the outlet temperature up to 630°C) and increasing the temperature drop in the flibe shifts the location of the minimum upward and outward - the latter effect is because of the increase in the term $\frac{1 - V}{V} (q_m''')_{\text{Flibe}}$ in the P_p/P_{th} equation. This indicates that the location of this minimum is influenced by the detailed design of the system and that this type of calculation should, as intended here, be used as a guide for, rather than a specification for, the more detailed design.

In summary, based on these preliminary calculations the following parameters were tentatively selected:

Cooling Tube Outside Diameter	2.25 cm
Average Helium Pressure	50 atm.
Temperature Rise in Front Portion	200°C

Selected Design

In the preliminary designs just discussed, the detailed mechanical arrangement of the blanket was given only minor consideration. However, in developing the selected design, just the opposite approach was taken. As mentioned earlier, one of the unique features of a toroidal machine is the significant variation in length between the inner and outer major circumference. This variation is increased in the present design by the inclusion of the divertor. This effect is illustrated in Fig. 11.11 which shows a top view of the midplane cross section of one of the 24 sectors of the reactor. The major portion of the blanket is above and below the midplane and does not appear in this cross section. However, the location of the inner edge of the blanket, at the entrance to the divertor, is indicated by a dashed line. The circumferential length at that point is

744104

Fig. 11.10 Pumping power vs cooling tube outside vs diameter for N_s constant and for N_s not constant.

⊙ £

734420

Fig. 11.11 Top view of one blanket sector - mid-plan cross section.

163 cm as contrasted with 403 cm at the outermost edge of the blanket. This variation of a factor of almost $2\frac{1}{2}$ was accommodated in the design of the Radiation Shield Wall and Vacuum Wall (see above) by running the cooling tubes parallel to the major circumference and using orifices to compensate for the variation in tube length. One of the main considerations in arriving at that design was that the tubes had to be formed together side by side to form solid walls. This constraint is not imposed on the arrangement of the tubes in the blanket. Thus, more latitude was available for selecting an optimum configuration.

After considering a number of different arrangements for the cooling tubes, the one illustrated in Fig. 11.12 was selected. The tubes run parallel to the minor circumference of the vacuum wall and are grouped in sections. Cross sections of a nominal section of the blanket are shown in Figs. 11.13, 11.14, and 11.15. As shown in Fig. 11.13, the individual tubes run from the inlet header to the front portion of the blanket and then in a serpentine configuration back to the outlet header. Both headers are located in the back portion of the blanket. The tubes are arranged in rows as indicated in Figs. 11.14 and 11.15. The basic direction of the flibe flow is parallel to the axis of the plasma, which is parallel to the main toroidal magnetic field, and across the cooling tubes. The width of each section is 100 cm at the vacuum wall and the depth is 74 cm from the back of the vacuum wall to the outside wall. The length of the sections along the direction of the plasma axis varies from 403 cm at the back of the outermost section to 147 cm at the back of the innermost section. As shown in Fig. 11.12, there are a total of 22 sections in the main part of the blanket. Also shown is a blanket section located in the central reactor core. The outside configuration of that section differs from the one described above, but the basic arrangement of the cooling tube and flibe flow is the same.

The reasons for selecting this arrangement for cooling the blanket were as follows: the tubes were arranged with their long runs parallel to the minor circumference of the vacuum wall on the basis of both mechanical and heat transfer considerations. As for the former, this arrangement in combination with grouping the tubes in relatively short sections (see Fig. 11.12) allows straightforward accommodation of the wide variation in sector width by adjusting the number of parallel tubes in each section. This also allows the blanket to be fabricated in relatively small units - the largest one, on the outer periphery of the torus, has maximum dimensions of 403 cm in length, 122 cm in width and 80 cm (including radiation shield wall) in depth. These units, or sections, can be shop fabricated and then assembled in the field to form

744400

Fig. 11.12 Vertical cross section of blanket.

734501

Fig. 11.13 Nominal blanket section parallel to axis of plasma.

744418

Fig. 11.14 Nominal blanket section parallel to minor circumference.

744403

Fig. 11.15 Nominal blanket section perpendicular to vacuum wall.

complete sectors, which can then be joined together to form the complete reactor structure. This will result in significant savings in fabrication and quality assurance costs.

With regard to heat transfer considerations, the sector arrangement allows for matching closely the wide variation in power deposition in the blanket because the tube spacing can be adjusted in the direction parallel to, as well as perpendicular to, the plasma axis. The arrangement of tube runs parallel to the minor circumference, gives better heat transfer from the flibe, and, thus, higher flibe volume fractions than can be obtained by flowing the flibe parallel to the tubes and yet minimizes the adverse magnetic effects on flow and heat transfer. This is because the major portion of the flibe flow path is parallel to the main toroidal field.

The partitions shown in Figs. 11.13, 11.14, and 11.15 serve two functions - to furnish mechanical support and to serve as guides for the flibe flow. The partitions that are perpendicular to the vacuum wall are primarily for furnishing mechanical support for that wall and the cooling tubes. The primary purpose of those running parallel to the vacuum wall is, on the other hand, that of channeling the flow of the flibe; without such channeling, the flibe flow would be mainly through the back portion of the blanket where the tube spacing is the largest, and thus the flow impedance the lowest. Such a condition would have three detrimental effects; first, it would reduce the heat transfer coefficient in the front portion of the blanket where the power deposition is the highest; second, it would increase the turnover time of the flibe in the front portion where the tritium production is the greatest; and third, it would allow stagnant pockets to develop where crud buildup would occur.

(a) Heat Transfer from the Flibe

Before going into a more detailed discussion of the selected design, it is desirable to consider the heat transfer characteristics of the flibe. This is because the major portion of the power produced in the blanket is produced in the flibe, and the efficient removal of this power is a key design objective. In the preliminary design calculations discussed earlier in this section the flibe was assumed to be stagnant. Consequently the only significant flibe heat transfer parameters were the thermal conductivity and temperature. However, in the final design the flibe must be circulated through the blanket. The primary reason for this is to allow the flibe to be processed to remove tritium and unwanted reaction products (see Chapter 14). It also, as will be discussed below, improves the heat transfer properties.

In the mid 1960's, when it was found that the heat exchangers for the Molten Salt Reactor Experiment were not meeting their design objectives⁵ it appeared that there might be some anomalies in the heat transfer characteristics of molten salts (Ref. 6, p. 231). However, when a detailed investigation was made,⁵ it was concluded that the discrepancies were probably due mainly to the thermal conductivity values used in the design being erroneously high. It was later confirmed (Ref. 7, p. 11 and Ref. 8, p. 4) that the earlier values of the thermal conductivities were high by factors of 3 (for the fuel) and 6 (for the coolant). Subsequent heat transfer measurements (Ref. 8, p. 40 and 152) concluded that molten salts do "indeed perform as typical heat transfer" fluids.

It is well known that the characteristics of a heat transfer system are determined not only by the fluids involved but also by the geometrical arrangement of the system. This is particularly true for operation with fluid flows in the transition region between pure laminar flow and fully developed turbulent flow. This is illustrated in Fig. 11.16, which is based on data from Tables 10.1 and 10.2 of Ref. 9. These data are for air; however, the two curves for flow normal to tube banks "can probably be employed with confidence at moderately high Prandtl numbers" (Ref. 9, p. 122, and p. 7). (The Prandtl number, $N_{Pr} = \mu C_p / K$, is a fluid property modulus which has values of about 0.7 for air and about 20 for flibe at 800°C.) Also, the shape of the curves for flow normal to staggered tube banks and for flow inside tubes is very similar to those given in Figs. 14.8 and 14.9 on page 233 of Ref. 6 for molten salt. Thus it is believed that the discussion below is valid for flibe.

The curves in Fig. 11.16 are plots of the product $N_{Nu} (N_{Pr})^{-1/3}$ as a function of N_{Re} . The Nusselt number, $N_{Nu} = hD_h / K$ is a heat transfer modulus; the Reynolds number, $N_{Re} = v \rho D_h / \mu$, is a flow modulus; and N_{Pr} is as defined above; D_h is the hydraulic diameter of the flow passage and the other coefficients are as defined in Table 11.3. These curves indicate how the heat transfer properties vary with flow, or, if the other coefficients are held fixed, how the heat transfer coefficient, h , varies with fluid velocity, v . As will be noticed, for a Reynolds number in the range of 2000 to 4000 (which is where our flibe flow falls, as will be shown below) the heat transfer is significantly improved by using the staggered tube bank arrangement. This appears to be due to the staggered arrangement forcing the transition to turbulent-type heat transfer at a much lower (by a factor of more than 10) Reynolds number than is the case for in-line tube banks or inside tube flow. The significance of this with regard to the effect of magnetic fields on heat transfer from flibe will be discussed below.

It has been pointed out in several papers (e.g., Refs. 10 and 11) that when a conducting fluid is flowing in a magnetic field, the field will exert a force that is

734422

Fig. 11.16 Heat transfer modulus vs Reynolds number.

opposed to eddies within the fluid and will tend to damp out turbulent flow with a resulting detrimental effect on heat transfer from the fluid. Although there have been no experimental studies of magnetic damping in molten salts,¹¹ a number of studies have been conducted with other conducting fluids (see references given in Refs. 10 and 11), and the relationships developed from those studies and given in Ref. 10 will be used here.

A dimensionless quantity that is a measure of the ratio of the magnetic forces to the viscous forces is the Hartmann number

$$H \equiv aB \sqrt{\frac{\sigma}{\mu}} \quad (11.13)$$

where a is half channel height (or radius) in the direction perpendicular to the flow, σ is the electrical conductivity of the fluid, μ is the fluid viscosity and B the magnetic field component, in consistent units. There are three Hartmann numbers corresponding to the perpendicular, parallel and, in an axisymmetric case, radial components of the magnetic field. The first two are of interest here.

Anticipating calculations that will be discussed below, the mean temperature of the flibe in the front portion of the blanket is about 690°C and the hydraulic diameter of the flow channel is 3.786 cm. These give $a = 1/4 D_h = 9.46 \times 10^{-2}$ m, $\sigma = 268$ mho/m (based on data given in Ref. 7), $\mu = 1.435 \times 10^{-2}$ Pa-sec at 690°C (based on interpolation of data given in Ref. 12, p. 81 and, with B in teslas,

$$H = 1.29 B \quad (11.14)$$

The component of the magnetic field perpendicular to the flow path was calculated by U. R. Christensen for several points in the blanket including one that was considered to have the highest value, i.e., toward the center portion of the reactor and near one of the vertical field coils. That value was 1.58 tesla, which gives

$$H_1 = 2.04 \quad (11.15)$$

but that location is also in a low heat flux portion of the blanket. In the highest heat flux portion of the blanket the perpendicular field was 0.91 tesla, which gives

$$H_1 = 1.18 \quad (11.16)$$

The requirement for full laminarization of flow in circular pipes is (see p. 6 of Ref. 10)

$$H_{\perp} \gtrsim \frac{N_{Re}}{500} \quad (11.17)$$

This gives values of critical Reynolds numbers of 1020 maximum and 590 in the highest heat flux region. As discussed above, for flow normal to staggered tubes, these should be reduced by a factor of at least 10. However, in the highest heat flux regions N_{Re} is about 3000, and there should be no serious effect on the heat transfer in any case. Using Blum's correlation equation (Ref. 10, Appendix A), which holds for Reynolds numbers greater than 1.5 times the critical value, i.e., greater than 880 in this case,

$$\begin{aligned} (N_{Nu})_m &\approx (N_{Nu})_o \left(1 - \frac{6 H_{\perp}^2}{N_{Re}} \right) \\ &\approx (N_{Nu})_o (1 - 0.003) \end{aligned} \quad (11.18)$$

This is a negligible correction.

Now to consider the effect of the parallel magnetic field which varies from 4.4 tesla at the vacuum wall of the outermost section of the blanket to 14 tesla at the vacuum wall of the innermost section. Thus, the parallel Hartmann number varies from 5.7 to 18. Using the relation (p. 51 of Ref. 10)

$$\begin{aligned} (N_{Nu})_m &\approx (N_{Nu})_o \left(1 - \frac{0.15 H_{\parallel}^2}{N_{Re}} \right) \\ &\approx (N_{Nu})_o [1 - (0.002 \text{ to } 0.02)] \end{aligned} \quad (11.19)$$

Here again, the correction is not significant. Thus, it is concluded that the effects of the magnetic field can be neglected in the heat transfer calculations.

Before leaving the subject of the effects of the magnetic fields, it should be mentioned that consideration must be given to the voltage induced when a conducting fluid crosses a magnetic field.¹³ This effect depends on the detailed geometrical configuration and will be examined later in this section. Suffice it to say here that these voltages can be kept to the order of several tenths of a volt and should cause no corrosion problems.

The viscosity of flibe is a fairly sharp function of temperature (Ref. 12, p. 81), and this factor must be considered. The other coefficients are either constant or very weak functions of the temperature. The method commonly used to correct for this viscosity variation is to multiply the Nusselt number calculated using the mixed mean temperature of the fluid by the factor (M_s/M^n) where n is a function of the Prandtl number and also depends on whether the fluid is heating or cooling (Ref. 9, p. 90).

Based on the considerations discussed above, it has been concluded that the best relationship to use for determining the flibe heat transfer coefficient is the following:

$$N_{Nu} = C_h N_{Re}^{0.6} N_{Pr}^{1/3} (\mu_s/\mu_m)^n \quad (11.20)$$

where C_h is a constant depending on the transverse pitch and longitudinal pitch of the cooling tubes. (See Fig. 7.5, p. 127 of Ref. 9). Substituting the physical coefficients and solving for the heat transfer coefficient, we obtain

$$h = \frac{C_h K^{2/3} C_p^{1/3} v_{max}^{0.6} \rho_m^{0.6}}{D_h^{0.4} \mu_m^{0.27}} \left(\frac{\mu_s}{\mu_m} \right)^n \quad (11.21)$$

where the terms are as defined above and in Table 11.3.

(b) Detailed Design

To date only approximate calculations have been made of the variation in power per unit area around the minor circumference of the vacuum wall for a toroidal configuration. As reported in Chapter 10 the peak may be 10% above the nominal, while the value may fall by 50% near the divertor. A calculation has been reported¹⁴ for a toroidal reactor with a homogeneous and isotropic volumetric neutron source of toroidal shape. The results of that calculation showed that the maximum variation of neutron flux around the minor circumference of the vacuum wall was about 10%. The actual variation for our reactor may be somewhere between these cases. Since the detailed design of the heat transfer system for the blanket is sensitive to the level of the neutron flux incident on the vacuum wall, it has not been possible to do a detailed design of the complete blanket cooling system. Instead, a detailed design has been made

for what is considered to be a nominal section of the blanket. The section chosen is the one located at a major radius of 11 meters (see Fig. 11.12) at the top, or bottom, of the blanket. As indicated in Fig. 11.17, this location is close to the point of nominal wall loading. A discussion is given, however, of how this nominal design can be modified to accommodate different wall loadings.

Using for guidance the results of the preliminary calculations given earlier in this section, a number of specific cooling tube arrangements were analyzed. Because of the complexities involved no attempt was made to optimize rigorously the design. Instead, using the optimization studies performed in the preliminary design phase, the following set of design goals was selected:

Flibe volume fraction in front portion, about 75%

Tube diameter, between 2 and 3 cm.

Total helium pressure drop, about one atm.

In addition the following constraints were observed:

Total flibe flow rate, 4.06×10^9 gm/hr, minimum

Maximum tube midwall temperature, 680°C

Maximum voltage developed due to flibe flow in magnetic field, 0.1 V

Helium inlet temperature, 360°C

Helium outlet temperature, 638°C

Tube wall thickness allowance for flibe corrosion, 0.063 cm

Thermal power developed in blanket and shield, 5305 MW.

The flibe flow rate was dictated by the requirement of complete turnover of the flibe in the reactor at least twice an hour. The maximum tube midwall temperature was selected on the basis of a compromise between a high allowable stress in the tube wall material and a helium outlet temperature high enough to allow 538°C (1000°F) steam to be obtained from the steam generators without unduly complicating their design. The steam temperature was somewhat arbitrarily chosen because it is close to the highest in use in turbine generators now being operated. The induced voltage in the flibe was selected on the basis of not exceeding 0.4 volt when the flibe flow rate is increased to maintain the helium temperature during the reactor field reversal cycle; in Ref. 11, 0.4 volt is considered adequately low to avoid wall corrosion problems. The helium inlet

744461

Fig. 11.17 Variation of neutron wall load from nominal.

temperature was fixed by the freezing, or solidus, temperature of eutectic flibe and the outlet temperature by the steam generator design. The tube wall thickness allowance for corrosion was picked on the basis of molten salt test loop results for stainless steel (Ref. 15, p. 166) that indicate a corrosion equivalent to 1.1 mils or 0.0028 cm per year; a plant life of 30 yrs at a 75% capacity factor was assumed. The thermal power developed in the blanket and shield is based on that required to give 2030 MW net electric.

The total thermal power of 5305 MW is obtained as the sum of 688 MW of radiation on the radiation shield wall and divertor, 55 MW of shield heating, and 4562 MW developed by neutron interactions with the walls and blanket. Of the latter figure, 462 MW are produced in the material of the vacuum wall and radiation shield wall based on an equivalent thickness of 0.993 cm (see Fig. 11.3), an area of $4\pi^2 \times 1100 \times 363 = 1.576 \times 10^7 \text{ cm}^2$ and a power deposition of 29.5 W/cm^3 . This leaves 4100 MW that are produced in the flibe blanket and its structure. The nominal blanket section is $100/(2 \times 360) = 1/22.6$ of a sector, and a sector is $1/24$ of the reactor. Thus, the power produced in a nominal section is 7.559 MW. As indicated in the preliminary calculations, the details of how this power is removed have a strong influence on helium pressure drop and temperature distributions in the system.

To satisfy the helium pressure drop constraint we can vary the number of tube runs, n , i.e., the power per tube, and the number of zigzags per run, n_z , i.e., the length of the tube. Also the tube diameter could be varied, but it was decided to fix that on the basis of using a standard size, 1 in. or 2.54 cm O.D., with an allowable stress of 749 atm. (11,000 psi, based on data given in Ref. 19), a pressure of 50 atm. and a corrosion allowance of 0.063 cm, the inside diameter of the tube is 2.256 cm. The pressure drop can be determined using Eq. 3.a in Table 11.2. The parameters in that equation, in addition to those given above, are as follows: the friction factor, f , 6.5×10^{-3} (one quarter (see page 145 of Ref. 2) of the value given in the figure on page 3-60 of Ref. 16 for a Reynolds number of 1×10^5 and a pipe roughness factor 1.5×10^{-4} foot); the gas constant for helium, R_G , $2.0500 \times 10^{-5} \text{ atm-cm}^3/(\text{gm}^\circ\text{C})$ derived from the value of $8.31432 \times 10^7 \text{ erg}/(\text{gm mole}^\circ\text{C})$ given on page A-10 of Ref. 17; the average temperature, 772 K; the flow rate per tube, from Eq. 1 in Table 11.2,

$$M = \frac{7.559 \times 10^6}{n \times 5.195 \times 278} = \frac{5234}{n} \text{ gm/sec} \quad (11.22)$$

the hydraulic length of a tube run,

$$L_h = \left(\frac{r_i}{3.6} - 2 x_t D + 2 \left(\frac{L}{d} \right)_{\text{bend}} d \right) n_z + L_{\text{in}} \quad (11.23)$$

(see Figs. 11.13 and 11.18), substituting weighted nominal values for the distance from the toroidal axis, $r_i = 393$ cm, and the transverse spacing between tubes, $x_t D = 6.86$ cm; the length of the inlet run, $L_{\text{in}} = 54$ cm (the outlet of the last zigzag is directly to the outlet header) and a value of 25 for the (L/d) of a 90° bend (see Ref. 18, p. 531), we obtain

$$L_h = 208.25 n_z + 54 \quad (11.24)$$

Substituting the above values in Eq. 3.a in Table 11.2 and solving for n ,

$$n = (651 n_z + 169)^{1/2} \quad (11.25)$$

This equation gives the following:

n_z	n
5	58
7	69
9	78
11	86

From the point of view of fabrication costs, it would be desirable to have n as low as possible. However, the heat transfer requirements set a limitation on this. Thus, before proceeding further, we must return to considerations of the heat transfer. Both the heat transfer from the flibe and to the helium must be considered. They are, of course, interrelated and could be handled in a combined set of equations. However, because of the complexity of that approach the calculations were performed separately and several iterations made until they matched. The heat transfer to the helium turns out to be limiting because of the relatively low maximum temperature allowed for the midwall of the tubes. However, the flow of and heat transfer from the flibe has a stronger influence on the physical arrangement and will be discussed first.

As shown earlier in this section, the heat transfer coefficient for the flibe for a given configuration is a function of its velocity. The minimum total flow rate is set by the requirements for flibe processing, but the velocity in the blanket depends on how this flow is distributed. A compromise must be made between a high velocity for good

744399

Fig. 11.18 Detail of the sinuous cooling tube path.

heat transfer and a low velocity to avoid corrosion due to induced voltages and to keep the pressure drops low. The desirability of parallel flow through the 24 sectors is clear. Also, there is a definite advantage to bringing the flibe into the outer edge of a sector and flowing it in parallel streams symmetrically above and below the reactor midplane (see Fig. 11.12). As discussed above in the section on Selected Design, the main part of each sector is divided into 22 sections - 11 above and 11 below the reactor midplane - and each section is divided into four regions. Within each section it is necessary that the main flow paths be parallel to the major circumference of the reactor. Also, it is desirable that the mass flow rate be the same in each region. The question remains as to whether sections should be connected in series or parallel. After looking at the two alternatives it was decided to use the series connection since it gives a high enough velocity to give good heat transfer but not high enough to cause voltage and pressure drop problems. The path selected was the following (see Fig. 11.19): The flibe enters at the outer region of the outermost section, flows parallel to the major circumference to the end of the section, then down to the next inner region of the section, back to the inlet end and over to the next section and so on to the innermost section. At the innermost section, the flow is down to the next region toward the vacuum wall and then back, zigzag fashion, to the outermost section of the blanket. By selecting this direction of flow, the low tritium content flibe is in contact with the higher temperature tube walls in the back part, and the high tritium content flibe in the front part spends less time in the blanket.

With the series arrangement for flibe flow, within each half-sector, the mass flow rate is the same in all regions of the blanket. Thus, for a total mass flow rate of 4.06×10^9 gm/hr, the flow through each region will be 8.46×10^7 gm/hr or 2.35×10^4 gm/sec. The velocity of the flibe, however, depends on the flow cross section of the channel. The minimum flow cross section in a channel is $(X - mD) \bar{r}_i / 3.6$, see Figs. 11.18 and 11.20, where X is the height of the channel perpendicular to the vacuum wall, m is the number of rows of cooling tubes in the channel in the direction perpendicular to the vacuum wall and $\bar{r}_i$ is the mean minor radius of the channel. Thus, the product of the maximum velocity and the density is

$$v_{\max} \rho = 3.6 M / (X - mD) \bar{r}_i \quad (11.26)$$

The hydraulic diameter of the channel is

$$D_h = 4(X - mD)x_\ell / (m\pi) \quad (11.27)$$

744398

Fig. 11.19 Flibe flow path through
one-half sector.

744097

Fig. 11.20 FLIBE flow across tubes with geometrical parameters used in flow and heat transfer equations.

where x_ℓ is the center-to-center spacing between rows of tubes in the direction of flibe flow divided by the outside diameter of the tube. Substituting these relations in the equation for h given previously, we obtain for the heat transfer coefficient

$$h = 1.958 \frac{m^{0.4} C_h K^{2/3} C_p^{1/3} M^{0.6}}{x_\ell^{0.4} (X - mD) \bar{r}_i^{0.6}} \frac{\mu_s^n}{\mu_m^{(n+0.27)}} \quad (11.28)$$

and substituting the values for K , C_p and M

$$h = 45.08 \frac{m^{0.4} C_h}{x_\ell^{0.4} (X - mD) \bar{r}_i^{0.6}} \frac{\mu_s^n}{\mu_m^{(n+0.27)}} \quad (11.29)$$

In the innermost flow channel the heat flux from the flibe to the cooling tubes and vacuum wall is

$$\left(\frac{Q}{A}\right)_{\text{flibe}} = \left(\frac{x_\ell X - m\pi D/4}{x_\ell + m\pi}\right) (q_m''')_{\text{flibe}} \quad (11.30)$$

Dividing this relation by that above for the heat transfer coefficient, we obtain for the film temperature drop in the flibe

$$(\Delta T)_{\text{flibe}} = \frac{x_\ell^{0.4} (\bar{r}_i)^{0.6} (X - mD) (4x_\ell X - m\pi D)}{180.31 C_h m^{0.4} (x_\ell + m\pi)} \left(\frac{\mu_m^{(n+0.27)}}{\mu_s^n}\right) (q_m''')_{\text{flibe}} \quad (11.31)$$

For the other channels the term x_ℓ in the denominator is deleted because all of the heat is removed by the cooling tubes. The power generated in and removed by the partitions and walls of the flibe channels has not been included and will be discussed below. This relationship was used to adjust the location of the partitions parallel to the vacuum wall, i.e., the values of X , to give a nearly uniform flibe bulk temperature. It is interesting to note that even though the cooling tube wall temperature varies along its

length, the variation in flibe bulk temperature is much less because of the zigzag arrangement of the tube runs and because of the presence of the viscosity terms, μ , in the ΔT equation; the viscosity exponent n is negative, about -0.37 , and the viscosity varies inversely with temperature.

The values of the parameters x_ℓ , m and C_h depend on the cooling tube spacings parallel to and perpendicular to the vacuum wall, i.e., the number of tube runs, n , and the number of zigzags per run, n_z , which, as shown above, are interrelated. The parameters x_ℓ and m also enter into the heat flux equation, and the value of the heat flux from the flibe has a strong influence on the film temperature drop in the helium coolant and, in turn, the tube midwall temperature. The tube midwall temperature is a more critical parameter than the flibe bulk temperature. The value of x_ℓ is the length of the section divided by the quantity nD . Because of the presence of helium headers in the ends of the section (see Fig. 11.14), the effective length is not the same in all four regions of the section. Also, the value of x_ℓ in the outer region is approximately twice that in the other regions in order to have an even number of tube runs, m , in each region and an uneven number of zigzags (so that the inlet and outlet helium headers can be on opposite sides of the section).

A series of calculational iterations were made to select the values for the parameters in the above equations. The values chosen as best meeting the various constraints are given in Table 11.5. The values of n_z , n and m are the same for all four regions. They were selected on the basis of helium pressure drop and tube midwall temperature and are as follows: $n_z = 7$, $n = 69$ and $m = 2$.

The flibe heat transfer parameters for the central portion of a nominal blanket cooling section are given in Table 11.6. These parameters will be somewhat different at the ends of the section because of the presence of the headers and the turning of the flibe flow from or to the next region. However, because of the fact the flibe is flowing, it is believed these variations will not have a significant effect.

Intermixed with the flibe heat transfer calculations were those for the helium portion. It was found that the heat transfer to the helium was, as mentioned above, more critical than that from the flibe. The temperature profiles along the length of a nominal cooling tube are given in Table 11.7 and Fig. 11.21. The temperature rise in the helium was calculated using Eq. 1 of Table 11.2. The film temperature drop in the helium was calculated using Eqs. 2.b and 2.c in Table 11.2. The heat flux, Q/A , used was that from the flibe, as given in Table 11.5, plus that from the heat generated in the tube walls. The temperature gradient in the tube walls was calculated using Eqs. 6 and 7 in

Table 11.5.

Physical Parameters for a Nominal Blanket Cooling Section

Region	$\bar{x}_l$	$\bar{r}_i$	X	C_h	n	μ_m	μ_s	$(q_m''')_{\text{flibe}}$
		cm	cm			poise	poise	W/cm ³
1 (inner)	1.46	372.2	12.5	0.45	-0.37	0.0734	0.646	11.0
2	1.52	386.3	14.9	0.46	-0.37	0.0734	0.310	5.0
3	1.64	404.5	20.7	0.48	-0.37	0.0734	0.231	1.8
4 (outer)	2.88	427.4	24.4	0.50	-0.37	0.0734	0.204	0.48

Table 11.6.

Flibe Heat Transfer Parameters
in a Nominal Blanket Cooling Section

Region	D_h	$v_{\max}$	N_{Re}	N_{Nu}	N_{Pr}	h	Q/A	ΔT_{flibe}
	cm	cm/sec				W/cm ² °C	W/cm ²	°C
1 (inner)	6.90	16.14	2880	65.5	20.4	0.0797	20.3	255
2	9.50	11.75	2890	86.1	20.4	0.0761	14.3	194
3	16.31	7.05	2970	104	20.4	0.0536	8.58	160
4 (outer)	35.42	5.40	4950	154	20.4	0.0365	5.06	139

Table 11.7.
Temperature Profile along a
Nominal Cooling Tube

Region of Blanket	End of Tube Run Number	Distance from inlet Header cm	Helium Drop Temp. °C	Film Drop °C	Inner Wall Temp. °C	Wall Temp. Drop °C	Tube Midwall Temp. °C
4	Header	0	360	12	372	12	378
4-1	Inlet	54	376	75	451	30	456
1	1	162	435	75	510	30	525
1	2	274	493	73	566	30	581
2	3	387	536	51	587	20	597
2	4	503	576	50	626	20	636
3	5	622	599	26	625	12	631
3	6	744	621	25	646	12	652
4	7	875	635	14	649	6	652

734421

Fig. 11.21 Temperature profile along nominal cooling tube.

Table 11.2; the gradient to the middle of the wall was taken as half the total gradient in the wall. The reason for the "bumps" in the tube wall temperature profile is that the heat flux varies from region to region but is almost constant within a given region. The shape of the "bumps" in the transition between regions was estimated.

As will be noticed in Table 11.7 and Fig. 11.21, the maximum tube midwall temperature is about 30°C below the design maximum of 680°C . An approximate calculation showed that going to a tube configuration with $n_z = 5$ and $n = 58$ would cause the midwall temperature to exceed 680°C . If a tube configuration with $n_z = 5$ and n greater than 58 (and thus, a lower tube pressure drop) were used, the temperature could be adjusted to 680°C . However, it was decided that because the calculation is for a nominal cooling tube, the 30°C margin should be kept to allow for variations from the nominal. An analysis has not been made, however, to determine whether this margin is the correct one to use.

The above description has been for a nominal blanket cooling section. The same design procedure can be used for the other sections of the blanket once the proper wall heat loadings and material heating rates have been determined. In those sections with higher heating rates the centerline to centerline spacing of the tubes will be smaller in the direction parallel to and also, possibly, perpendicular to the vacuum wall. In those sections with lower heating rates the reverse would be true. It might also be desirable to change the diameter of the tubes. The section arrangement selected allows these adjustments to be made in a straightforward manner.

As mentioned earlier in this section, the above refinements to the design of the blanket cooling system have not been made. In order to obtain a number of the characteristics given in Table 11.1, the following assumptions were made: the blanket was assumed to be a simple torus with a major radius of 1100 cm and a minor radius of 360 cm (at the plasma side of the radiation shield wall). From a mechanical point of view, this is considered a good assumption because the vacuum wall surface area calculated on this basis gives a value within 1% of that computed using a code developed by W. G. Price, Jr. to match the actual configuration of the wall as shown in Fig. 11.1. The other assumption was that the cooling tube spacings and thus the number of cooling tubes and the material volume fractions in the nominal blanket section were average values for the whole blanket. This is believed to be a reasonable assumption, but this can not be confirmed until a complete design can be made.

Using these assumptions, the volumes and weights of the flibe, Nimonic Alloy PE-16 and helium and the number and weight of the cooling tubes for the entire blanket

were obtained by calculating them for the nominal blanket section and multiplying by the ratio of the vacuum wall area for the torus to that of the nominal section.

Shielding Blanket

The cross section of the shielding blanket is shown in Figs. 11.1. and 11.12. The design of this blanket is discussed in Chapter 10. The 55 MW of thermal power deposited in the blanket materials is removed by helium flowing in Type 304 stainless steel tubes with outside diameters of 2.54 cm and inside diameters of 2.45 cm. The latter was calculated for the hoop strain due to a helium pressure of 50 atm. and an allowable stress of 1430 atm. (21,000 psi) obtained from Fig. 7 of Ref. 20. As indicated in Fig. 11.22, the helium enters the shielding blanket at 66°C from the hydrogenic species removal system (see Chapter 14) and exits to the radiation shield wall at about 95°C.

The design of the shielding blanket cooling system has not been developed in detail. However, it would be similar in many respects to that for the flibe blanket cooling system. The power deposition in the shielding blanket will also vary both in the direction perpendicular to the vacuum wall and in the direction parallel to the minor circumference; but the total power developed will be only about 1.4% of that removed by the flibe blanket cooling system. The pressure at the outlet of the compressor in the hydrogenic species removal system and the pressure drops in the shielding blanket and radiation shield wall cooling systems will be adjusted so that the outlet pressure of the helium from the radiation shield wall matches that from the vacuum wall.

Helium Circulation System

A one line block diagram of the heat transfer system is shown in Fig. 11.22. The main function of the helium circulation system is to interconnect the various systems, subsystems and components in order to transfer the power developed in the reactor system to the steam generators. An important secondary function is to pass part of the helium through a cleanup system, which is described in Chapter 14, where hydrogenic species picked up in the reactor and steam generators are removed. In the latter, the helium is also passed through two feedwater heaters to make efficient use of the heat that must be removed to reduce the helium temperature from the 360°C required at the inlet to the breeding blanket cooling tubes to the 66°C required by the cleanup system, divertor, and shield.

744475

Fig. 11.22 Schematic of helium coolant system.

All of the helium passes from the reactor through the steam generators. The bulk of it, about 87%, is returned to the cooling systems of the breeding blanket and the vacuum wall at 360°C. The remainder is returned through two of the feedwater heaters and the cleanup system to the cooling systems of the divertor and the concrete blanket shield at 66°C. The split of the flow between these two return paths is determined by the cooling requirements of the associated reactor subsystems.

The objectives of the design of the helium circulation system were to determine approximately the dimensions of the headers and ducts and to identify the key components such as valves and circulators. The main guidelines followed were, for the former, to achieve a low pressure drop around the system consistent with the physical and functional requirements and, for the latter, to satisfy the primary operational and safety requirements. In designing the system, it was considered as divided into the following categories or subsystems: the headers within the reactor; the main ducts between the reactor and steam generators; the helium cleanup loop; and the circulators and key valves.

Headers within the Reactor

The headers within the blanket proper are shown schematically in Fig. 11.23 for half of one sector and are shown in cross section for a nominal section of the blanket in Figs. 11.13, 11.14 and 11.15. For the blanket cooling, there are two categories of headers, the branch headers that feed the cooling tubes and the main headers that feed the branch headers. For the vacuum wall and radiation shield wall separate inlet headers, shown in Fig. 11.24, are needed. The one for the latter is mounted inside the former (see Fig. 11.14) to furnish insulation from the flibe. One common outlet header is used. As discussed above in the sections on the radiation shield and vacuum walls, this outlet header is connected to the main inlet header for part of the blanket cooling system.

As in the case of the blanket cooling system, the design of the reactor header system has been carried out only for the nominal sector. As above, that sector is the one centered at a major radius of 11 meters, identified as 6A on Fig. 11.23. However, because the main headers serve the other 10 sections as well, some assumptions had to be made concerning the power removed by those sections. Those assumptions were as follows: the total power removed from one sector was one twenty-fourth of the total reactor power generated in the corresponding portion of the reactor, i.e., blanket, radiation shield wall or vacuum wall; for sections 1 through 6 and 12, (see Fig. 11.12) the power removed was proportional to the vacuum wall area of the section, i.e., the neutron flux was

744477

Fig. 11.23 Schematic of flibe cooling tube header system.

744478

Fig. 11.24 Schematic of radiation shield wall and vacuum wall cooling tube header system.

uniform; for sections 7 through 11, the total power removed by those five sections was one-half the total sector power minus the sum of the power from sections 1 through 6 minus one-half the power from section 12.

The pressure profile along each blanket header (to or from section 6A) was obtained by calculating the pressure drop in each segment, i.e., between sequential branches using Eqs. 1 and 3a in Table 11.2. The pressure loss going into or from a branch was obtained by using the nomogram on page 531 of Ref. 18 for sudden contractions or enlargements. This approach is probably all right for the main headers, but it would have been more precise to have treated the branch headers as manifolds. The results obtained are given in Table 11.8. The inside diameters of the headers, also given in that table, were selected as a compromise between low pressure drops and displacement of flibe from the blanket or shielding material from the blanket shield.

Main Ducts

The main ducts, which conduct the helium from the reactor to the steam generators and back, are shown in the simplified plan and elevation views in Fig. 11.25. The helium from the blanket outlet headers is fed through downcomers, one for each of the 24 sectors, to a ring duct beneath the reactor floor. The helium flow varies a factor of 12 around the ring duct and thus the duct can be tapered to conserve material. After a straight horizontal run the duct is teed to feed the 12 steam generators through risers. The return path from the circulators at the lower end of the steam generators to the reactor blanket and vacuum wall inlet headers is the reverse of that described above except that because of the lower flow rate and temperature, the return duct diameters are 11% less. The parameters for the hot leg ducts are given in Table 11.9 and for the cold leg in Table 11.10.

Helium Cleanup Loop

The main function of the helium cleanup loop is to remove hydrogenic species from the helium. The part of the subsystem that accomplishes this function is described in Chapter 14. The helium that is passed through this loop, about 13% of the total flow, is also used to supply power to two of the feedwater heaters, Numbers 5 and 6 (see Figs. 11.22 and 11.26). In passing through the feedwater heaters the helium temperature is reduced from 360°C to 66°C, the latter temperature being limited by the water inlet temperature, 50°C or 122°F. By this means 628 MW of thermal power that would otherwise have to be discharged to the atmosphere are used productively.

The helium pressure drop through the feedwater heaters has been estimated to be one atmosphere based on the calculated value for the steam generators, 0.69 atmosphere

Table 11.8.

Blanket Header Parameters

Line or Header	Total No. for Reactor	Inside Diameter cm	Length m	Pressure Drop, atm
Inlet Line to Blanket	24	70	2	0.30
Main Inlet Header in Blanket	48	25	8.3 5.95*	
Branch Inlet Header in Blanket	528	15	2.80**	
Inlet Line to Concrete Shield	24	40	1.0	0.30
Vacuum Wall Header	48	18	12.8	
Radiation Shield Wall Header	48	10	14.5	
Outlet Header from Vacuum Wall and Radiation Shield Wall	48	15	17.5	
Branch Outlet Header in Blanket	528	15	2.47**	0.52
Main Outlet Header in Blanket	48	25	11.9 7.14*	
Outlet Line from Blanket	24	80	2.0	

* To or from nominal section.

** For nominal section.

ELEVATION VIEW

PLAN VIEW

734502

Fig. 11.25 Simplified plan and elevation view of the plant.

Table 11.9.

Hot Leg Duct Parameters

Section	Total Number	Inside Diameter cm	Length m	Pressure Drop atm
Downcomer	24	150	16.7	0.15
Half of Ring Header	2	150+366*	54.0	0.15
Duct to Steam Generator Bldg.	1	366	31.7	0.15
Duct to Steam Generator Risers	2	315+200*	18.0	0.15
Risers to Steam Generators	12	200	23.0	0.15

* Diameter varies along the length.

Table 11.10.

Cold Leg Duct Parameters

Section	Total Number	Inside Diameter cm	Length m	Pressure Drop atm
Risers from Circulators	12	180	10.0	0.15
Duct from Circulator Risers	2	180+280*	18.0	0.15
Duct to Ring Header	1	325	31.7	0.15
Half of Ring Header	2	325+135*	54.0	0.15
Riser to Reactor	24	135	16.7	0.15

* Diameter varies along the length.

(see Appendix C). The pressure drop in the cleanup system is one atmosphere (see Chapter 14). The pressure drop in the ducts connecting the various components of this subsystem has been estimated to be 0.2 atmosphere based on the values calculated for the main return ducts given above.

Circulators and Valves

As indicated in Fig. 11.22, circulators are used to raise the pressure of the helium from the outlet of the steam generators to 50.95 atmospheres, required to give a pressure of 50 atmospheres in the middle of the blanket cooling system. The pressure drops in the individual tubes, manifolds, heaters and ducts have been discussed in previous sections and for the steam generators in Appendix C. These can be summarized as follows:

Cooling Tubes	1.00 atm.
Outlet Manifolds and Headers	0.52 atm.
Ducts to Steam Generators	0.15 atm.
Steam Generators	0.69 atm.
Ducts to Reactor	0.15 atm.
Inlet Manifolds and Headers	0.30 atm.
Total	2.81 atm.

Combining Eqs. 1 and 3.b in Table 11.2 we obtain for the ideal pumping power

$$P_p = \left(1 - \frac{1}{\gamma}\right) \frac{\Delta P}{P_{in}} T_{in} C_p M \quad (11.32)$$

where the symbols are as defined in Table 11.3. The values of the parameters are $\gamma = 1.6569$; $\Delta P = 2.81$ atm.; $p = 50$ atm.; $T = 633$ K; $C_p = 5.195$ W sec/g^oC; and $M = 3.279 \times 10^6$ g/sec. These give an ideal pumping power requirement of 241 MW. It has been decided to use electric motors to drive the circulators, and an electrical to mechanical conversion efficiency of 96% has been assumed. The electrical power requirement will be 251 MW, which is 67% of the total estimated station services requirement.

There are twelve steam generators, and one circulator will be mounted on the outlet of each unit. Each unit will require a 20.8 MW or 27900 HP motor. This size is only about 16% larger than circulators currently being designed for use in High Temperature Gas Cooled Reactors.²¹

The locations of key valves in the helium circulation system are indicated in Fig. 11.22. They are those valves that are required for major operational and safety-related functions. In each sector of the reactor there are shutoff valves in the inlets and outlets to each of the main portions of the cooling system. These are required to isolate these portions in the event of a serious malfunction such as a large leak so that cooling can be maintained to the other portions. This subject is discussed further in Chapter 16. Shutoff valves at the inlets to the steam generators and outlets from the circulators are needed in the event that a steam generator must be shut down for repair or maintenance; this allows the operation of the plant to continue at reduced output using the remaining steam generators. The valves in the recirculation line on the steam generators (see Appendix C) are to adjust helium flow during startup, part power or shutdown operations. The valves in the helium cleanup loop are to allow for isolation of part or all of that system if required for repair or maintenance. The valve requirements are tabulated in Table 11.11.

Steam Generators

The reference design cycle will require twelve supercritical, double reheat steam generators in parallel service. The design of these units is given in Appendix C and will be only summarized here. Each unit will be 117 feet in height and 12.5 feet in diameter with an electric motor driven single stage axial flow helium circulator mounted at the base. Design of the units by Foster Wheeler Corporation is in keeping with present day technology used in the HTGR program.

Heat will be conveyed to the steam generators by helium gas circulating through the reactor heat transfer surfaces at a rate of 25.7 million pounds per hour. The reactor exit gas temperature will be 1180°F, compatible with our use of PE-16 and stainless steel heat transfer tubing. The hot helium will then be admitted to each steam generator through a 6-foot diameter duct that will direct a portion of the gas flow upward over the second reheater and allow the remainder to pass downward over the first reheater. The ratio of gas flow over each surface will be controlled mechanically by a damper in the uppermost section of each steam generator and will serve as one means of temperature control. An alternate means of temperature control will be by attemperation.

The gas flows will recombine in an area just below the first reheater and will continue downward over the supercritical fluid coil and into the helium circulators. In one pass through the steam generators, the helium temperature will drop 494°F and its pressure will drop 10.1 psi.

Table 11.11.
Heat Transfer System Valve Requirements

Valve Location	Total Required	Operating Temp. (°C)	Flow Diameter (cm)	Flow Rate per Valve (kg/sec)
Into Divertor	24	66	40	2.7
Into Concrete Blanket	24	66	40	13.3
Into Vacuum Wall	24	360	60	50.6
Into Breeding Blanket	24	360	90	69.9
Exit Vac. & Rad. Shield Wall	24	460	15	63.9
Exit Divertor	24	638	100	2.7
Exit Breeding Blanket	24	638	100	133.8
Into Steam Generator	12	638	200	273.1
Steam Generator Recirculating Valve	12	360	91	273.1(max)
Circulators to Reactor	12	360	200	241.1
To Feedwater Heaters	12	360	80	32.0
Helium Cleanup System	2	66	25	1.39
Helium Cleanup System By-Pass	1	66	50	382.6

Feedwater will be admitted to the lower section of each steam generator at 561°F, pass upward through the helically wound supercritical coil, and exit to the turbine at 1000°F and 3690 psi. Steam flows to the first and second reheaters will enter their respective heat exchangers at the upper level of each steam generator and will also exit to the turbine at 1000°F.

Steam generator heating surfaces (scaled to the final steam flows from the values given in Appendix C) will consist of:

Supercritical coils	-727,533 ft ²	- 1 1/8"	OD tubes
First reheater	-117,852 ft ²	- 1 3/4"	OD tubes
Second reheater	- 95,447 ft ²	- 1 3/4"	OD tubes

The overall duty will be about 16.8 billion Btu's per hour transferred from the helium to the steam cycle fluid. All heat exchanger surfaces will be helically wound with the exception of the second reheater, which will be of vertical tubular arrangement. Also, all helically wound surfaces will transport their fluids in counter flow to the helium passage over them.

The steam generator tubes and tubesheets will be fabricated of Incoloy 800 or, as discussed in Chapter 15, an Incoloy 800-Copper-Incoloy-800 sandwich. Tube ends will be accessible for plugging or other maintenance reasons due to the location of tube sheets outside of the helium pressure vessel. Internal inspection of each steam generator will be made possible through the central core pipe that is located in the supercritical coil and reheater sections.

Each helium duct entering and leaving the steam generators will be equipped with a shutoff isolation damper. In conjunction with this feature, each helium circulator discharge duct will also contain a bypass arrangement whereby helium will be allowed to recirculate through a steam generator during its transition from inactive to active status, or during part power operation.

Energy Conversion System

Steam System

The steam cycle of the plant is illustrated schematically in the full load heat balance in Fig. 11.26. The turbine-generator combination considered for our prototype fusion power plant will be a cross-compound 3600 rpm unit with supercritical steam conditions of 3690 psia and 1000°F at the throttle, followed by two reheats to 1000°F. Each shaft of the cross-compound concept will be driven by four double-flow

Fig. 11.26

turbines arranged in tandem and coupled to a single generator. Specifically, the high pressure shaft will include a very high pressure (VHP), intermediate pressure (IP), and two low pressure (LP) turbines, with a very similar arrangement of high pressure HP, intermediate pressure (IP), and two low pressure (LP) turbines constituting the low pressure shaft. Each shaft will be approximately 200 feet long. The electrical outputs will be very nearly balanced with gross outputs of 1235 MW and 1170 MW respectively from the high and low pressure shaft generators. The design of this turbine-generator system was supplied by the Large Turbine Division of the Westinghouse Power Systems Company and is within the current state of the art.

Throttle steam flow of nearly 14.6 million pounds per hour will enter the VHP turbine and give up 118 Btu's per pound as it expands through the stages. The result of this energy conversion will be 498 MW of "wheel" output before the steam leaves the VHP turbine at 1155 psia and 680°F. Approximately 10 percent of the flow will then be diverted for regenerative feedwater heating in No. 1 heater, and the remainder will be returned to the first reheat sections of the steam generators where its temperature will be restored to 1000°F.

The reheated steam will then pass through the HP turbine and generate an additional 437 MW of "wheel" power. At two separate points along the turbine expansion path a total of 1.4 million pounds per hour of steam will be extracted for No. 2 and No. 3 feedwater heaters, and the remaining 11.6 million pounds per hour will be returned to the second reheater where 1000°F steam temperature will be restored for the second time.

After leaving the second reheater, one half of the steam flow will be directed to each of the two IP turbines. The No. 4 extraction flow amounting to 0.65 million pounds per hour will be removed from the IP turbine flows and an additional 0.62 million pounds per hour will be diverted from the IP discharge lines to supply boiler feed pump turbine steam. The IP section "wheel" output will be 483 MW.

Steam will be discharged from the IP turbines at 113 psia and 695°F. The four LP turbines will receive 10.4 million pounds per hour of steam in parallel flow and generate an additional 990 MW of "wheel" power before the steam will be directed to the condensers at a "Used Energy End Point" of 1047 Btu per pound. There will be no extraction points from the LP turbines.

Condensed steam from the LP turbines and the boiler feed pump turbines will be collected in the condenser hotwell and mixed with cycle drains, leakages and other miscellaneous small flows. Actually, the hotwell will serve as a reservoir from which the condensate pumps will draw their suctions for discharge through the oil coolers,

hydrogen coolers, air ejectors and gland steam condensers. Flow at this point will be 11.15 million pounds per hour; temperature increase through the auxiliary heat exchangers will be 13°F ; and line pressure will be 200 psi.

A condensate polishing system will be incorporated in this portion of the cycle to establish and maintain the high degree of water quality necessary for supercritical operation.

Condensate flow from the auxiliary heat exchangers will enter the deaerator pumps and be discharged at 300 psi through No. 6 and No. 5 helium feedwater heaters into the open type deaerator (which is also the No. 4 feedwater heater). After leaving the deaerator at 380°F , the condensate route will be into the boiler feed pump suction. At a boiler feed pump discharge pressure exceeding 4000 psi, the feedwater will be pumped through No. 3, No. 2 and No. 1 feedwater heaters and ultimately into the steam generator supercritical coil sections. The final feed temperature will be 561°F .

As a point of interest, the boiler feed pump work performed on the feedwater will produce a 15 Btu per pound enthalpy increase in the 14.6 million pounds per hour of pumped water.

A unique feature of the six stage feedwater cycle is the use of extraction steam only in the four high pressure heaters and deaerator. Hot helium gas will supply the initial two stages of feedheating. Actually, the helium gas will enter No. 5 heater at a flow rate of 3.26 million pounds per hour and 680°F ; will pass through No. 5 and No. 6 in series; and will be discharged to the helium clean-up system at 150°F . Under these conditions, the helium will heat up 11.1 million pounds per hour of condensate from 122°F to 318°F . Due to the high capacity of the cycle, all feedwater heaters will be arranged in parallel strings.

Net Cycle Performance

The total internal power requirement is anticipated to be 375 MW. Major equipment demands can be listed as: helium circulators, 251 MW; refrigeration system, 100 MW; cooling tower fans, 15 MW; and other cycle usage including secondary pumps, plasma ignition and heating systems, lighting, instrumentation and controls, 9 MW. Overall this amounts to 15.6 percent of the gross generation (10.4% due to the helium circulators) and represents a performance penalty when compared to a fossil fueled cycle utilizing but 2.5 percent of its output for station service. (The boiler feed pump, which would normally consume another 62 MW of auxiliary power, will be steam driven.)

The following inputs are used to evaluate the overall plant performance:

Reactor Power	5305	MW
Gross Generation	2404.7	MW
Station Service	374.7	MW
Net Electric	2030	MW
Net Efficiency	38.3	%
Net Heat Rate	8917	Btu/kW hr.

Auxiliary Power Systems

When the plant generating units are in operation above 150 MW, the auxiliary power will be supplied from the generator via the station service transformer that will reduce a portion of the 26 kV generator output to bus requirements of 13.2 kV, 4.16 kV and 480 V. The remaining generator output will be stepped up to 345 kV transmission voltage at two 1400 MVA transformers. At this level of operation, all 115 kV standby power will be discontinued, and the plant will be electrically self sufficient.

When decreasing load on the generating units, a reverse procedure will be followed to insure standby power for station service throughout the lower load range and specifically at the time of turbine tripout.

In the event of an unforeseen incident that would cut the availability of both generator station service and standby power, two automated 4 MW-4160 V diesel generators would supply emergency power for a safe and orderly shutdown of the plant after turbine tripout. A third diesel generator would be available for backup, and each one could be started, synchronized and loaded to an emergency bus within 10 seconds.

During such an eventuality, it would be desirable to have the following equipment operational: emergency helium circulator, refrigeration machinery, secondary cooling pumps, vacuum pumps, circulating water pumps, cooling tower fans, tritium processing equipment, instrumentation, controls and lighting. To elaborate somewhat on the above, the emergency helium circulator will be a standard single stage axial flow circulator driven by a 4160 V pony motor instead of the conventional 13.2 kV drive. Less than one megawatt of emergency power will be required for this service. Also, since it will not be possible to blow safety valves to the atmosphere during unit tripouts, provision must be made to accommodate the excess steam in the condensers. This will

require at least two circulating water pumps and 20 percent of the cooling tower fan capacity to be supplied from the emergency diesel generator bus. Power consumption will be 3 MW and 2 MW respectively. The remaining emergency services will collectively use less than 2 MW of diesel generator output.

The diesel generators will be designed to reconcile emergency shutdown conditions only. Recognizing the high station service requirements for plant startup, it would be economically unreasonable to provide in-house capacity for this infrequent service.

Station dc power will be provided by a 125 V station battery and charger. It will be used to power the reactor instrumentation and control system and to control and power the turbine-generator emergency auxiliaries, switchgear, annunciators, emergency lighting, inverters and instrumentation. The battery will be designed to deliver two hours of rated dc power supply, independent of the charger, without any appreciable voltage drop.

Acknowledgments

In developing the designs described in this chapter, assistance was obtained from a number of outside individuals. Our grateful appreciation is extended to the following persons and their companies for their generous help:

Mr. G. White of the Babcock and Wilcox Co., New York, N.Y., for help in preliminary scoping of the steam generator system.

Messrs. R. O. Barratt, J. Polcer, W. T. Klamm, M. D. Bernstein, R. Costello and R. de Flesco of the Nuclear Department, Foster Wheeler Corp., Livingston, N.J., for help with design of the helium heat transfer system and for developing the conceptual design of the steam generators and cost estimates given in Appendix C.

Dr. G. R. Hopkins of The General Atomic Co., San Diego, Cal., for help with the design of the helium heat transfer system and for cost estimates for the helium circulators.

Messrs. J. R. Bushey and J. R. Kalalian of the Large Turbine Division, Westinghouse Power Systems Co., Lester, Pa., for furnishing design information and cost estimates for the turbine generators.

References

- ¹G. R. Hopkins and G. Melese-d'Hospital, Direct Helium Cooling Cycle for a Fusion Reactor, Nuclear Fusion Reactor Conference Proceedings (British Nuclear Energy Society, London, 1969) pp. 522-535.
- ²W. F. Weiland, Jr., Local Heat Transfer Coefficients for Flow from Hydrogen and Helium in a Smooth Tube at High Surface-to-Fluid Bulk Temperature Ratios, Nuclear Engineering, Part 12, No. 60, Chemical Engineering Progress Symposium Series (American Institute of Chemical Engineers, New York, 1965) Vol. 61, pp. 97-105.
- ³W. H. McAdams, Heat Transmission, 3rd edition (McGraw-Hill Book Co., New York, 1954).
- ⁴A. P. Fraas, Problems in Coupling a Gas Turbine to a Thermonuclear Reactor, ASME Paper No. 72-GT-98; presented at the Gas Turbine and Fluids Engineering Conference, San Francisco, Calif., 1972.
- ⁵G. H. Gabbard, R. J. Kedl, and H. B. Piper, Heat Transfer Performance of the MSRE Main Heat Exchanger and Radiator, Internal Oak Ridge National Laboratory Report (1967), unpublished.
- ⁶A. P. Fraas and N. M. Ozisik, Heat Exchanger Design (John Wiley and Sons, New York, 1965).
- ⁷Physical Properties of Molten Salt Reactor Fuel, Coolant and Flush Salts, S. Cantor, ed., Oak Ridge National Laboratory Report ORNL-TM-2316 (1968).
- ⁸Molten Salt Reactor Program Semiannual Progress Report for Period Ending August 31, 1971, Oak Ridge National Laboratory Report ORNL-4728 (1972).
- ⁹W. Kays and A. L. London, Compact Heat Exchangers, 2nd edition (McGraw-Hill Book Co., New York, 1964).
- ¹⁰M. A. Hoffman, Magnetic Field Effects on the Heat Transfer of Potential Fusion Reactor Coolants, Course on the Stationary and Quasi-Stationary Toroidal Reactors (Commission of the European Communities, Luxembourg, 1973) EUR 4999e, pp. 261-280.

¹¹W. R. Grimes and S. Cantor, Molten Salts as Blanket Fluids in Controlled Fusion Reactors, Oak Ridge National Laboratory Report ORNL-TM-4047. (1972).

¹²Reactor Chemistry Division Annual Progress Report for the Period Ending January 31, 1965, Oak Ridge National Laboratory Report ORNL-3789 (1965).

¹³W. G. Homeyer, Thermal and Chemical Aspects of the Thermonuclear Blanket Problem, Massachusetts Institute of Technology Technical Report 435 (1965).

¹⁴W. Daenner, Neutron Flux Asymmetry in Toroidal Geometries, Proceedings of the 7th Symposium on Fusion Technology (Commission of the European Communities, Luxembourg, 1972) EUR 4938e, pp. 107-112.

¹⁵Molten Salt Reactor Program Semiannual Progress Report for Period Ending August 31, 1970, Oak Ridge National Laboratory Report ORNL-4622 (1971).

¹⁶Handbook for Mechanical Engineers, T. Baumeister, L. S. Marks, eds., 7th edition (McGraw-Hill Book Co., New York, 1967).

¹⁷Handbook of Chemistry and Physics, R. C. Weast, ed., 48th edition (Chemical Rubber Publishing Co., Cleveland, Ohio, 1967).

¹⁸Flow of Fluids through Valve Fittings and Pipe (Crane Co., Chicago, 1957) Technical Paper No. 410.

¹⁹NIMONIC Alloy PE-16, Henry Wiggin and Co., Ltd., Hereford, England, Publication 3349B (1969).

²⁰L. D. Blackburn, J. C. Tobin, and R. A. Moen, Materials Creep Behavior and Elevated Temperature Design, Nuclear Technology 16, 278 (1972).

²¹P. Fortescue, The Case for Gas Cooling, Power Engineering, (July 1972), pp. 420-425.

Chapter 12
The Vacuum Wall, Radiation Damage,
Structural Problems
P. Bonanos and H. K. Perkins

Introduction

The basis for establishment of design criteria and materials selection is first, the retention of structural integrity in a severe irradiation environment at elevated temperatures and second, the prior existence of a proven technology in the fabrication of tubes, structural shapes and weldments. The prediction of fusion reactor materials behavior and properties is an extrapolation of effects observed in fission reactor irradiation experiments and supportive ion and electron bombardment studies. The relevant effects observed in neutron-irradiated metals are volumetric swelling and a loss of ductility. A comprehensive understanding of the processes leading to these property changes does not yet exist, and the extrapolation is therefore uncertain. The major differences between fusion and fission reactor materials performance will be due to (1) the harder spectrum of the fusion reactor neutron flux, (2) a unique problem at the first wall which, in vacuo, views the reacting plasma and may present a new set of plasma-wall interactive surface damage problems and (3) a postulated power reactor lifetime of 200,000 hours (22.8 years) leading to a higher accumulated fluence. A five year life is assigned, arbitrarily at present, to the first wall. The actual life will be set by the "residual ductility" to fracture determined experimentally by fission irradiation with high helium content. The prospect of accelerated experimental simulation of the combined effect of these conditions is at this date remote, short of building a prototype fusion reactor. Reliance must be placed on fission irradiation and ion bombardment experiments and perhaps, mathematical modeling.

A list of first wall design parameters is given in Table 12.1.

Neutronic Spectral Effects

The high energy component of the neutron spectrum results in two effects. The damage effectiveness of the flux is greater, and the generation of transmutation products proceeds at a higher rate. Helium is produced directly by (n, α) reactions and supplemented by additional reactions whose cross sections are not yet evaluated (see Chapter 10). Hydrogen and solid products are also generated at higher rates. Fission reactor helium effects experiments are usually limited to helium contents of less than

Table 12.1
First Wall Design Data

	<u>Nominal Value</u>	<u>Peak Value</u>
14 MeV neutron current n/sec-cm ²	7.81×10^{13}	1.28×10^{14}
14 MeV neutron power MW/m ²	1.65	2.71
Total neutron flux n/sec-cm ²	8.66×10^{14}	1.42×10^{15}
Total yearly fluence n/cm ²	2.73×10^{22}	4.48×10^{22}
2×10^5 hr (22.83 yr) fluence n/cm ²	6.23×10^{23}	1.02×10^{24}
Fraction of flux > 0.1 MeV	0.51	
Fraction of flux at 14 MeV	0.165	
Displacements/atom year	27	44
Displacements in 2×10^5 hours/atom	616	1005
Helium production ppm/year	392	644
Helium production in 2×10^5 hours (atom percent)	0.895	1.47
Bremsstrahlung (w/cm ²)	33.6	55.1
Synchrotron radiation (w/cm ²)	4.4	-
Volumetric heating rate (w/cm ³)	29.5	48.4
Operating temperature range	250 - 660°C	
Internal pressure - tubes	50 atm (735 psia)	

10^{-4} atomic fraction, a value less than the accumulated helium production from 100 days operation of the reference design first wall. Low helium fractions promote void swelling and embrittle stainless steels. Higher helium fractions may induce additional swelling from gas effects alone. The vital property change however is the loss of ductility which may drop from 20% to < 1% elongation.¹ The peak fluence for the first wall is $\sim 10^{24}$ n/cm² ($\sim 5 \times 10^{23}$ > 0.1 MeV). Fission reactor experiments yield data to fluences of $5 - 10 \times 10^{22}$ (> 0.1 MeV). The dose dependence of void swelling and mechanical property changes for this added fluence range is as yet unknown. It must be emphasized that the 14 MeV flux is 16% of the total first wall flux.

First Wall Effects

Ion and fast neutral bombardment (hydrogen isotopes, helium, impurities) of reactor walls affect the wall lifetime, the influx of cold particles to the plasma, and the tritium holdup and recovery route. Sputtering coefficients for erosion due to all plasma particles over a wide energy range are not available, nor are hard numbers available to predict the identity, number, and energy distribution of the bombarding particles.

Erosion due to physical sputtering is strongly dependent on the mass and atomic number of the bombarding species and the wall atoms as well as on the energy and flux of the incoming energetic ions and neutrals. The implantation of energetic gaseous ions and neutrals produces drastic changes in the surface layers of metals: a large number of metal lattice vacancies and interstitials; a gas atom concentration that may equal the metal atom concentration; and electron excitation and thermal heating. The majority of these implanted gas atoms will be reemitted at the bombarded surface for the metals that we consider, such as ferrous- and nickel-alloys, at temperatures around 500°C. Under certain unpredictable conditions some of this reemission may result from the bursting of gas-filled bubbles that form in the surface layer by a coalescence of gas trapped at vacancies. This phenomenon, which is referred to as blistering, has recently been observed for He bombarding stainless steel^{2,3} and nickel.⁴ Some of the implanted hydrogen may migrate into the bulk of the metal wall and either become trapped at energetically favored sites, form a dilute solution in the metal, or be emitted at the unbombarded side. Too high a concentration of hydrogen in the bulk of the metal is undesirable because of degradation of mechanical strength by hydrogen embrittlement (especially important on cooldown) and the desire to keep the tritium inventory as low as possible.

Fabrication Technology

Fabrication requirements lead first to a survey of the austenitic steels. All have well established forming, machining and welding methods. The choice for the FFTF fuel cladding, 20% cold worked 316 stainless steel, swells least among the 300 series materials. The projection of the swelling vs. dose dependence to the fusion design fluence yields unacceptably high values (even with low He content) so that the Wiggins alloy Nimonic PE-16 has been substituted. PE-16 is also an austenitic material with swelling predicted at $\sim 1\%$ for the peak fluence based on ion bombardment data.⁵ This test used carbon ion bombardment at 525°C with unspecified (but probably low) helium content; so the extrapolation is uncertain and serves only to indicate that the swelling may remain low for neutron irradiation as well. PE-16 may be hot worked in the range $950 - 1180^{\circ}\text{C}$. Annealed material may be cold worked, exhibiting 42% elongation in a 50 mm gage length with a shear strength of 52 hbar (75 ksi). Intermediate anneals in successive stages of cold work can be accomplished rapidly requiring only 15 min. at 1040°C for thin sheets. Permitted joining methods include flash butt, resistance, spot seam and electron-beam welding and high temperature brazing. Normal welding processes allowed include T.I.G., M.I.G., and metal-arc.

To develop the required properties PE-16 must be aged. The recommended heat treatments include

(a) 1h/ $900^{\circ}\text{C}/\text{AC}$ + 3h/ $750^{\circ}\text{C}/\text{AC}$

or (b) 2h/ $300^{\circ}\text{C}/\text{AC}$ + 16h/ $700^{\circ}\text{C}/\text{AC}$

Heat treatment facilities are thus required to accommodate fabricated assemblies. Final vacuum welds in the reactor vessel must be made to annealed material and placed behind the blanket away from the plasma neutron-source, where the total fluence is lower.

The alloy choice is, of course, not final, and evaluation of high helium fraction, high neutron fluence data from sources such as the HFIR-CTR (ORNL), EBR - II, FFTF and DFR experiments will reduce the dubiousness of the property predictions. Irradiation of nickel alloys in HFIR results in helium generation rates exceeding those calculated for a CTR and is thus particularly useful in evaluating austenitic steels and nickel-based alloys.

In the paragraphs that follow, specific details of the various damage mechanisms and first wall surface processes are more fully discussed. In a concluding statement, an outline of the experiments and data needed for reliable design are summarized.

Bulk Radiation Effects

The important neutron-irradiation effects are now examined.

Swelling

Swelling in irradiated fusion reactor blanket structural materials is expected to result from two sources. First is the accumulated interstitial content manifested by its negative image, voids, and second, helium bubbles. Void swelling was first reported⁶ in 1966 and has since been the subject of intensive investigation. Two international meetings^{7,8} have been held devoted entirely to this subject and serve as excellent summaries along with a review article by Norris.⁹ What follows is an outline of the mechanisms of void swelling and some introduction to the terminology used in the literature.

If a metal atom is struck by an energetic atom or neutron, it is displaced from its lattice site and moves a short distance losing its energy by Coulomb interaction with its neighbors. The struck atom is termed a primary knock-on. The knock-on, in moving through the lattice, may displace some thousands of atoms from their lattice sites. This cascade results in a displacement spike generating many displaced atoms that now occupy sites between the regular lattice positions and are termed interstitials. The unoccupied lattice sites are vacancies, and the combination of a vacancy and an interstitial is termed a Frenkel pair.

At the operating metal temperatures in a reactor both vacancies and interstitials are mobile, the mobility of interstitials exceeding that of vacancies. If vacancies encounter interstitials, their identities are lost in the recombination (Frenkel pair annihilation). The interstitials, however, are preferentially absorbed at line defects called dislocations. Concurrent with the absorption of interstitials is an aggregation of vacancies into clusters. The vacancy clusters upon reaching a critical size collapse into dislocation loops. The collapse is a net volume decrease, which compensates for the volume increase of the interstitial loops, and little or no swelling results. The presence of atomic impurities, particularly helium atoms, is believed to stabilize the vacancy cluster preventing its collapse, thus forming an embryonic void and initiating volumetric swelling. Void growth and attendant significant swelling require a higher flux of vacancies than interstitials to the void nucleation sites. This will occur if (1) the temperature is low enough so that the irradiation-produced vacancies exceed the thermal equilibrium concentration (supersaturation), (2) the temperatures are high enough to mobilize vacancies and (3) dislocations are present to absorb

interstitials preferentially. These conditions are satisfied between 0.25 to 0.5 times the absolute melting point of the metal ($0.25 - 0.5T_m$). The nucleation of voids is then a sensitive function of the vacancy supersaturation, the degree of preferential attraction of interstitials by dislocations and the presence of helium. Experimental data substantiate these concepts. An empirical equation¹⁰ based on many experimental samples indicates that the neutron irradiation swelling of cold worked TP 316 stainless steel varies with temperature in the range 350-750°C corresponding to $0.3-0.55 T_m$. This equation conveniently represents a summary of available data for a particular material and is shown in Fig. 12.1. Note that this formula does not include any saturation mechanism with increasing fluence nor represents samples with high helium content.

Swelling data for Nimonic PE-16 are not as plentiful. Carbon ion bombardment of both 316 and aged PE-16 produced an order of magnitude less swelling in PE-16. Aged PE-16 contains a coherent γ' precipitate $Ni_3(Ti, Al)$. The explanation for the reduced swelling is that the precipitate is the major point defect in the system serving as a self-regulating recombination center for large fluxes of vacancies and interstitials. Additionally, the precipitate can pin dislocations making more difficult the dislocation climb associated with the preferential attraction of interstitials by dislocations.

Ductility Loss and Creep

Austenitic stainless steels subjected to neutron irradiation suffer a drastic loss in ductility. As an example, a TP 304 specimen irradiated at 410°C to 6.7×10^{22} n/cm² (> 0.1 MeV) exhibited a creep-rupture ductility of 0.1% at 27,500 psi and life of 0.5 hour when tested at 600°C.¹ An unirradiated sample of the same material tested at the same stress and temperature failed in 185 hours at an elongation of 19.7%.

In tensile tests at low temperatures (below 550°C) irradiation hardening reduces the strain-hardening exponent and consequently the uniform strain. Current theories attribute this to a phenomenon termed "channeling". The irradiation-produced defects are removed from bands or channels by slip dislocations. Subsequent deformation occurs more easily along the bands since the critical shear stress is lower in the defect-free zone than the surrounding harder matrix. Although the macroscopic strain is low it is concentrated in these very highly strained bands which fail. At higher temperatures ductility is attributed to intragrain cohesion. Helium formed from transmutation reactions accumulates at grain boundaries and weakens the cohesive force again resulting in premature intergranular failure at low elongations.

744010

Fig. 12.1 Swelling in 316 S.S. due to fission neutrons (Ref. 10) and C^+ ion bombardment (Ref. 5). PE-16 swelling from Ref. 5.

At all operating temperatures in a CTR structural material the enormous helium content may cause grain boundary failures and require low design values for total elongation in order to achieve a decent structural lifetime. Operating stresses must be low enough to reduce the time-dependent deformation, called creep, to acceptable levels over the design life of the component. Present FFTF design criteria,¹⁰ for example, assume cladding failure at thermal creep strains of 1 to 2%.

Surface Radiation Effects

The surface damage due to bombardment by energetic ions and neutrals is examined first, followed by a brief discussion on "neutron sputtering".

The Bombarding Species

The interior of the reactor plasma contains D^+ , T^+ , and Ar^{18+} , with an average energy of about 25 keV and He^{2+} ions with energies from 3.5 MeV down, but the average He^{2+} energy in the outer zone is expected to be¹¹ in the range of 6-10 keV. Our reference to ~ 10 keV ions is to these He^{2+} ions, since the fuel ions are cold (<1 keV) at the boundary. The question of how many of these ~ 10 keV ions result in ~ 10 keV particles (ions or neutrals) bombarding the first wall cannot be answered yet but is expected to be less than 1% of the throughput. Bombardment by ~ 1 keV neutrals, primarily D and T, can result from charge exchange if cold neutrals or partially-stripped ions enter the reacting plasma. (The probability of He^{2+} acquiring two electrons via charge exchange is thought to be low so that bombardment by ~ 10 keV - He neutrals may be negligible.)

The plasma in the 50-cm wide zone adjacent to the first wall (scrapeoff region) is not well characterized; however, its temperature and density are not expected to differ appreciably from the divertor plasma since they are linked by common magnetic flux surfaces. (Some of the outer flux surfaces intercept the first wall.) The divertor plasma temperature is 0-300 eV; the electron density is $1.4 \times 10^{14}/cm^{-3}$. The flux of low energy (0-1 keV) ions and neutrals that bombard the first wall is not known and unlikely to be uniform over the first wall. Higher fluxes might occur near the divertor entrance or the injector. An upper limit is calculated by dividing the particle throughput by the first wall area (J_{max}), a lower limit is none; our arbitrary design number is $0.1 J_{max}$. The values for J_{max} are listed in Table 12.2. The energies of these bombarding particles are unknown both because of uncertainties in the scrapeoff plasma temperature distribution and in the plasma sheath potential, which is expected to be less than

Table 12.2
Particle Fluxes to the First Wall

	Energy eV	Flux particles/cm ² -sec
D	$10^0 - 10^3$	$10^0 - 6.0 \times 10^{14*}$
	$10^3 - 10^4$	$10^0 - 6.0 \times 10^{12+}$
T	$10^0 - 10^3$	$10^0 - 6.0 \times 10^{14*}$
	$10^3 - 10^4$	$10^0 - 6.0 \times 10^{12+}$
He	$10^0 - 10^3$	$10^0 - 7.8 \times 10^{13*}$
	$10^3 - 10^4$	$10^0 - 7.8 \times 10^{11+}$
Ar	$10^0 - 10^3$	$10^0 - 6.5 \times 10^{13*}$
	$10^3 - 10^4$	$10^0 - 6.5 \times 10^{11+}$

* Upper limit estimated as the throughput divided by the first wall area.

+ Upper limit estimated as 1% of the throughput divided by the first wall area.

$4 kT_e$.¹² The chosen design parameters are a 300 eV plasma, no plasma sheath potential so that the average bombarding energy is 300 eV, and a particle flux of $0.1 J_{\max}$.

The damage to the first wall from bombardment is assumed to be directly proportional to the flux for a given energy and identity of bombarding particles. (It is essentially independent of the charge on the particle so that damage from ions and neutrals of the same species and energy will be similar.) Since uncertainties exist in the fluxes, energies, and identity of the particles that will bombard the various regions of the first wall, we consider the damage associated with a wide span of energies. Using existing experimental data we shall estimate erosion rates for several assumed fluxes and energies.

Sputtering

When energetic particles bombard target materials with energy above a threshold value ($\sim 10\text{eV}$), they penetrate the surface and initiate a transfer of energy to target atoms so that some of them are ejected from the surface. This process is referred to as sputtering. The number of ejected target atoms per incident particle is the sputtering coefficient. The incident particles in most sputtering experiments have been ions; however, the sputtering yield is assumed to be independent of the charge on the bombarding particle.

A qualitative picture of sputtering from a random target follows. An impinging particle undergoes a series of collisions in the target; atoms that recoil with enough energy undergo secondary collisions, thereby creating another generation of recoiling atoms. The incident particles and energetic recoil atoms have the possibility of getting back-scattered out of the target surface with an appreciable fraction of their initial energy. Since the second generation of scattered target atoms has much lower recoil energy and consequently shorter range, only those located originally within a few atomic layers of the surface will retain sufficient energy to overcome the surface binding force and be ejected. The majority of the sputtered atoms result from these secondary collisions in the surface region. Hence the sputtering yield depends primarily on (1) the energy deposited in the surface region of the target by both the bombarding particle and energetic recoil atoms and (2) the surface binding energy.

The surface binding energy depends on surface impurities, absorbed or adsorbed gas, and the crystallographic plane or planes at the surface. Thus sputtering yields are dependent on the existing surface conditions. The temperature of polycrystalline targets is not likely to be a significant factor as long as it is well below the melting point.

In the case of reactive bombarding species, such as hydrogen, chemical forces may play a significant role in the sputtering process and molecules like NiH could be ejected.

The maximum energy that can be transferred in a two body collision is limited by conservation of momentum and energy to $4E_1 m_1 m_2 / (m_1 + m_2)^2$, where m_1 and E_1 are the mass and energy of the incident particle in the reference frame where the second particle of mass, m_2 , is at rest. When $m_1 = m_2$ all of the energy can be transferred. The transferred energy decreases as the mass ratio m_1/m_2 departs from one; this is reflected in similar decreases in sputtering yields; however, this simplified model does not quantitatively give the mass dependence of the sputtering yields.

The sputtering yield exhibits a maximum when the angle of incidence is about 60° - 70° from normal.¹³ Most sputtering data are for normal incidence; incidence at $\sim 60^\circ$ from normal could increase the yield by an order of magnitude.

A representative curve showing the dependence of the sputtering coefficient $S(E)$ on the energy E is shown in Fig. 12.2. The rather broad maximum that occurs around 1 keV to 10 keV reflects the energy region where the increasing penetration depth of the bombarding particle results in a smaller fraction of its total energy being deposited in the surface region.

Available Data

A number of review articles on sputtering contain collections of existing data and details of various theoretical models.¹³⁻¹⁹ No pertinent sputtering data were found for the proposed first wall alloy, Nimonic PE-16 containing primarily 44% Ni, 17% Cr, 1% Ti, 3% Mo, and 35% Fe; however, its sputtering coefficient is not likely to differ appreciably from that of Fe and Ni. (The available sputtering coefficients for these two metals are within a factor of two.) Figs. 12.3 - 12.9 are plots of the more pertinent sputtering data to the first wall erosion of ferrous- or nickel-based alloys.

Guseva's data²⁰ on the sputtering yields for stainless steel bombarded by 5-30 keV D (Fig. 12.3) show that S is essentially the same at 5 and 10 keV and decreases at higher energies.

The most extensive collection of sputtering data comes from Wehner and co-workers,²¹⁻²³ who measured the sputtering yields for the inert gases over the energy range 0 to 600 eV and for H_2^+ and H_3^+ with an energy of 7 keV on many metal targets. Their data for He^+ ^{21,22} and Ar^+ ^{21,23} bombardment of Fe, Cr, and Ni are plotted in Figs. 12.4 and 12.5. Similar values for Ar^+ bombardment have been reported by Weijssenfeld et al.²⁵ No data were found for hydrogen in this low energy range; however,

734223

Fig. 12.2 A representative sputtering curve to demonstrate the energy dependence of the yield.

734222

Fig. 12.3 The sputtering yield for D⁺ bombardment of stainless steel as a function of ion energy. Data from Ref. 20.

734218

Fig. 12.4 Sputtering yield (uncorrected for secondary electrons) of low energy He⁺ on Cr, Ni, and Fe. Data from Ref. 21.

734219

Fig. 12.5 Sputtering yield (uncorrected for secondary electrons) of low energy Ar^+ on Cr, Ni, and Fe. Data from Ref. 21.

later in this section we shall scale some data on silver to estimate a value for 700 eV D. Figures 12.6 and 12.7 are plots of the sputtering yield of 300 eV He^{+22} and 7 keV H_2^{+24} as a function of the atomic number of the target material. A strong periodic dependence of the yield exists in metals for all bombarding ions with maxima occurring at Cu, Ag, and Au. This consistency gives us a certain amount of confidence in scaling the rather extensive sputtering data of Gronlund and Moore²⁶ on silver shown in Figs. 12.7 and 12.8 to obtain estimated sputtering yields on iron and nickel.

The sputtering yield for molecular ions such as 7 keV H_2^+ in Fig. 12.7 is used to calculate the yield for 3.5 keV H^+ by assuming that the energy is equally divided between the H nuclei, which then sputter independently. Similarly the data for 2 keV H_3^+ on silver in Fig. 12.9 can be used to obtain sputtering yields for 700 eV H^+ on silver. This implies that the sputtering yield for H and D is roughly constant over the energy range 1 keV to 12 keV with the value for D falling off more at the higher energies than that for H. Also the value for D is more than twice the value for H.

A summary of experimental sputtering yields for H^+ , D^+ , He^+ , Ar^+ , Fe^+ , Ni^+ , and Cr^+ bombardment of Fe, Ni, Cr, and stainless steel targets is given in Table 12.2. Except for Ar^+ on Fe we can see that the existing data fail to cover a wide spectrum of energies. Sputtering data for Fe, Cr, and Ni bombarding ions are of interest since sputtered wall atoms may become energized in the plasma and return to the wall to produce additional sputtering. Assuming that the data on silver and iron scale as in Figs. 12.6, 12.7, 12.8, 12.9 we estimate sputtering yields on iron-nickel-chromium alloys in the energy range 1 keV to 10 keV to be 0.03 for D, 0.04 for T, 0.3 for He, and 2.0 for Ar. These values are expected to be good to better than an order of magnitude, and, in fact, the value obtained this way falls within a factor of two of the experimental data in Fig. 12.3 for D on stainless steel. We feel less certain about scaling and extrapolating the sputtering data for H to estimate the yields for D and T at energies below 1 keV; however, we assume that they fall off linearly below 1 keV.

Wall Erosion

For a twenty year lifetime the wall erosion rates cannot appreciably exceed 10^{-2} cm/year. The yearly erosion rate from sputtering by particle i with energy E is

$$T_i(E) = C S_i(E) J_i(E) = 3.3 \times 10^{-16} \frac{\text{cm sec.}}{\text{year atom}} S_i(E) J_i(E)$$

$$C = (3.16 \times 10^7 \frac{\text{sec}}{\text{year}}) n^{-1}$$

$$n \approx 8.4 \times 10^{22} \frac{\text{atoms}}{\text{cc}}$$

734216

Fig. 12.6 The sputtering yield for 300 eV He^+ bombardment as a function of the target's atomic number. The divisor $(1 + \gamma)$ indicates that the ion current was uncorrected for secondary electron yield γ . Data from Ref. 22.

734217

Fig. 12.7 Sputtering yield for 7 keV H_2^+ bombardment as a function of the target's atomic number. Data from Ref. 24.

734221

Fig. 12.8 Sputtering yield of He⁺, D⁺, and H⁺ on silver as a function of energy (Ref. 26).
Figure from Ref. 26.

734220

Fig. 12.9 Sputtering yield of D_3^+ , D_2^+ , H_3^+ , and H_2^+ on silver as a function of energy (Ref. 26). Figure from Ref. 26.

Table 12.3
Sputtering Yield Data for Fe, Cr, Ni

BOMBARDING PARTICLE	YIELD ^a			REFR.
	Fe Target	Ni Target	Cr Target	
7.2 keV H ₂ ⁺	-	0.037	-	24
7.5 keV H ₂ ⁺	0.024	0.035	-	24
8.0 keV H ₂ ⁺	0.017	-	-	24
7.2 keV H ₃ ⁺	-	0.058	-	24
7.5 keV H ₃ ⁺	0.030	0.058	-	24
8.0 keV H ₃ ⁺	0.022	-	-	24
35 keV H ⁺	0.009 ^b	-	-	37
36 keV (H ⁺ , H ₂ ⁺)	0.009 ^b	-	-	38
51 keV (H ⁺ , H ₂ ⁺)	0.016 ^b	-	-	38
75 keV (H ⁺ , H ₂ ⁺)	0.009 ^b	-	-	38
6.30 keV D ⁺	0.05 - 0.03 ^b	-	-	20
100 eV He ⁺	0.030	0.028	0.030	21,22
600 eV He ⁺	0.170	1.18	0.20	21,22
100 eV A ⁺	0.20,0.10	0.28,0.25	0.30	21,23,25
600 eV A ⁺	1.26,0.90	1.52,1.5	1.30	
1 keV A ⁺	1.4	2.1	-	25,39
2 keV A ⁺	2.0 ^b	-	-	39
5 keV A ⁺	2.5 ^b	-	-	39
1.1 keV Fe ⁺	1.1	-	-	40
45 keV Fe ⁺	2.9	-	-	40
1.0 keV Ni ⁺	-	1.0	-	40
45 keV Ni ⁺	-	3.8	-	40
45 keV Cr ⁺	-	-	1.0	40

- a. The yield per bombarding particle where H₂⁺ is counted as one bombarding particle.
b. Stainless Steel.

Here T_i is the thickness that is eroded in cm/year; C is a constant that varies with the atomic density of the wall, n , as shown and is assigned a value that is typical of ferrous- and nickel-alloys; $S_i(E)$ is the sputtering coefficient of projectile i with energy E in atoms per ion; and $J_i(E)$ is the flux in ions (or neutrals)/cm²-sec.

As mentioned earlier the fluxes and energies of the bombarding particles are not known; however, in Table 12.4 are presented the calculated first wall erosion from sputtering for four cases: Case I - the design case - the bombarding energy is 300 eV and the fluxes are $0.1 J_{\max}$. Case II - the plasma energy is 300 eV, the potential sheath is 800 eV, and the fluxes are $J_{\max}$. Case III - the plasma energy is 100 eV, the sheath potential is zero, and the fluxes are $J_{\max}$. Case IV - the bombarding energy is 1 - 10 keV (characteristic of the edge of the burning plasma) and the fluxes are $0.01 J_{\max}$.

In all of the calculated cases argon bombardment dominates the erosion rate. We do not have enough understanding of the plasma transport and temperature in the scrapeoff region to place any figure of merit on the bombardment conditions assumed in Case I - the design case. Erosion by the sputtered wall particles (e.g., Fe and Ni) will be negligible as long as their bombarding energy is below ~ 1 keV.

These calculations suggest that the wall erosion rate from D, T, and He particles that escape from the plasma will be tolerable provided (1) these escaping particles strike the wall no more than once before they are pumped (The divertor should prevent most of them from striking the wall even once.), (2) there are not locally high fluxes such as at the divertor entrance or near the refueling entrance, and (3) the estimated sputtering yields are accurate.

In concluding this section on erosion by sputtering we wish to reemphasize the lack of sputtering data on the proposed first wall alloy and the uncertainty of the particle fluxes and energies that will bombard the wall. The combination of these two unknowns prevents an accurate prediction of the first wall erosion rate due to sputtering at this time.

Blistering and Associated Phenomena

Blistering, pitting, flaking, and exfoliation of surfaces subjected to bombardment by H^+ , D^+ , and He^+ have been reported for both metals and insulators.^{2-4,27-33} The available data indicate that blistering is favored by a low gas solubility and a high number of radiation-produced lattice vacancies, which promotes the coalescence of the injected gas and vacancies to form gas-filled bubbles. The region at

Table 12.4
Erosion by Sputtering

	Energy eV	S atoms/ion	J ions/cm ² -sec	T mm/year
Case I - Design Case				
D	300	0.015	6.0×10^{13}	0.0034
T	300	0.021	6.0×10^{13}	0.0048
He	300	0.07	7.8×10^{12}	0.0021
Ar	300	0.8	6.5×10^{12}	$\frac{0.0197}{0.0300}$
Case II				
D	1100	0.05	6.0×10^{14}	0.113
T	1100	0.07	6.0×10^{14}	0.159
He	>1100	0.3	7.8×10^{13}	0.089
Ar	>1100	2.0	6.5×10^{13}	$\frac{0.493}{0.854}$
Case III				
D	100	0.005	6.0×10^{14}	0.011
T	100	0.007	6.0×10^{14}	0.016
He	100	0.04	7.8×10^{13}	0.012
Ar	100	0.3	6.5×10^{13}	$\frac{0.074}{0.113}$
Case IV				
D	$10^3 - 10^4$	0.05	6.0×10^{12}	0.001
T	$10^3 - 10^4$	0.07	6.0×10^{12}	0.002
He	$10^3 - 10^4$	0.3	7.8×10^{11}	0.001
Ar	$10^3 - 10^4$	2.0	6.5×10^{11}	$\frac{0.005}{0.009}$

which bubble growth initiates corresponds to the projected range or penetration depth for the bombarding ion (10^3 Angstroms for ~ 10 keV H or He).

Many factors are expected to influence the growth, migration, and eventual rupture of these bubbles. These include temperature, implantation rate and depth, re-solution probability of precipitated gas, radiation damage, local stress, yield strength, and target microstructure including grain size, crystallite orientation, and precipitated solid phases such as carbides. Unexpectedly high surface temperatures may develop due to subsurface bubbles lowering the thermal conductivity and result in evaporation, enhanced bubble growth, or surface rupture at a lower dose. Presently, the theoretical models for formation, growth, and rupture of gas bubbles cannot predict the magnitude and nature of the surface damage thus produced, and experimental data on blistering of iron- or nickel-alloys are scant. The short range (50 Angstrom) and high sputtering coefficient for 10 keV argon suggest that it will not cause blistering. On the other hand, we expect blistering to occur from bombardment with ~ 10 keV He and do not know about bombardment with 10 keV H, D, or T.

Bauer and Thomas² have found that 300 keV He implanted in stainless steel in the temperature range - 50°C to 500°C produces a large amount of surface flaking after a critical He dose of 10^{18} atoms/cm² whereas at temperatures above 600°C it results in ruptured surface blisters after doses of 5×10^{17} atoms/cm². Their helium ion flux was 6×10^{13} ions/cm²-sec. Das and Kaminsky³ find that blistering of 304 stainless steel from 100 keV He⁺ bombardment results in an equivalent erosion rate of 3 atoms/bombarding ion whereas 500 keV He results in an equivalent erosion of 1 atom/bombarding ion; these values are for target temperatures of 450°C and are estimated by careful examination of electron micrographs of the blistered surfaces. The erosion is lower at 300°C and 550°C . Blistering has also been reported by McCracken and Erents⁴ for molybdenum bombarded by 36 keV He⁺ ions; the temperature ranges differed but the critical dose was still $\sim 5 \times 10^{17}$ He⁺ atoms/cm².

The value of 3 atoms/incident 100 keV He⁺ ion for blister-induced erosion of 304 stainless steel at 450°C is a factor of 10 higher than the estimated sputtering coefficient for 1 - 10 keV He⁺. (The same properties that make PE-16 resistant to swelling may reduce its blister-induced erosion also.) If one assumes that 1% of the He⁺ ions exiting hit the first wall with 100 keV energy (8×10^{11} ions/cm²-sec), then the yearly erosion rate would be 0.01 mm/year, which is tolerable. In light of the surprising increase in blister-induced erosion yield as the ion energy decreases from 500 keV to 100 keV, we cannot estimate its value at still lower energies and will have to await experimental data.

Hydrogen is known to permeate through metals and have a higher solubility than helium. This is the basis for predicting that blistering from hydrogen implantation will occur less often and require larger doses. This has been somewhat verified by the observation that 250 keV D^+ bombardment of 700°C Nb produces blistering after a fluence of $\sim 10^{19}$ ions/cm² and that the surface damage is less severe than from 500 keV He^+ bombardment. However, blistering in niobium from implanted D was unexpected and indicates the important role that radiation damage and kinetic effects must play in bubble formation.

Neutron Sputtering

Theoretical predictions for 14 MeV neutron sputtering are $10^{-4} - 10^{-5}$ atoms per neutron.^{34,14} Kaminsky et al.³⁵ have recently discovered that 14 MeV neutron bombardment of room temperature Nb results in the ejection of large chunks ($\sim 10^3$ Angstroms) of Nb from the surface. This is attributed to the high primary knock-on energy transferred to Nb by an elastic collision with a 14 MeV neutron (maximum transferred energy of 1 MeV). The primary Nb knock-ons that are sufficiently near the surface result in chunks of material being ejected. Kaminsky et al. report an average sputtering coefficient of 0.2 ± 0.1 atoms/incident 14 MeV neutron. The earlier two papers^{34,14} on 14 MeV neutron sputtering did not report ejected chunks; (They probably did not look for them); their values for 14 MeV neutron sputtering coefficients were between $10^{-2} - 10^{-3}$ atoms/neutron. For calculation of erosion due to neutron sputtering we shall use $S = 0.2$, the largest reported value.

The flux of 14 MeV neutrons incident on the first wall has a peak value of 1.28×10^{14} neutrons/cm²-sec and an average value of 7.81×10^{13} neutrons/cm²-sec. We shall calculate the erosion for the peak value of 1.28×10^{14} neutrons/cm²-sec. The formula for the yearly erosion rate given earlier can be used directly to calculate the erosion if one remembers that most of the 14 MeV neutrons pass through the first wall so that both forward and backward sputtering are present. The forward and backward sputtering coefficients are taken to be the same. (Kaminsky et al. found the forward sputtering coefficient about 20% larger than the backward sputtering coefficient.) The calculated peak erosion rate is

$$T_i(n) = 2 \times 3.8 \times 10^{-16} \text{ cm} - \text{sec/atom} - \text{year} \times 0.2 \text{ atom/neutron} \times 1.3 \times 10^{14} \text{ n/sec} \\ = 0.02 \text{ cm/year.}$$

This value suggests that the 0.08 cm thick first wall tubing will not have a life time of more than about a year; however, future experiments on PE-16 at expected operating temperatures are needed to ascertain whether its effective 14 MeV neutron sputtering coefficient is greater or less than 0.2 atoms/14 MeV neutron.

Discussion

Limited experimental data are available on pertinent sputtering yields and blistering of iron, nickel, and their alloys subjected to ion bombardment with H^+ , D^+ , He^+ or Ar^+ . The sputtering data were obtained before blistering from injected H, D, and He had been observed. The study of blistering and associated phenomena is in the stage of characterizing the dependence of the surface damage on a few parameters - energy, fluence, temperature, target material; information is not yet available to forecast the cumulative wall damage and erosion from long term particle bombardment. The large threshold dose ($\sim 10^{17}$ ions/cm²-sec), microscopic size ($\sim 10^{-6}$ - 10^{-3} cm), and impulsive gas release with surface rupture typical of blistering make it difficult to ascertain whether possible erosion contribution from blistering were measured in some of the early sputtering experiments.^{20,22,24,25}

Recent experimental data show that blistering occurs for 2 keV He bombardment of Nb.³⁶ The question of how much erosion this produces has yet to be determined. The collection method used by Gronlund and Moore²⁶ for determining the sputtering coefficient of 2 - 12 keV He^+ on silver should not measure any erosion due to blistering, whereas experiments such as those of Wehner²¹ where the target mass loss or fluence required to drill a hole through the target is measured may also include erosion due to blistering.

The estimate of wall erosion due to particle bombardment in this design was 0.03 mm/year. This assumes that 10% of the exiting ions bombard the first wall with an energy of 300 eV. (If these assumptions are wrong, significantly higher erosion rates are possible. Especially if a significant number of ~ 100 keV He particles bombard the first wall.) However, for this design case particle bombardment produces negligible erosion compared with neutron erosion of 0.2 mm/year.

No experimental data exist on nickel and iron alloys under prolonged and/or simultaneous bombardment with H-, D-, He- and Ar-ions or neutrals in the energy range 5 eV to 50 keV and 14 MeV neutrons. Such experiments are needed since the erosion from combined bombardment is unlikely to be the sum of separate erosion rates - especially when blistering occurs.

This presentation of experimental data on Fe, Ni, and Cr and the subsequent use of it to estimate erosion rates of ferrous- or nickel-alloys are made in the hope that the scarcity of data and uncertainty in the estimated erosion rates are noted. In addition to measuring gross erosion rates, an experimental program should aim at characterizing and understanding the factors that determine both (1) the migration, holdup and reemission of the injected H, D, and He atoms and (2) the energy and identity of material eroded from the surface. This is important because of possible plasma contamination and interest in anticipating the holdup or migration through the first wall by tritium.

The lack of data on bulk radiation effects will persist until prototype reactors are constructed. Materials evaluation programs must therefore aim at screening techniques and more reliable extrapolations of fission spectrum data. Alloys exhibiting low volumetric swelling under ion bombardment must be irradiated in-pile. It appears that the swelling problem is solvable through use of PE-16 or similar precipitation hardened austenitic such as A-286 and Inconel alloys such as 718. Irradiation of these alloys, all containing nickel, in HFIR (ORNL) would yield high fluence- high helium content data to establish ductility and creep properties. Fluence dependence over the temperature range 300-700°C must be determined.

References

¹E. E. Bloom and J. O. Stiegler, Effect of Fast Neutron Irradiation on the Creep-Rupture Properties of Type 304 Stainless Steel at 600°C, Oak Ridge National Laboratory Report ORNL-TM-3169 (1971).

²W. Bauer and G. J. Thomas, Helium Re-Emission and Surface Deformation in 316 Stainless Steel During 170°C to 700°C Implantations, J. Nucl. Mat. 47, 241 (1973).

³S. K. Das and M. Kaminsky, Erosion of Stainless Steel First Wall by Helium Blistering, Proceedings of the Fifth Symposium on Engineering Problems of Fusion Research, (The Institute of Electrical and Electronics Engineers, New York, 1974) pp. 31-36.

⁴G. M. McCracken and S. K. Erents, Blistering of Molybdenum under Helium Ion Bombardment, International Conference on Ion-Surface Interaction, Sputtering and Related Phenomena, (Max-Planck-Institut für Plasmaphysik, Garching, Germany, 1973), Abstract M-6; also Radiation Effects 18, 191 (1973).

⁵R. S. Nelson, J. A. Hudson, D. J. Mazey, G. P. Walters, T. M. Williams, Void Formation in Metals During Ion Bombardment, Radiation-Induced Voids in Metals, J. W. Corbett, L. C. Ianniello, eds., (National Technical Information Center, Springfield, Va., 1972) pp. 430-448.

⁶C. Cawthorne and E. J. Fulton, The Nature of Small Defect Clusters, Nature, 216, 575 (1967).

⁷Voids Formed by Irradiation of Reactor Materials, S. F. Pugh, M. H. Loretto, D. I. R. Norris, eds., (British Nuclear Energy Society, London, 1971).

⁸R. S. Nelson, J. A. Hudson, D. J. Mazey, G. P. Walters, T. M. Williams, op. cit.

⁹D. I. R. Norris, Voids in Irradiated Metals, Radiation Effects, (Gordon and Breach, Glasgow, 1972), Vol. 14 (Part I) pp. 1-37, Vol. 15 (Part II), pp. 1-22.

¹⁰A. Boltax, A. Biancheria, B. L. Harbourne, T. P. Soffa, Mixed-Oxide Fuel Pin Performance Analysis Using the OLYMPUS Computer Code, Conference on Fast Reactor Fuel Element Technology, Ruth Farmakes, ed. (American Nuclear Society, Hinsdale, Ill., 1971) pp. 157-199.

¹¹S. L. Gralnick, private communication.

¹²F. H. Tenney and G. Lewin, private communication.

¹³G. Carter and J. Colligon, Ion Bombardment of Solids, (American Elsevier Publishing Co., New York, 1968).

¹⁴R. Behrisch, Festkörperzerstaubung durch Ionenbeschuss, Ergeb. Exakt. Naturwiss. 35, 295 (1964); First-Wall Erosion in Fusion Reactors, Nuclear Fusion 12, 695 (1972).

¹⁵M. Kaminsky, Atomic and Ionic Impact Phenomena on Metal Surfaces, (Academic Press, New York, 1965).

¹⁶R. S. Nelson, The Observation of Atomic Collisions in Crystalline Solids, (North-Holland Publishing Co., Amsterdam, 1968).

¹⁷M. W. Thomson, Defects and Radiation Damage in Metals, (Cambridge University Press, 1969).

¹⁸P. Sigmund, Theory of Sputtering. I. Sputtering Yield of Amorphous and Polycrystalline Targets, Phys. Rev. 184, 383 (1969).

¹⁹U. A. Arifov, Interaction of Atomic Particles with a Solid Surface, (Plenum Publishing Corp., New York, 1969).

²⁰M. I. Guscva, Sputtering of Stainless Steel and Tantalum by Deuterium and Krypton Ions with Energies of 5-30 keV, Radio Eng. Electron. Phys., 7, 1563 (1962).

²¹G. K. Wehner, Sputtering Yield Data in the 100-600 eV Energy Range, General Mills Inc., Electronics Division Report 2309, Minneapolis, Minnesota (1962).

²²D. Rosenberg and G. K. Wehner, Sputtering Yields for Low Energy He -, Kr -, and Xe - Ion Bombardment, J. Appl. Phys. 33, 1842 (1962).

²³N. Laegrid and G. K. Wehner, Sputtering Yields of Metals for Ar and Ne Ions with Energies from 50 to 600 eV, J. Appl. Phys. 32, 365 (1961).

²⁴C. E. KenKnight and G. K. Wehner, Sputtering of Metals by Hydrogen Ions, J. Appl. Phys. 35, 322 (1964).

²⁵C. H. Weijsenfeld, A. Hoogendorn, M. Koedam, Sputtering of Polycrystalline Metals by Inert Gas Ions of Low Energy (100-1000 eV), Physica 27, 763 (1961).

²⁶F. Gronlund and W. J. Moore, Sputtering of Silver by Light Ions with Energies from 2 to 12 keV, J. Chem. Phys. 32, 1540 (1960).

²⁷R. S. Blewer and J. K. Maurin, Dimensional Expansion and Surface Microstructure in Helium Implanted Erbium and Erbium-Hydride Films, J. Nucl. Mat. 44, 260 (1972).

²⁸R. S. Barnes and D. J. Mazey, The Migration and Coalescence of Inert Gas Bubbles in Metals, Proc. Roy. Soc. (London) 275, 47 (1963).

²⁹W. Bauer and G. J. Thomas, Helium Release and Electron-Microscopy of Helium-Implanted Palladium, J. Nucl. Mat. 42, 96 (1972); W. Bauer and D. Morse, Helium Implantation and Re-Emission from Niobium at Elevated Temperatures, J. Nucl. Mat. 44, 318 (1972); G. J. Thomas, W. Bauer, and J. B. Holt, Helium Migration in Palladium II. Scanning Electron Microscopy Examination of High Concentration Samples, Radiation Effects 8, 27 (1971); J. N. Holt, W. Bauer, and S. J. Thomas, Helium Migration in Palladium I. Concentration Dependence of Helium Release, Radiation Effects 7, 269 (1971).

³⁰M. Kaminsky and S. K. Das. Blistering of Polycrystalline and Monocrystalline Niobium. International Conference on Ion Surface Interaction-Sputtering, Garching, Germany, 1972, M. Kaminsky and S. K. Das, Effect on Channeling and Irradiation Temperature on the Morphology of Blisters in Niobium. Appl. Phys. Letters 21, 443 (1972); S. K. Das and M. Kaminsky, Radiation Blistering of Polycrystalline Niobium by

Helium-Ion Implantation, J. Appl. Phys. 44, 25 (1973); L. H. Milacek, R. D. Daniels, and J. A. Cooley, Proton- Radiation-Induced Blistering of Aluminum, J. Appl. Phys. 39, 2803 (1968); L. H. Milacek and R. D. Daniels, Orientation Dependence of Pitting and Blistering in Proton-Irradiated Aluminum Crystals, J. Appl. Phys. 39, 5714 (1968).

³¹L. H. Milacek, R. D. Daniels, and J. A. Cooley, *ibid*; L. H. Milacek and R. D. Daniels, *ibid*.

³²W. Primak, Radiation-Induced Cavities and Exfoliation, J. Appl. Phys. 34, 3630 (1963); W. Primak, Y. Dayal, and E. Edward, Ion Bombardment of Silicon, J. Appl. Phys. 34, 827 (1963); W. Primak and J. Luthra, Radiation Blistering: Interferometric and Microscopic Observations of Oxides, Silicon, and Metals, J. Appl. Phys. 37, 2287 (1966).

³³H. E. Shiott, Approximation and Interpolation Rules for Ranges and Range Straggling, Radiation Effects 6, 107 (1970).

³⁴T. S. Baer and I. N. Anno, Sputtering of Iron by Fast Neutrons, J. Appl. Phys. 43, 2453 (1972).

³⁵M. Kaminsky and S. K. Das, Particle Release from Niobium under 14 MeV Neutron Impact, Proceedings of the Fifth Symposium on Engineering Problems of Fusion Research (The Institute of Electrical and Electronics Engineers, New York, 1974) pp. 37-40.

³⁶R. Behrisch, Max-Planck-Institut für Plasmaphysik, private communication.

³⁷E. S. Borovik, N. P. Katrick, G. T. Nikolaev, Interaction of Fast H Ions with Metal Surfaces in Very High Vacuum, At. Energ. (USSR) 21, 339 (1966) (Sov. Atom. Energ. 21, 1019 (1966)).

³⁸F. Fairbrother Jr. and J. S. Foster Jr., Sputtering of Stainless Steel by Protons in the 30-80 keV Range, Vacuum 4, 112 (1954).

³⁹Handbook of Thin Film Technology, Leon I. Maissel and Richard Glang, eds., (McGraw-Hill Book Co., New York, 1970) pp. 4-40.

⁴⁰O. Almén and G. Bruce, Sputtering Experiments in the High Energy Region, Nuclear Instrumentation Methods 11, 279 (1961).

Chapter 13
Superconducting Magnets and Cryogenic Systems
J. File

Introduction

This chapter describes the superconducting magnet systems and cryogenic equipment, including the refrigerators required to sustain reliable operation.

The superconducting magnets are divided into two systems:

- a) The confining field coils with supporting cylinder, and
- b) The pulsed field coils.

The pulsed field coils are further subdivided into three groups:

- a) The divertor coils,
- b) The vertical field coils,
- c) The control coils.

The first part of this chapter describes the two coil systems, as well as the design criteria used for the various coils and their support.

Superconducting magnets operate at temperatures well below 20 K, thereby necessitating a cryogenic cooling system using helium as a refrigerant, comprised of the following equipment:

- a) The Dewar,
- b) Cryogenic helium, storage and repurifying equipment,
- c) The refrigeration equipment, piping and insulation.

The second part of the chapter describes design criteria, as well as refrigeration requirements for each component of the system.

In the last part of this chapter, the power supplies to energize the magnet as well as an initial startup procedure are described.

Throughout this chapter we propose materials and equipment for the various systems and components described. We have limited this choice to materials and components now being manufactured or to those capable of manufacture after minor modifications to present processes. Prudence dictates this approach, because it not only lends itself to credible practical design, but also to more accurate cost estimates that can be easily compared to costs of existing devices.

Toroidal Field Coils

Description

The function of the toroidal field coils is to provide a steady toroidal magnetic field to help confine and stabilize the plasma during the burning cycle. For flexibility of access to the center of the reactor and for ease of construction and assembly, the magnet is comprised of 48 D-shaped, discrete coils, one of which is shown in Fig. 13.1. The coils are equally spaced in a toroidal arrangement as seen in Fig. 13.2. The design field is 160,000 gauss at the innermost conductor thereby providing a field of 60,000 gauss at the nominal plasma radius of 10.5 m. The toroidal field coils are described below.

Technical and Economic Design Considerations

The power density of a fusion reactor increases as B^4 , and the cost of the magnet increases at a somewhat lower rate (somewhere between B and B^2).^{1,2} Because of these relationships, it is generally agreed that fusion reactor designers intend to take advantage of the highest fields available, consistent with other mechanical constraints such as first wall loading and stress limits of structural materials. Commercially obtainable superconductors are already capable of producing magnets with fields in excess of 160,000 gauss³ in relatively small bores. That field level taxes the known limits of structural design. In addition to high fields, fusion reactors will require larger working volumes (by several orders of magnitude) than any previously designed. The combination of high field and large working volume requires the design of superconducting magnets well beyond present known technology.

A novel design of a toroidal magnet coil that partially accommodates the large forces generated by a high field, large volume magnet was previously described.^{4,5,6} In a toroidal magnet, the field strength within the useful volume varies inversely with the radius from the axis of symmetry, and in almost all cases the conductors generating such fields will be subject to bending moments in addition to effective internal pressure. It was shown that a conductor tethered at either end and immersed in a toroidal field will be stable if it is in pure tension and, therefore, not subject to bending moments. The net forces are then taken on a cylindrical structural element to which the conductor is tangent. Except where the conductor lies flat against the support, it lies in a curve such that its radius of curvature, ρ , is proportional to the radius from the axis of the torus, r ; $\rho = kr$. k is a constant depending on the geometry of the system. (See Fig. 13.3.) A coil of this shape is now commonly known as the "D coil".

744455

Fig. 13.1 One of 48 D-shaped coils

DIMENSIONS, CM

SECTION A-A

744459

Fig. 13.2 Arrangement of D coils
showing part of the support cylinder

724232

Fig. 13.3 Equilibrium shape of a conductor in a $1/r$ toroidal field

Of all the commercially available superconductors, only two, Nb_3Sn and V_3Ga , are capable of operation at 160,000 gauss while maintaining usable current densities. It has been shown by Powell² that there is no reasonable expectation that the price of V_3Ga will ever be competitive with the price of Nb_3Sn . Further, he shows (see Fig. 13.4) that the projected cost of NbTi is two to four times larger than Nb_3Sn at fields (less than 80,000 gauss) where either may be used. This seems to preclude the use of NbTi even as part of a hybrid coil system in fields below 80,000 gauss because, in addition to its higher cost, it has a lower critical temperature than Nb_3Sn . The higher critical temperature of Nb_3Sn permits higher operating temperatures of the refrigeration system, thereby reducing the capital cost of that system.

In summary, therefore, the D coil shown in Figs. 13.1 and 13.2 was designed of Nb_3Sn for the following economic and technical reasons:

- a) It is capable of operation at 160,000 gauss,
- b) It is the least expensive at all field levels,
- c) It has the highest critical temperature thereby minimizing refrigeration costs.

Each coil is composed of five complete and two partial pancakes as shown in Fig. 13.5. The "T" shape of the cross section takes advantage of all the space available to utilize a more favorable current density. Table 13.1 lists electrical and mechanical parameters for the proposed toroidal field coils.

Superconducting Material

The superconducting tape proposed for the confining field coils, a typical cross section of which is shown in Fig. 13.6, is comprised of four parallel paths of superconductor, 4.5 cm wide. It is capable of carrying 10,000 amperes (2500 amperes per path). The design considerations that produced this size and configuration tape are described below. Due to radial field instability, thermal stabilization and stress considerations, fifteen different thicknesses of tape are proposed from 0.48 to 1.52 cm thick. This conserves both space and money. Each conductor is tailored to its unique location in the coil and contains the requisite amount of copper for thermal stabilization and stainless steel for strength. Table 13.2 lists each of the fifteen conductors used in the similarly numbered sections of Fig. 13.5.

This proposed material is capable of being constructed with some modification to the present manufacturing processes, but without major developments in the state of the art. We do not propose a multifilament superconductor now because of its very

Fig. 13.4 Projected costs of
superconductors

744452

Fig. 13.5 Cross section of reactor coil

Fig. 13.6 Cross section of Nb₃Sn tape proposed for reactor coil

Table 13.1
Parameters for Proposed Fusion Reactor Toroidal
Field Coils

Superconductor	Nb ₃ Sn
Width of Conductor	4.5 cm
Thickness of Conductor	0.48 - 1.52 cm
No. of Pancakes/Coil	7
Maximum Field at Conductor	160,000 gauss
Nominal Plasma Radius	1050 cm
Field at Nominal Plasma Radius	60,000 gauss
Ampere Turns/Coil	6.57×10^6
Minimum Current Density (Conductor Area)	2170 amp/cm ²
Current/Conductor	10,000 amperes
Energy Stored in Field	250×10^9 joules
Inductance of Torus	5×10^3 henries
Weight of Cylinder	10^6 kg
Weight of Toroidal Coils	6.2×10^6 kg

Table 13.2

Nb₃Sn Conductors in Confining Field Coils

Section	Copper Thick. (Centimeters)	S.S. Thick. (Centimeters)	Superconductor Thick. & Other (Centimeters)	Total Thick. (Centimeters)
1	0.60	0.90	0.02	1.52
2	0.60	0.68	0.02	1.30
3	0.80	0.48	0.02	1.30
4	0.60	0.30	0.02	0.92
5	0.60	0.14	0.02	0.76
6	0.40	0.90	0.02	1.32
7	0.50	0.68	0.02	1.20
8	0.60	0.48	0.02	1.10
9	0.50	0.30	0.02	0.82
10	0.40	0.14	0.02	0.56
11	0.32	0.90	0.02	1.24
12	0.32	0.68	0.02	1.02
13	0.32	0.48	0.02	0.82
14	0.32	0.30	0.02	0.64
15	0.32	0.14	0.02	0.48

preliminary state of development. However, if and when an economically competitive and satisfactory form of multifilament Nb_3Sn is developed, the proposed coil can easily be adapted to that material.

1. Radial Field Instability

Nb_3Sn is presently available in relatively wide, thin tapes. Typically, it consists of a small amount of superconductor, sandwiched between layers of copper for stability and stainless steel for strength, all bonded together with solder. Fig. 13.7 is an enlarged photograph of a typical, 1/2 cm wide, 0.042 cm thick, Nb_3Sn tape now being manufactured by the Intermagnetics General Corporation of Guilderland, New York.

It has been found that tapes of this type have a greater current carrying capacity when immersed in magnetic fields parallel to the wide edge. When operating in fields perpendicular to the wide edge, the current carrying capacity of tape material degrades.^{7,8} The degradation, which increases with the aspect ratio of the ribbon, is due to eddy currents produced in the normal metals surrounding the superconductor. This limitation to the use of Nb_3Sn tape is often called the radial field instability. Present design practice limits the current-carrying capacity to about 500 amps/cm of width/parallel path and operation of the tape in radial field components of less than 40,000 gauss. Fig. 13.8 shows a cross section of one of the coils taken in the plane of the axis of the torus; the axial component of the field is plotted on one half of the coil cross section and the radial field component on the other half. It is seen that the maximum axial field component is at the inner edge of the bore and is, as previously stated, 160,000 gauss. The maximum radial component is near the outer edge of the torus, at about the middle of the build and is 33,000 gauss, composed of a toroidal field component of 25,000 gauss and a poloidal field component of 8,000 gauss. This is well within the 40,000 gauss maximum allowable radial field. The proposed tape is designed to carry 560 amperes/cm of width/parallel path, in a maximum radial field component of 40,000 gauss. This represents only a moderate increase over presently used design practice.

2. Thermal Stabilization

It is well known that flux penetrates inside a Type II superconductor such as Nb_3Sn . As the flux penetrates into the superconductor, it concentrates into bundles and becomes pinned to inhomogeneities, strains, dislocations or other defects in the material. Due to the $\vec{J} \times \vec{B}$ Lorentz force, these bundles jump from barrier to barrier. This is known as flux jumping, and if enough energy is released during a flux jump to bring the local temperature above the critical temperature, T_c , a normal transition follows. The local transition usually propagates rapidly, thereby rendering the whole

744479

Fig. 13.7 Photograph of 1/2 cm wide Nb_3Sn manufactured by the Intermagnetics General Corporation.

744243

Fig. 13.8 Cross section of the reactor coil showing lines of constant axial field on one side of the centerline and lines of constant radial field on the other. Both are symmetrical about the coil centerline

coil to its normal state. The effect of flux jumping can be minimized if one can quickly restore the local hot spot to a temperature below T_c before the normal transition spreads and becomes catastrophic. Superconductors have poor heat transport properties. In addition, the normal electrical resistivity of these materials is high, even at liquid helium temperatures. Therefore, if one places another material, usually called stabilizing material, of high thermal and electrical conductivity in intimate contact with the superconductor, the effect of flux jumping is minimized in the following way:

a) As the temperature of a local area of the superconductor exceeds T_c due to a flux jump, its electrical resistance becomes excessively high. The current in the superconductor then flows in the lower resistance parallel path made available by the stabilizing material.

b) At the same time, and because of the good conduction and thermal transport properties of the stabilizer, the heat produced in the superconductor is quickly conducted to the helium coolant.

c) If the I^2R ohmic heating in the stabilizer is minimal, and if the temperature of the superconductor attains a value less than T_c before the local normal transition spreads, then superconductivity is restored locally, and the current again flows in the superconductor.

There are three methods of stabilizing superconductors. These are known as cryogenic, dynamic, and adiabatic stabilization. They are defined by Hancox⁹ as:

a) Cryogenic stabilization, which depends on the provision of an alternative low resistance path for the current to flow. Sufficient stabilizing material must be provided so that the conductor is capable of carrying the current indefinitely without raising the temperature of the superconductor above T_c .

b) Dynamic stabilization, which is obtained by the addition of sufficient normal material to magnetically damp any flux jumps so that the energy released can be removed locally by conduction and possible instabilities prevented from growing.

c) Adiabatic stabilization, which requires the use of fine filaments of superconductor so that the energy associated with a flux jump is too small to drive the conductor normal.

If a large number of very thin wires or filaments can be contained in a conductor in such a way that each reacts independently to any magnetic field change, then the whole conductor is stable. Magnetic interaction is prevented by fully transposing the filaments within the conductor (as in a Litz wire) and also to some extent by twisting the wire.

Careful consideration must be given to stabilization whenever a superconducting magnet is designed. The effect of using under-stabilized superconductors in any given application is to lower the useful current rating, i.e., design specifications cannot be met. In this case, because of the choice of tape material, adiabatically stable operation is not possible; therefore, for economic reasons, and in the belief that the design of dynamically stable magnets will be better understood in the next decade or so, we propose a dynamically stable magnet for this reactor, using copper as the stabilizing material.

We choose copper as the stabilizing material even though high purity aluminum and nickel have some interesting possibilities,¹⁰ because copper has had widespread use and is more than adequate to fill the needs of this design.

The amount of copper required depends on the severity of the flux jumps (therefore on the magnitude of the magnetic field and the current flowing) as well as the conduction heat transfer properties of the copper and the superconductor. Hancox¹¹ has established design criteria for the amount of copper and superconductor required for a conductor to operate in a dynamically stable mode. According to these criteria, the average current density in the copper during normal transitions is not to exceed 4,600 amperes/cm², and accordingly each of the fifteen types of conductor listed in Table 13.2 is designed for a lower average current density. If the magnet were designed as a cryogenically stable magnet, the average current density in the copper during normal transitions would have to be less than 2,100 amps/cm².

3. Stress in the conductor

In cgs, electromagnetic units, the tensile stress in a conductor bundle of a constant tension, moment-free coil has been shown to be

$$T = B I k r \quad (13.1)$$

where B is the magnetic field at the conductor, I is the current flowing, k is the constant previously described, and r is the radial distance from the axis of the torus to the conductor.

Nb₃Sn is a very brittle, intermetallic compound, and therefore can tolerate only limited elongation before breaking. In a composite tape conductor, the superconductor, the copper, and the support material are bonded together. In the early days of the manufacture of Nb₃Sn, the bonding techniques were such that the tape could not tolerate strains of much over 0.001. It was soon found that Nb₃Sn can be deposited in a pre-stressed condition, thereby doubling the allowable strain. More recently,

advanced techniques of bonding copper, stainless steel and Nb_3Sn have produced conductors capable of withstanding strains in excess of 0.003. We therefore deemed it reasonable to assume that a strain of 0.003 is tolerable in our design.

Considering expression 13.1, and Young's modulus for stainless steel, we limit the stress in any conductor to 90,000 psi and allow plastic deformation in the copper. Assuming a factor of safety of 1.5, we suggest that work-hardened stainless steel of yield stress greater than 135,000 psi (or equivalent non-magnetic material with $E \geq 30 \times 10^6$ psi) be the strength material in the tape.

4. Electrical Protection of the Magnet

If for any reason there is a normal transition in any part of a superconducting magnet, and it does not recover to the superconducting state, there will be ohmic heating of the normal region. In this event, the current in the conductor must be reduced to zero in a sufficiently short time to prevent excessive heating of the normal regions. The magnetic energy in a superconducting coil is customarily dissipated in one of three ways:

- a) Within the Dewar system,
- b) Transferred to an external coil, or
- c) Transferred to an external resistive load. In high energy systems, the third alternative is usually preferred and is the one proposed for this reactor.

Using the criteria suggested by Maddock,¹² it is found that there is sufficient stabilizing copper in the conductor to prevent "burnout" in the magnet. The average temperature rise is less than 50 K while limiting the peak induced voltage to 12,000 volts.

Figure 13.9 shows the system designed to protect the confining field magnet. Each coil has its own protection with $\frac{d\phi}{dt}$ sensors that will isolate the coil from the rest of the magnet should it suffer a normal transition.

It is assumed that the reactor could be kept on line until its power output decreases to one-half rated (i.e., 1000 MW). Inasmuch as power output varies as B^4 , we see that 84%, or about 40 of the 48 coils, is needed to generate the required field to attain 1/2 power, provided that the coils taken out of service are approximately evenly spaced. Should they not be evenly spaced, fewer coil outages can be tolerated, and the number varies as the asymmetry of the out-of-service coils increases.

The inductance of each coil is about 100 H, the external resistance is 1.2 ohms and the time constant for the current decay is 83.3 sec. The energy in each coil of 5×10^9 joules is dissipated in an artificial lake containing about 1.6×10^6 gallons of

744453

Fig. 13.9 Diagram of coil protection circuit

water, ($2.1 \times 10^5 \text{ ft}^3$) whose temperature will be raised by 0.2°C . Should all 48 coils undergo a normal transition, the temperature rise of the pool of water will be about 9.6°C .

5. The Supporting cylinder

Forty-eight coils, evenly spaced and keyed to the supporting cylinder comprise the toroidal field magnet assembly, (See Fig. 13.10). The cylinder is made of eight segments to match the assembly procedure for the reactor. Since each of the conductor strands is in tension and designed to be self-supporting, the segmented supporting cylinder is subjected only to the compressive magnetic pressure of 15,000 psi (the magnetic pressure at 160,000 gauss). The total centering force on the cylinder is about $5.52 \times 10^9 \text{ lb}$ or about $1.12 \times 10^8 \text{ lb}$ per coil.

The segmented cylinder is 14 m high, 2.25 m inside radius, 2.70 m outside radius and 0.45 m thick. It is made of work-hardened stainless steel with yield stress of 150,000 psi. The maximum stress in the cylinder, using a safety factor of 1.5, is 100,000 psi. Under maximum stress conditions, the cylinder deflects 0.75 cm. It weighs $2.2 \times 10^6 \text{ lb}$ and is housed in a segmented Dewar as shown in Fig. 13.11. This arrangement eliminates the need to transmit high forces through Dewar walls. The weight of the toroidal field magnet assembly is supported by low conductivity rods and pylons from the inside structure of the Dewar. Individual coils cannot be removed from the reactor unless the whole assembly is warmed up. Should one or more coils fail, the reactor continues to operate at reduced power (see above). The affected coils are repaired during a long-term shutdown.

Pulsed Field Coils

The vertical fields required to ignite, heat, stabilize, clean and control the plasma are pulsed over a period of 100 minutes, of which about 13 seconds is the rise portion of the pulse. For details of the pulse cycle see Chapter 5. There are three sets of vertical field coils. (See Fig. 13.12.) They are:

- a) The vertical field coils, placed inside the D coils, used to heat and ignite the plasma,
- b) The divertor field coils, placed inside the D coils, used to produce the poloidal divertor field,
- c) The control coils, placed outside of the D coil, used to induce a voltage to maintain the plasma current during the 97-minute burning period.

744497

Fig. 13.10 Isometric drawing of coil arrangement showing segmented support cylinder.

744454

Fig. 13.11 Dewar Construction for the confining field magnet

744378

Fig. 13.12 Cross section of the reactor

Pulsed Field Requirements and Losses

The vertical field and divertor coils are inside the D confining field coils and when pulsed are energized to full current in about 13 seconds. For economic reasons, these coils will be made of Nb₃Sn with a peak current density of 2000 amperes/cm² across the face of the coil. For compatibility with the D coil and refrigeration system, the material is Nb₃Sn similar to that shown in Fig. 13.6, but the amount of stainless steel and copper is tailored to the pulsed field operating conditions, i.e., to achieve a 30,000 gauss peak in about 13 seconds. This moderate operating condition should not cause severe losses in the coil itself, and multifilament wire will probably not be necessary for these coils. However, each of the coils is easily adaptable to multifilament material, either of Nb₃Sn (if developed) or NbTi.

When the vertical field and divertor coils are pulsed, flux is produced during the rise. Some energy is dissipated as eddy current losses in the normal material of the toroidal field coils (copper and steel). This adds to the heat load on the refrigeration system. Alternatively the coils can be shielded by a high conductivity shield with high heat capacity in which the ohmic losses can be cooled during the pulse. A shield of this kind is massive and necessitates large amounts of copper that otherwise would not be needed. Therefore, the decision not to shield the coils is economic; the added refrigeration required is nominal and is less expensive than constructing the normal, external copper shield on the D coils.

It can be shown that only a fraction of the energy, inversely proportional to the rise time, τ , of the pulsed field, is dissipated as eddy current loss when the pulsed field diffuses into the toroidal field coils. For a cylindrically shaped conductor, for example, the fractional loss is given approximately by the expression:

$$F = \frac{A}{8 \pi D \tau} \quad (13.2)$$

where A is the cross sectional area of the material through which the flux diffuses in cm², τ is the rise time in sec., and D is diffusion coefficient, $D = 10^9 \rho / 4\pi$, where ρ is the effective resistivity in ohm-cm.

The energy density, in joules per unit volume of normal metal through which the field diffuses, produced by the pulsed field coils is:

$$E = \frac{B^2}{8\pi} \times 10^{-7} \quad (13.3)$$

where B is the pulsed field strength in gauss. By combining expression 13.2 and 13.3 and

the expression for D, the diffusion coefficient, one obtains

$$E = \frac{B^2 A}{16\pi \rho T} \times 10^{-16} \quad (13.4)$$

where E is the portion of the energy in joules, dissipated as eddy currents induced in the normal material per unit length of confining field coil.

The added refrigeration required (in watts) is given by

$$R = \frac{1}{16\pi} \frac{B^3 AV}{\rho T} \times 10^{-16} \quad (13.5)$$

where V is the volume of the normal metal through which the field diffuses, and T is the period of the pulse.

Applying expression (13.5) to our case, taking into account the amount of copper and stainless steel in each of the 15 proposed sections, the resistivity and magneto-resistance of copper and stainless steel at 4 K and 160,000 gauss, respectively, with pulsed fields of up to 30,000 gauss, and using the rise time and period previously described, we find that the additional refrigeration required due to the diffusion of the pulsed field into the toroidal field is about 35 kW.

The pulsed field coils, the toroidal field coils and their Dewars are supported as shown in Fig. 13.12. During normal operation, the structure supports only the dead weight of the coils. These forces are transmitted through the Dewar walls by low conductivity supports.

Coil Design Fault Forces

In case of faults, the fault forces are supported by the bracing between coils. The fault forces, which are most severe between confining field coils and which could be as high as 10^8 lb, are transmitted through the Dewar by low conductivity compression members that do not touch unless the magnetic field becomes asymmetric. Movement of 0.5 cm is required before contact with the compression member is made.

There are a large number of possibilities of coil failures. Only two of the possible modes are presented. The first considers the fault forces on all remaining coils if any one of the 48 coils fails and is no longer energized. The second considers two adjacent coils failing under the same conditions as above. Tables 13.3 and 13.4 tabulate the forces on each of the remaining energized coils. We tabulate F_x , the radial force, F_z , the tangential axial force, F_t , the total force (the vectorial sum of F_x and F_z), M_{zx} , the overturning

Coil Number	Radial Force Fx (lbs)	Tangential Force Fz (lbs)	Total Force Ft (lbs)	Torque About Y-Axis Mzx (ft-lbs)
1	This Coil Fails			
2	-1.057D 08	-8.880D 07	1.381D 08	-8.388D 08
3	-1.060D 08	-4.161D 07	1.139D 08	-3.963D 08
4	-1.065D 08	-2.540D 07	1.095D 08	-2.426D 08
5	-1.069D 08	-1.714D 07	1.083D 08	-1.631D 08
6	-1.074D 08	-1.217D 07	1.081D 08	-1.145D 08
7	-1.078D 08	-8.897D 06	1.082D 08	-8.218D 07
8	-1.083D 08	-6.634D 06	1.085D 08	-5.968D 07
9	-1.086D 08	-5.015D 06	1.087D 08	-4.358D 07
10	-1.090D 08	-3.830D 06	1.090D 08	-3.187D 07
11	-1.093D 08	-2.949D 06	1.093D 08	-2.328D 07
12	-1.095D 08	-2.286D 06	1.095D 08	-1.695D 07
13	-1.097D 08	-1.782D 06	1.099D 08	-1.226D 07
14	-1.099D 08	-1.395D 06	1.099D 08	-8.791D 06
15	-1.101D 08	-1.095D 06	1.101D 08	-6.231D 06
16	-1.102D 08	-8.615D 05	1.102D 08	-4.352D 06
17	-1.103D 08	-6.773D 05	1.104D 08	-2.982D 06
18	-1.104D 08	-5.307D 05	1.104D 08	-1.995D 06
19	-1.105D 08	-4.128D 05	1.105D 08	-1.295D 06
20	-1.106D 08	-3.164D 05	1.106D 08	-8.106D 05
21	-1.106D 08	-2.361D 05	1.106D 08	-4.843D 05
22	-1.107D 08	-1.677D 05	1.107D 08	-2.728D 05
23	-1.107D 08	-1.075D 05	1.107D 08	-1.406D 05
24	-1.107D 08	-5.247D 04	1.107D 08	-5.834D 04
25	-1.107D 08	-2.290D 04	1.107D 08	-3.249D 07
26	-1.107D 08	5.246D 04	1.107D 08	5.828D 04
27	-1.107D 08	1.075D 05	1.107D 08	1.406D 05
28	-1.107D 08	1.677D 05	1.107D 08	2.728D 05
29	-1.106D 08	2.361D 05	1.106D 08	4.843D 05
30	-1.106D 08	3.164D 05	1.106D 08	8.105D 05
31	-1.105D 08	4.128D 05	1.105D 08	1.295D 06
32	-1.104D 08	5.307D 05	1.104D 08	1.995D 06
33	-1.103D 08	6.773D 05	1.104D 08	2.982D 06
34	-1.102D 08	8.615D 05	1.102D 08	4.352D 06
35	-1.101D 08	1.095D 06	1.101D 08	6.231D 06
36	-1.099D 08	1.395D 06	1.099D 08	8.790D 06
37	-1.097D 08	1.782D 06	1.098D 08	1.226D 07
38	-1.095D 08	2.286D 06	1.095D 08	1.695D 07
39	-1.093D 08	2.949D 06	1.093D 08	2.328D 07
40	-1.090D 08	3.830D 06	1.090D 08	3.187D 07
41	-1.086D 08	5.015D 06	1.087D 08	4.358D 07
42	-1.083D 08	6.634D 06	1.085D 08	5.968D 07
43	-1.078D 08	8.897D 06	1.082D 08	8.218D 07
44	-1.074D 08	1.217D 07	1.081D 08	1.145D 08
45	-1.069D 08	1.714D 07	1.083D 08	1.631D 08
46	-1.065D 08	2.540D 07	1.095D 08	2.426D 08
47	-1.060D 08	4.161D 07	1.139D 08	3.963D 08
48	-1.057D 08	8.880D 07	1.381D 08	8.388D 08

Table 13.3 Force and Torque Matrix;
One Coil Fails (48 Coil Torus)

Coil Number	Radial Force Fx (lbs)	Tangential Force Fz (lbs)	Total Force Ft (lbs)	Torque About Y-Axis Mzx (ft-lbs)
----------------	-----------------------------	---------------------------------	----------------------------	--

1	This Coil Fails			
2	This Coil Fails			
3	-1.002D 08	-1.304D 08	1.645D 08	-1.235D 09
4	-1.010D 08	-6.702D 07	1.212D 08	-6.390D 08
5	-1.019D 08	-4.254D 07	1.104D 08	-4.057D 08
6	-1.028D 08	-2.931D 07	1.069D 08	-2.776D 08
7	-1.037D 08	-2.106D 07	1.058D 08	-1.967D 08
8	-1.046D 08	-1.553D 07	1.057D 08	-1.419D 08
9	-1.054D 08	-1.165D 07	1.060D 08	-1.033D 08
10	-1.061D 08	-8.845D 06	1.064D 08	-7.545D 07
11	-1.067D 08	-6.780D 06	1.069D 08	-5.516D 07
12	-1.073D 08	-5.236D 06	1.074D 08	-4.023D 07
13	-1.077D 08	-4.069D 06	1.078D 08	-2.921D 07
14	-1.081D 08	-3.177D 06	1.082D 08	-2.105D 07
15	-1.085D 08	-2.490D 06	1.085D 08	-1.502D 07
16	-1.088D 08	-1.957D 06	1.088D 08	-1.058D 07
17	-1.091D 08	-1.539D 06	1.091D 08	-7.333D 06
18	-1.093D 08	-1.208D 06	1.093D 08	-4.977D 06
19	-1.094D 08	-9.435D 05	1.095D 08	-3.290D 06
20	-1.096D 08	-7.291D 05	1.096D 08	-2.106D 06
21	-1.097D 08	-5.525D 05	1.097D 08	-1.295D 06
22	-1.098D 08	-4.038D 05	1.098D 08	-7.571D 05
23	-1.099D 08	-2.751D 05	1.099D 08	-4.134D 05
24	-1.099D 08	-1.599D 05	1.099D 08	-1.989D 05
25	-1.099D 08	-5.247D 04	1.099D 08	-5.831D 04
26	-1.099D 08	5.247D 04	1.099D 08	5.831D 04
27	-1.099D 08	1.599D 05	1.099D 08	1.989D 05
28	-1.099D 08	2.751D 05	1.099D 08	4.134D 05
29	-1.098D 08	4.038D 05	1.098D 08	7.571D 05
30	-1.097D 08	5.525D 05	1.097D 08	1.295D 06
31	-1.096D 08	7.291D 05	1.096D 08	2.106D 06
32	-1.094D 08	9.435D 05	1.095D 08	3.290D 06
33	-1.093D 08	1.208D 06	1.093D 08	4.977D 06
34	-1.091D 08	1.539D 06	1.091D 08	7.333D 06
35	-1.088D 08	1.957D 06	1.088D 08	1.058D 07
36	-1.085D 08	2.490D 06	1.085D 08	1.502D 07
37	-1.081D 08	3.177D 06	1.082D 08	2.105D 07
38	-1.077D 08	4.069D 06	1.078D 08	2.921D 07
39	-1.073D 08	5.236D 06	1.074D 08	4.023D 07
40	-1.067D 08	6.780D 06	1.069D 08	5.516D 07
41	-1.061D 08	8.845D 06	1.064D 08	7.545D 07
42	-1.054D 08	1.165D 07	1.060D 08	1.033D 08
43	-1.046D 08	1.553D 07	1.057D 08	1.419D 08
44	-1.037D 08	2.106D 07	1.058D 08	1.967D 08
45	-1.028D 08	2.931D 07	1.069D 08	2.776D 08
46	-1.019D 08	4.254D 07	1.104D 08	4.057D 08
47	-1.010D 08	6.702D 07	1.212D 08	6.390D 08
48	-1.002D 08	1.304D 08	1.645D 08	1.235D 09

Table 13.4 Force and Torque Matrix;
Two Adjacent Coils Fail (48 Coil Torus)

moment about the Y axis. Because of symmetry, the vertical force F_y , and the overturning moment about the x axis, M_{zy} are essentially zero, and therefore not shown. The total radial force is 5.52×10^9 lb or 1.12×10^8 lb per coil.

Of the two cases tabulated, the first is the most probable mode of failure. On the other hand, it is clear that as the number of adjacent coils that fail is increased, the tangential forces increase. These are maximum when half of the torus becomes deenergized. Figure 13.13 is a plot of the maximum forces on any single coil and moments, F_x , F_z , F_t and M_{zx} as a function of the number of adjacent deenergized coils in the torus. The centering force, F_x , is maximum when all the coils are energized. The tangential force, F_z , and the torque about the Y axis, M_{xz} have maximum values when one half of the torus is deenergized. The total force F_t is a maximum when 20 coils are deenergized. Therefore the structure has been designed according to the following maximum conditions:

- a) $F_x = 1.12 \times 10^8$ lb per coil
- b) $F_z = 2.22 \times 10^8$ lb per coil
- c) $F_t = 2.30 \times 10^8$ lb per coil
- d) $M_{xz} = 2.05 \times 10^8$ ft-lb per coil.

Each of the above are considerably higher than the maxima tabulated in Tables 13.3 and 13.4. We choose to design the reactor to withstand the worst possible fault condition.

The compression pads, made of glass epoxy laminate, NEMA G-10, whose properties are given below, are designed for the worst fault conditions, both for strength and increased heat load. For strength, an allowable compressive stress of 10,000 psi is used. This results in each toroidal coil Dewar having 10 compression pads, a typical set of which is shown in Fig. 13.14, each with a compression area of 2250 in^2 .

When faults occur, causing some of the compression pads to touch, the leak through the Dewar increases. The outer pads are kept at liquid nitrogen temperature or lower, to minimize the heat load to the liquid helium. During the worst fault condition, the pads of 20 of the forty-eight coils will make contact. This results in an increased heat load to the helium of 80 kW above that discussed below.

Cryogenic Equipment

Dewar Design

The Dewar arrangement for the toroidal field coils is shown in Fig. 13.11. The construction is typical for all the Dewars. The Dewar is composed of an inner and

744462

Fig. 13.13 Plot of maximum fault forces and moments on any single coil as a function of the number of adjacent coil failures

744463

Fig. 13.14 Compression pads for use in fault conditions

outer vacuum-tight stainless steel wall. The vacuum space of 3 cm is filled with layers of superinsulation. A typical example of superinsulation made of glass paper and doubly aluminized mylar has been previously described.¹³ For this kind of superinsulation, an average thermal conductivity of 1.5×10^{-6} watts/cm K is easily attainable. Using this value for thermal conductivity, it is calculated that the heat loss through the superinsulation for all the Dewars is 80 kW.

It is proposed that the low conductivity supports be made of glass-epoxy laminate, NEMA G-10. Allowable design stresses for this material are: 10,000 psi in tension or compression and 2000 psi in shear at 4 K. Its thermal conductivity is 2.6×10^{-3} watts/cm K. The coils, including coil forms, supports, and compression pads, weigh a total of 2.1×10^7 lb, the loss due to conduction through the glastic supports is 70 kW.

During normal operating conditions, the losses due to heat transfer through the Dewar walls and from the glastic supports of all coil systems is 150 kW under the worst fault conditions, when operating at the 1/2 load condition described above this increases to 230 kW. Table 13.5 tabulates the refrigeration requirements from each of the coil systems.

Cryogenic Helium

During normal operation, the coolant is forced supercritical helium vapor. The required inventory is approximately 6.5×10^5 liters. The helium system is closed and is shown in Fig. 13.15. Helium, cooled to cryogenic temperatures by the refrigerator, is used to cool the magnet from 25 K to the starting temperature of 8 K. During normal operation, vapor is removed at a temperature of 10 K and a pressure of 15 psig, recondensed by the refrigerator to 8 K, and returned to the Dewar at 8 K and 60 psi.

The total helium produced both as ash and in the blanket is 10.2 m^3 STP per day or the equivalent of 14.6 liters of liquid helium. This is a 0.0022% of the inventory and is deemed sufficient for daily makeup of both cryogenic helium and hot helium gas coolant during normal operation. That not used for makeup will be stored for future use in major shutdowns.

Refrigeration Requirements

The refrigeration requirements, tabulated in Table 13.5 for all the superconducting magnetic field coils, are generated from the following four sources:

- a) The diffusion of the pulsed fields into the normal regions of the confining coil (discussed above).

744451

Fig. 13.15 Diagram of the closed helium system

Table 13.5

Required Refrigeration for Fusion Power Reactor

<u>Source</u>	<u>Loss, kW</u>
Pulsed Fields	35
Neutron Radiation (Confining Field & Vertical Field Coils)	95
Conduction (Through Coil Supports)	70
Conduction (Through Superinsulation)	80
Total Loss	280

- b) Conduction losses in the Dewar due to gravity supports of the coil (discussed above).
- c) Conduction in the Dewar in certain fault conditions (discussed above).
- d) Conduction and radiation heat transfer losses through the superinsulation (discussed above).
- e) Losses of 95 kW thermal due to neutron and gamma irradiation of all the coils (discussed in Chapter 10).

The heating due to neutron and gamma irradiation depends on the amount of attenuation in the blanket and the concrete shield. Chapter 10 describes the procedures and parameters used in designing the shielding blanket. For example, should the level of radiation prove to be excessive for convenient operation of the superconducting magnets and their Dewars, one can increase the thickness of the shield.

The sum of the losses due to each of the four components listed above is 280 kW, and the estimated power input is 84 MW during normal operation.

Refrigeration

Although only 280 kW of refrigeration is required, under normal conditions, about 360 kW is provided. The 29% excess is necessary to take care of initial cooling down of the magnets, extra refrigeration required for cooling down spare magnet sections, extra refrigeration required during fault conditions and other contingencies such as failure of spare refrigeration units. Nine units of 40 kW each comprise the refrigeration plant; during normal operation seven units are used. The refrigerators are of the multi-stage turbo expander type, and for 8 K operation require 300 watts input for each watt of refrigeration. Each unit is field erected with proper cross-piping and control consoles. Each of the units is about 15 feet in diameter and about 30 feet in overall height.

Neutron and Gamma Irradiation of Superconducting Magnet Systems

A superconducting magnet system usually consists of the superconductor, the structure, the coil supports, the Dewar with superinsulation and any electrical insulation required. Since irradiation cannot be completely prevented, one must examine the consequences on the superconducting magnet system.

The superconductor is composed of Nb_3Sn , copper and stainless steel. Chapter 12 discusses neutron damage to engineering materials likely to be present in fusion reactors including stainless steel, copper and insulation (both thermal and electrical).

Copper for superconducting applications is used because of its good electrical and thermal conduction, as well as for its high ratio of resistivity at 273 K to that at 4 K. The lower the resistivity of the stabilizing copper, the less is the I^2R loss when current flows in the normal material, and therefore, the better is the thermal stabilization during flux jumps.

The magnetoresistance of copper is relatively higher than high-purity aluminum. When subjected to neutron irradiation, however, the magnetoresistance of copper increases at a lower rate than that of aluminum making copper more suitable for use with high fluences.

When copper is subjected to neutron irradiation, the resistivity ratio decreases,¹⁴ which in turn decreases the stabilization properties of the copper. Fluences of 10^{18} to $10^{19}/\text{cm}^2$, however, do not seem to alter the properties of copper appreciably.

There is indication^{15,16} that neutron irradiation of Nb_3Sn up to fluences of from 10^{18} to $10^{19}/\text{cm}^2$ enhances the current carrying capacity of the superconductor, and also that subsequent moderate annealing restores previous properties. Recently, Schweitzer and Parkin¹⁷ have shown that there may be some drastic reduction in both critical temperature and critical current in Nb_3Sn at fluences of about $3 \times 10^{18}/\text{cm}^2$ (neutrons < 1.0 MeV). There are indications, however,¹⁸ that restoration of former properties is accomplished after annealing NbTi for about five minutes at temperatures from 60-270 K.

Very little is known about the properties of either thermal or electrical insulation when subjected to fluences of from 10^{18} to $10^{19}/\text{cm}^2$. There are even less data available on the behavior of these kinds of materials when irradiated at cryogenic temperatures.

For organic compounds such as polyester films and mylar, the threshold of mechanical damage occurs at a fluence of $3.3 \times 10^{15}/\text{cm}^2$ (for neutrons > 0.1 MeV).¹⁹

Inorganic compounds such as mica, ceramic and glass seem to be less susceptible to neutron damage. For example, at room temperature alumina was severely damaged at a fluence of $6 \times 10^{21}/\text{cm}^2$ (neutrons > 0.1 MeV).²⁰ There is some hope that the data will be more favorable at cryogenic temperatures, but no meaningful information seems to be available.

It is clear that this is an area of material testing that must be expanded in the near future. It is important to have data on properties for specific materials when the first experimental power producing reactors are built about a decade from now. As explained above, if necessary, neutron irradiation of the cryogenic system can be reduced below the levels used in this design by increasing the thickness of the radiation shield. This, however, will increase the capital cost of the reactor.

Power Supplies and Initial Startup

Power Supplies

1. Normal Charge

We seek to charge the 48 toroidal field coils simultaneously to attain operating conditions in about three hours. Under those conditions the stored magnetic energy of each coil is 5200 megajoules. To accomplish this we charge at an initial voltage of 1000 volts and continuously decrease it to 25 volts while the current rises to the operating level of 10,000 amperes. This necessitates a power supply capable of producing 1.25 megawatts peak power or an average power of about 0.6 megawatt for each coil.

2. Normal Discharge

The power supply described above will be used for normal discharge of the coil. We simply reverse the normal charging procedure, and the coils are discharged in about three hours.

3. Current Regulation

During operation the resistive losses of each coil circuit (copper coil leads, shunts, etc.) will be compensated for by a 10 kW supply operating at 10,000 amperes and 1 volt dc. There are forty-eight of these units. Current regulation provides a maximum absolute variation around the torus of 0.01%.

4. Emergency Discharge

In the event of a normal transition, equipment safety problem, or personnel hazard, it is necessary to reduce the stored energy as rapidly as possible. The rate is limited by the maximum coil terminal voltage allowed of 12,000 volts. This enables reduction at an initial rate of 120 A/sec to about 3700 amperes in an e-fold time, and about 200 in four e-folds (about 5 1/2 minutes). In the discharge procedures, the energy appearing as heat in the dump resistor will be dissipated in the artificial lake described above. See Fig. 13.9 for the circuit diagram of the emergency discharge circuit.

Initial Startup

The initial startup procedure is described in Chapter 18. However, cooling of the magnet in preparation for initial startup is accomplished in three steps as follows:

- a) From ambient to 80 K, the magnet is cooled with liquid nitrogen and vented to the atmosphere, using an average cooling rate of 2,000 kW.
- b) From 80 K to 25 K, it is cooled with liquid hydrogen and vented to the atmosphere with proper safety precautions, using an average cooling rate of 1,000 kW.

- c) From 25 K to 8 K, it is cooled with forced supercritical helium vapor that is recovered and reliquified during the cooling process, using an average cooling rate of 300 kW.

The total weight of the magnets is 2.1×10^7 pounds, and the time required to attain 8 K is approximately nine days. Figure 13.16 shows the magnet temperature as a function of time during cooldown.

744460

Fig. 13.16 Plot of temperature vs time
for initial cool-down of the reactor

References

¹M. S. Lubell, H. M. Long, J. N. Luton, Jr., W. C. L. Stoddart, The Economics of Large Superconducting Toroidal Magnets for Fusion Reactors, Proceedings of 1972 Applied Superconducting Conference (Institute of Electrical and Electronics Engineers, Inc., New York, 1972), p. 341.

²J. R. Powell, Design and Economics of Large DC Fusion Magnets, Proceedings of 1972 Applied Superconducting Conference (Institute of Electrical and Electronics Engineers, Inc., New York, 1972), p. 346.

³Carl D. Henning, Intermagnetics General Corporation Newsletter, Vol. 1, No. 2 (Fall 1972) pp. 1-3.

⁴J. File, R. G. Mills, and G. V. Sheffield, Large Superconducting Magnet Designs for Fusion Reactors, Princeton Plasma Physics Laboratory Report MATT-848 (1971).

⁵J. File and G. V. Sheffield, A Large Superconducting Magnet for Fusion Research, Proceedings of the Fourth International Conference on Magnet Technology (National Technical Information Service, Springfield, Va., 1972). p. 240.

⁶J. File, Proposed Superconducting Coils for the Princeton Fusion Reactor, to be published.

⁷H. R. Hart, Jr., Magnetic Instabilities and Solenoid Performance: Application of the Critical State Model, Proceedings of the 1968 Summer Study on Superconducting Devices and Accelerators (Brookhaven National Laboratory, Upton, New York, 1968) BNL 50155, Vol. 2, p. 571.

⁸P. S. Swartz and C. P. Bean, Model for Magnetic Instabilities in Hard Superconductor, The Adiabatic Critical State, J. Appl. Phys. 39, 4991 (1968).

⁹R. Hancox, Proceedings of the International Working Sessions on Fusion Reactor Technology, (Oak Ridge National Laboratory, Oak Ridge, Tennessee, 1971) Conf. - 710624, p. 296.

¹⁰C. D. Henning, R. L. Nelson, C. O. Ward, C. L. Rowe, D. W. Moon, Progress on Aluminum Clad Nb₃Sn, to be published.

¹¹R. Hancox, The Design of Niobium - Tin Strip Wound Magnets, Proceedings The Third International Conference on Magnet Technology (Deutsches Elektronen Synchrotron, Hamburg, West Germany, 1970) p. 950.

¹²B. J. Maddock and G. B. James, Protection and Stabilization of Large Superconducting Coil, Proceedings of Institute of Electrical Engineers (London) 115, 543 (1968).

¹³J. W. Willard, J. File and G. D. Martin, The Levitated Ring in FM-1, IEEE, Transactions on Nuclear Science NS-18, (1971), p. 283.

¹⁴J. A. Horak and T. H. Blewitt, Fast Neutron Irradiation Induced Resistivity in Metals, Phy. Stat. Sol. (a) 9, 721 (1972).

¹⁵G. W. Cullen, R. L. Novak and J. P. McEvoy, Effect of Neutron-Induced Defects on the Current-Carrying Behavior of Niobium Stannide, RCA Review, XXV, 479 (1964).

¹⁶E. R. Schrader and F. Kolondra, Analysis of Degradation Effects in Superconductive Niobium Stannide Solenoids, RCA Review, XXV, 582 (1964).

¹⁷D. M. Parkin and D. G. Schweitzer, Effects of Neutron Irradiation on the Superconducting Properties of NbTi and Nb₃Sn Multifilamentary Composites, Nuclear Technology 22, 108 (1974).

¹⁸M. Soell and S. L. Wipf, Change in Critical Current of Superconducting NbTi by Neutron Irradiation, Proceedings of 1972 Applied Superconducting Conference (Institute of Electrical and Electronics Engineers, Inc., New York 1972), p. 434.

¹⁹M. Olyphant Jr., Radiation Effects on Electrical Insulation, Minnesota Mining and Manufacturing Co., Electrical Products Laboratory (undated).

²⁰G. W. Keilholtz, R. E. Moore and H. E. Robertson, Fast-Neutron Damage to Polycrystalline Alumina at Temperatures from 60° to 1230°C, Nuclear Technology V, 17, (1973).

Chapter 14
Fuel Handling
E. F. Johnson

Introduction

This chapter describes the fuel flows throughout the fusion power reactor and the processing sequences that are necessary to maintain a reactor feedstock of adequate purity while assuring on the one hand self-sufficiency in respect to the tritium fuel and on the other reasonable control of tritium losses to the environment.

The principal fuel flows are shown as three major loops in Fig. 14.1, namely, the primary fuel loop, the coolant helium loop, and the breeder salt loop. In the primary fuel loop the reactor feed materials, deuterium, tritium, and argon, are circulated continuously through the reactor along with small amounts of helium and protium ash. Both ash species are removed continuously in the course of condensing the primary fuel stream.

Because of the permeability of most metals at high temperatures to hydrogen isotopes, it is unavoidable that appreciable amounts of these isotopes will diffuse throughout the reactor system and to some extent wind up in the coolant helium. Chapter 15 treats this problem at some length. Since the helium is used to heat the steam generators without the protection of an intermediate loop, its tritium pressures must be kept low to prevent losses of tritium to the steam cycle and subsequently to the environment. Low tritium pressures are achieved by maintaining a modest oxygen pressure in the helium to assure conversion of all hydrogenic species to water and by continuously removing the water by means of powerful desiccants or adsorbents.

In the breeder salt loop most of the tritium generated from the lithium in the salt is stripped out as tritium fluoride and recovered in cold traps. The small fraction of the tritium generation that is not stripped out permeates into the coolant helium, where it is recovered as water.

Whereas the principal constraint on tritium concentration in the coolant helium is the permissible permeation rate of tritium into the steam system, the principal constraint on tritium concentration in the breeder salt is the allowable solution of tritium in metal walls before embrittlement occurs. A secondary constraint is the allowable inventory of tritium. Because of the very large mass of the salt even low tritium concentrations correspond to substantial inventories. For example, one ppm tritium by mass represents nearly three kilograms of tritium tied up in the salt and decaying away.

744397

Fig. 14.1 Principal fuel flows.

Since it is likely that the tritium regenerated from the recovery systems on the coolant helium and the breeder salt will be contaminated somewhat with protium because of back permeation from the steam system, the regenerated tritium streams are fed back to the primary fuel loop just upstream of the ash removal system on that loop as shown in Fig. 14.1.

The foregoing provides a brief overview of the fuel handling features of the plant. We now examine in detail each of the three loops, their features and components, and the alternative possibilities.

Primary Fuel Loop

Principal Elements and Flows

The principal elements comprising the primary fuel loop are shown in Fig. 14.2, and typical flows and compositions are summarized in Table 14.1.

A variety of processing schemes might be employed to effect the purification of the primary fuel flow. We have chosen to rely on fractional distillation because the technology for the hydrogen-deuterium system is well known,¹ and the requirements for mechanical apparatus are minimal.

The principal elements in the primary fuel flow path are the injectors, the reactor, the divertors, coolers and traps, vacuum pumps, a compressor, precooler, the primary fractionator, and the fuel storage facilities at the ejectors.

Some deuterium and tritium escape from the primary loop by permeation into the coolant helium at the divertors and at the high temperature end of the coolers before the vacuum pumps. The amounts, though small relative to the total fuel flow rates, may be of the same order of magnitude as the burn-up. They are routinely accommodated by the tritium processing system for the coolant helium.

Once the fuel stream temperatures fall below about 300°C the possibilities for loss through permeation of metal walls vanish, and coolants other than helium may be used to reduce the temperature to the low levels necessary to avoid excessive thermal loads on the vacuum pumps. These pumps are conventional, albeit large, mercury diffusion pumps followed by mercury injector pumps exhausting gases at 40 torr.

After leaving these pumps the reactor exhaust gas is compressed to approximately 10 atm in a multiple-stage hydrogen compressor, then cooled almost to its dewpoint before passing into the primary fractionator tower. In the tower the gas is virtually entirely condensed to liquid except for the helium ash and protium.

744396

Fig. 14.2 Primary fuel loop.

Table 14.1: Typical Flows in Primary Fuel Loop

Component	Reactor g/hr	Feed mole fraction	Vacuum Pump Effluent g/hr	Precooler Effluent g/hr	mole fraction	Primary Fractionator Bottoms g/hr	mole fraction	Exit Gas g/hr	Cyclic Condenser Exit Gas g/hr	Secondary Fractionator Overhead g/hr
He			29.3	29.3	0.100			29.3	29.3	
H			0.0	0.0	0.000					0.3
HD			0.15	0.15	0.001			0.9	0.008	< 0.01
HT			0.10	0.9	0.003			0.02	0.000	< 10 ⁻⁷
D ₂	77.1	0.277	77.0	77.6	0.263	62.2	0.236			
DT	219.8	0.632	182.8	182.8	0.497	219.8	0.667			
T ₂	0.0		0.0	21.5	0.049					
Ar	252.8	0.091	252.8	252.8	0.087	252.8	0.097			
Total	549.6	1.000	542.2	565.1	1.000	534.8	1.000	30.2	29.3	0.3

ash, which escape overhead. The bottoms from the tower, i.e., the liquid leaving the bottom of the tower, flows to storage, from which it is mixed with make-up deuterium and reinjected into the reactor.

The operation of the primary fuel loop may be regarded as a recycling of a stream consisting of deuterium, tritium, and argon with some appearance and subsequent loss of helium and protium ash at the expense of some of the deuterium and tritium, which expense is made up by the addition of deuterium from outside purchase and of tritium from the tritium recovery systems on the coolant helium and the breeder blanket.

The change in composition of the fuel stream per pass through the reactor is of the same order as the fractional burn-up, roughly a ten mole percent drop for the total deuterium and tritium and a corresponding ten mole (atom) percent rise for the helium. Thus, the helium must be removed continuously from the reactor effluent to maintain adequate compositions in the reactor feed. In principle, the protium ash, which is produced at a much lower rate, could be removed intermittently. Neither of these separations poses any serious problems, and the equipment requirements for effecting them are so modest that there is no advantage in attempting to define them in great detail at this stage of development. We describe below their likely features.

Primary Fuel Loop Purification System

1. Choice of System

Both cryogenic fractional distillation and multistage permeation of metal membranes are attractive possibilities for separating the isotopes of hydrogen from each other and from other gases. Preliminary studies by Anastasia and Maroni² on the former and Kobisk³ on the latter attest to the likely practicability of either process. Permeation has the advantage of proceeding atomically with the result that the separation of the individual isotopes is not confounded by the formation of HD and HT. Furthermore, since the streams are handled as gases during the separation processes, the tritium inventories can be somewhat smaller. Nevertheless the principal advantages would appear to lie with the fractional distillation system for the following reasons.

a) The fractionation can be effected in the normal course of condensing the fuel gas for subsequent reinjection. With permeation systems the fuel gas would have to be compressed and heated to temperatures optimal for the permeation.

b) The total liquid phase holdup necessary for proper functioning of the fractionators is relatively small because the towers are small; hence the tritium holdup in the purification system can be small.

c) Although the hydrogen isotopes permeate metals at different rates, the differences in rates of surface processes may be in the opposite sense.

Figure 14.3 shows a possible purification system for the primary fuel loop. We make no claim that the system is optimal, only that it is practicable and relatively simple. The characteristics of the system are described below in terms of each of the major parts of the system.

2. Precoolers

Because of the low mass flow rates of the primary fuel stream and the availability of cold helium from the magnet's cryostat, the simplest procedure for condensing the fuel is to precool it to its dewpoint by indirect heat exchange with helium and then condense it by direct contact with refluxing liquid fuel in the primary fractionator.

Vapor pressures for argon, recomputed from data in Perry,⁴ are shown in Fig. 14.4, and the corresponding dewpoints for various argon concentrations in the fuel gas and various total pressures are presented in Fig. 14.5, assuming the fuel gas behaves ideally in the ranges of conditions covered by the figure. At a total pressure of 10 atm and an argon content of 0.10 mole fraction the gas exit from the precoolers should not be much cooler than 90 K.

If we assume the aftercoolers (not shown) on the compressor hold the fuel gas temperature to 300 K, the precooler duty is to cool the gas roughly 200°. A subsidiary function of the precooler is to trap out any small amounts of high-Z material picked up by the fuel stream. Any such accumulation would be melted out periodically and drained to the drag stream fractionator for subsequent elimination from the loop.

3. Primary Fractionator

The primary fractionator consists of a column, 3 cm in diameter and 2 m tall, filled with packing like Podbielniak Heli-Pak giving a separating power equivalent to one theoretical plate per cm height,⁵ a small reboiler at the bottom, and at the top a total condenser and a small reflux accumulator.

On entering the column at a point in the lower half of the column the feed stream encounters a liquid down flow or reflux, which is several times the gas mass flow rate, with the result that most of the gas is quickly cooled and condensed to liquid as it is swept up the column by the cold vapors rising from the reboiler. In distillation parlance the section of the column above the feed entry is an enriching section because the vapors rising through it become progressively richer in the more volatile species present in the vapor, for example, H_2 and HD, whereas the section of the column below

744395

Fig. 14.3 Primary fuel purification system.

744394

Fig. 14.4. Vapor pressure of argon.

744393

Fig. 14.5 Dewpoint of argon in reactor effluent.

the feed is a stripping section because the liquid downflow becomes increasingly stripped of its more volatile species as it moves down through the packing.

Since the column is operated under essentially total reflux condition, i.e., all the condensable vapor emerging from the top of the column is condensed and returned to the column as liquid downflow, the separating power is maximized. The only flows out of the fractionation system are the liquid bottoms stream out of the reboiler and the helium gas stream leaving the vapor space of the reflux accumulator. The former contains only argon, tritium and deuterium (mostly as DT and D₂), and the latter contains all the helium ash and protium ash plus deuterium as HD and very small amounts of tritium as HT.

At total reflux the separating capability of a column of a given number of theoretical plates can be estimated readily from Fenske's equation,

$$\left(\frac{x_A}{x_B}\right)_{\text{top}} = \alpha^n \left(\frac{x_A}{x_B}\right)_{\text{feed}} \quad (14.1)$$

where

- x_A = mole fraction of more volatile constituent
- x_B = mole fraction of less volatile constituent
- α = relative volatility of A with respect to B
- n = number of theoretical stages between feed point and top of column.

Vapor pressure and melting point data for the components in the fuel streams are summarized in Table 14.2,^{6,7,8} and vapor pressure curves for the hydrogen isotopes, recomputed from the tabulations of Mittelhauser and Thodos,⁸ are presented in Fig. 14.6.

For ideal solutions the relative volatility is given by the ratio of the vapor pressures of the pure species at the particular temperature of interest. The small amount of protium in the feed will tend to be present as HD and HT in a distribution equal to the distribution of D and T. For the purposes of this illustration we will lump the HD and HT together as a single species, A, and the remaining D₂, T₂ and DT as species B. The averaged vapor pressures around 35 K yield an equivalent α of 1.14. From Table

Table 14.2. Some Properties of Primary Fuel Stream Components

	He-3	He-4	n-H ₂	HD	HT	n-D ₂	DT	n-T ₂	Ar
Molecular Weight	3.016	4.003	2.016	3.022	4.025	4.028	5.031	6.034	39.95
Triple Point, K			13.96	16.60	17.62	18.73	19.71	20.62	84.0
P, atm			0.071	0.122	0.144	0.169	0.192	0.213	
Normal Boiling Point, K			20.39			23.67		25.04	87.5
Critical Temperature K	3.31	5.19	33.24	35.91	37.13	38.35	39.42	40.44	151.2

744392

Fig. 14.6. Vapor pressures of H isotopes (triple point to critical point).

14.1 the molar ratio of the column feed (precooler effluent) is 0.004/0.809, whence, for 100 theoretical plates

$$\left(\frac{x_A}{x_B}\right)_{\text{top}} = 1.14^{100} \left(\frac{0.004}{0.809}\right) = 2.4 \times 10^3 \quad (14.2)$$

Thus $x_{HD} + x_{HT}$ at the top of the column will exceed 0.999, and inverting the calculation for the bottom of the tower for only ten theoretical plates, the concentration of protium would be less than 0.001 mole fraction.

As can be seen from Fig. 14.6 the relative volatilities increase with decreasing temperature, and consequently improved separation could be achieved by operating the tower at lower pressures. Unfortunately the limited solubility of solid argon in hydrogen places a lower bound on system temperatures if we wish to avoid the difficulties inherent in handling slurries. Figure 14.7 presents an estimate of the solubility of argon in ideal solutions, from which it can be seen that reboiler temperatures should exceed about 36 K, requiring that we operate the column at pressures around 10 atm.

The solubility of argon was estimated from the thermodynamic expression for the temperature coefficient of solubility of a solute as a function of its heat of solution⁹

$$\left(\frac{\partial \ln x}{\partial T}\right)_P = \frac{L}{RT^2} \quad (14.3)$$

where

- x = mole fraction solute
- L = molar heat of solution of solute
- R = molar gas constant
- T = K.

By assuming that the heat of solution of argon changes little with temperature we may integrate Eq. 14.3 from the melting point of argon, 83.95 K, where $x = 1.0$, and the heat of solution equals the heat of fusion, 290 cal/g mole,¹⁰ down to any temperature T to get

$$\ln x = \frac{290}{1.987} \left(\frac{1}{83.95} - \frac{1}{T} \right) \quad (14.4)$$

744391

Fig. 14.7 Ideal solubility of argon in liquids.

The fractionator bottoms stream flows to storage where it is ready for reinjection except for the fact that it is deficient in deuterium by the amount of burnup in the reactor. For lack of better insights into the injection problem, we assume that the deuterium can be added in a tightly controlled way to the argon-fuel stream from storage to provide the requisite composition for manufacturing fuel pellets. Given the insolubility of argon in the fuel at low temperature it is clear that the pelletizing and injection operations will pose difficult technological challenges. If the pellets are solidified by evaporative cooling of fuel droplets, the actual feed to the pelletizer must be richer in fuel species than the pellet entering the plasma.

Since the primary fuel loop involves a continual recycling, there must be provisions for preventing the buildup of undesirable materials in the flowing stream. Volatile materials routinely get carried off in the helium ash overhead from the primary fractionator, but a drag stream is necessary to accommodate the heavy impurities. A small fractionator is shown in Fig. 14.3 deriving its feed from a drag stream on the primary fractionator bottoms and also from any solids accumulation in the precoolers. This drag stream fractionator presumably would run intermittently, accumulating impurities in its reboiler, which from time to time would be rejected to a final tritium containment control.

The condensate temperatures in the reflux condenser and reflux accumulator exert a sensitive control on the hydrogen concentrations at the top of the primary fractionator. For a given protium feed rate to the column the lower these temperatures can be maintained, the higher the concentration of protium and protium deuteride at the top of the column, and consequently the lower the recycle of tritium to the secondary fractionator. Even though no molecular protium (H_2) enters the primary fractionator, it will tend to accumulate at the top of the column by virtue of some equilibration via the reaction,

which will be driven to the right because the products are respectively more volatile and less volatile than either of the reactants. The protium will tend to be carried off in the helium gas leaving the reflux accumulator, and the DT will tend to be carried down the column in the liquid reflux. If necessary, the equilibration can be speeded up by inserting appropriate catalysts like iron oxide or silica gel¹¹ in the top of the column and in the reflux accumulator.

We have assumed that the total protium entering the primary fuel loop is of the order of 0.3 g atoms H per hour, one-third of which is produced in the reactor, and

two-thirds is the result of back permeation of hydrogen from the steam side of the boilers into the coolant helium. The latter enters the primary fuel loop along with the tritium recovered from the coolant helium. It is likely that the back permeation will be at a much lower rate (see Chapter 15), hence our design is conservative.

4. Cyclic Condenser

The helium ash leaving the reflux accumulator passes through a cyclic condenser maintained at temperatures below 10 K by cold helium gas from the magnet cryostat. This condenser serves as a cold trap, freezing out most of the hydrogen isotopes in the helium. It consists of a pair of heat exchangers, one of which is on-stream, the other off-stream, being melted out. The frequency of alternating the exchanger duties can be quite low because the tritium flow rate at this point in the system is very small, equal at most to small fractions of a gram per hour.

The helium leaving the cyclic condenser is discharged through a pressure control valve into a small storage tank from which it can be pumped into the coolant helium to ensure that any small amounts of tritium carried by the ash will be subjected to the tight tritium recovery system on the coolant loop. Alternatively the helium ash could be subjected to final tritium control without going through the coolant helium loop inasmuch as the amount of tritium under normal conditions of operation, less than 0.001 g/hr, is not worth recovering. However this alternative would require the installation of an oxidizer to convert the hydrogen isotopes in the ash to water, hence somewhat greater process complexity and capital cost.

The hydrogen isotopes melted out of the off-stream half of the cyclic condenser drop to a receiver for subsequent feed to the secondary fractionator.

5. Secondary Fractionator

The secondary fractionator is a small distillation column less than 2 cm in diameter, approximately 2 m tall, filled with a packing to provide a separating power equivalent to 100 theoretical plates. Ancillary equipment includes the receiver on the cyclic condenser, which serves as a feed tank, an equilibrator (a small vessel containing solid catalyst), a reboiler at the bottom of the column, and a total condenser at the top.

The principal purpose of this fractionator is to produce an overhead stream of protium essentially free of tritium. Because of the low mass flow rate of the protium inflow to the primary loop, this unit, like the primary fractionator, can be operated under conditions close to total reflux. Furthermore, since there is no constraint on temperature because of argon, the column can run at atmospheric pressure, with

temperatures at the top around 20 K and around 23 K at the bottom, corresponding to pure protium and pure deuterium respectively.

To make a conservative estimate of the tritium content in the overhead stream from the column let us assume that the feed enters as HD and HT in a molar ratio of 2 to 1 and ignore the contribution of the equilibrators. At 20 K the relative volatility of HD with respect to HT is 1.33, and for 50 theoretical plates between the feed and the top of the column and operation at total reflux, the resulting ratio of HD to HT in the overhead based on Fenske's equation will be $(1.33)^{50} \times (1.5) = 2.3 \times 10^6$. Since the feed will contain as much as 0.2 mole fraction H and since the relative volatility of H_2 with respect to HT is over 2.0, the actual ratio of T to H in the overhead can be far below 10^{-6} .

Despite this low tritium content the overhead stream is oxidized to water in a small reactor, and the water is impounded on a strong desiccant like magnesium perchlorate and stored until the tritium content has decayed to levels low enough for dumping.

Energy Requirements on the Primary Fuel Loop

The energy requirements for the primary fuel loop exclusive of those for the fuel injectors and the vacuum and forepumps and for reactor and divertor cooling, all of which are dealt with elsewhere, are quite modest. We shall consider only the two principal requirements, which are those associated with driving the compressor and those imposing a duty on the magnet's cryostat.

1. Work of Compression

To simplify the computation we assume that the work load on the compressor is equivalent to isothermally compressing 600 g/hr hydrogen from 0.04 atm to 10 atm at 300 K. This work is given exactly for unit mass by

$$w = H_2 - H_1 - T (S_2 - S_1) \quad (14.6)$$

where

H = enthalpy, cal/g

T = K

S = entropy, cal/g K.

From temperature-entropy charts in Perry¹² we find

$$w = 600 [955 - 955 - 300 (10.6 - 15.4)] = 864,000 \text{ cal/hr}$$

or

$$864,000 \frac{\text{cal}}{\text{hr}} \cdot \frac{\text{watt hr}}{860.6 \text{ cal}} = 1004 \text{ W ,}$$

which for 50% efficiency is 2kW.

2. Cooling Duty

Again to permit use of the temperature-entropy charts for hydrogen we assume that the gross flow is equivalent to 600 g/hr hydrogen and that the boilup rate in the primary fractionator is three times the gross flow rate, i.e., the condensing duty on the reflux condenser is that required to condense four times the gross flow rate. Then at a total pressure of 10 atm:

at precooler, cool 600 g from 300 K to 35 K

$$600 (H_{300} - H_{35}) = 600 (955 - 122) = 499,800 \text{ cal/hr;}$$

at reflux accumulator

$$4 (600) (H_{35} - H_{20}) = 2400 (122 - 10) = 269,000 \text{ cal/hr;}$$

at secondary fractionator condenser

assuming 100 g/hr boilup (operating

$$\text{at 1 atm) } 100 (100 - 10) = 9,000 \text{ cal/hr;}$$

Total	777,800 cal/hr
-------	----------------

Losses, etc.	777,200
--------------	---------

Total duty	1,555,000 cal/hr
------------	------------------

or approximately 2 kW cooling load on the magnet's cryostat. If we assume the electric power requirement for cooling in this temperature range is 30W/W cooling duty, the net power need is 60kW.

Equipment Costs

Because of the small scale of much of the fuel processing equipment on the primary fuel loop, it is difficult to estimate reliably the likely capital costs. Assuming all the equipment is made of alloy steels and designed for high pressure leak-tight operation, the individual costs of the major units might run as follows:

Compressor	\$5000
Precoolers	5000
Primary Fractionator	10000
Cyclic Condenser	5000
Secondary Fractionator	4000
Drag Steam Fractionator	4000
Storage Tanks, etc.	<u>10000</u>
Total Equipment Cost	\$43000

Using a Lang factor of 5.0^{13} the capital cost of the installed process equipment, fully insulated and instrumented, then would be $5 \times 43,000 = \$215,000$.

Since the whole operation would run automatically, the operating costs will be very small. The major element in these costs is the amortization charge for the capital investment. As was shown in the preceding section, the energy requirements are modest.

Coolant Liquid Helium

Problems and Perspectives

1. Sources of Hydrogenic Species

From the standpoint of fuel processing the principal elements in the coolant helium loop are the divertors, the blanket, and the steam generators. Deuterium and tritium enter the helium at the divertors, tritium enters at the blanket, and protium enters at the steam generators. Although there are considerable uncertainties as to the magnitudes of these permeations, it is certain that the coolant helium must be processed continuously to ensure that its tritium pressure is maintained at low enough levels to avoid excess permeation of tritium into the steam cycle.

2. Principal Constraint

To be conservative we have assumed that the total amount of hydrogen isotopes to be recovered from the coolant helium system is of the order of 12 g atoms/day, which is two orders of magnitude greater than estimated permeation rates. It is likely that the actual amount to be recovered will be much less. However, the overriding requirement for the recovery system is not related to the actual amount to be recovered but rather to the effective tritium pressure that can be maintained in the coolant helium. As pointed out in Chapter 15, for tritium pressures in the coolant helium below 10^{-15} atm the permeation of tritium through the tube walls of the steam generators and into the steam cycle will be at physiologically safe levels. The only practicable way by which these

very low pressures can be achieved is to tie up the tritium and other hydrogen isotopes as molecular compounds that are thermodynamically stable under the conditions in the coolant loop. Possible compounds are oxides (water) and metal hydrides. For systems with metal walls stable toward oxidizing atmospheres, as is the case in this reference design, the advantages would appear to lie with water. By maintaining an oxygen pressure of 5×10^{-3} atm in the coolant helium and removing water continuously to hold the water pressure at or below 10^{-5} atm, the tritium pressure can be held well below the required level. The tritium recovery problem thus is essentially one of drying helium gas and regenerating tritium gas from the collected water.

3. Contaminants

It is not clear at this point what impurities will build up in the coolant helium. Certainly there will be some contamination from the lubricants used in the primary helium pumps. This contamination will preclude lubricating these pumps with conventional mineral oil lubricants. Something like chlorofluorocarbons or other hydrogen-free synthetic lubricants¹⁴ will be required to prevent isotopic exchange with the lubrication system.

4. Choice of Drying Process

There are three industrial methods for drying gases in common use, and all could be used for maintaining low water pressures in the coolant helium. They are: condensation by refrigerating the gas, physical adsorption on high-surface-area solids, and chemical absorption by appropriate desiccants. Condensation is simply cold trapping. By cooling the helium to very low temperatures the water content can be reduced to virtually any level desired. The only disadvantage is that the operation can be costly if large masses of helium must be cooled. A major advantage is that the water is readily recovered as liquid by periodic warming of the trap.

Physical adsorption on activated alumina or molecular sieves can effect the same drying at much higher temperatures than simple condensation.^{15,16} In this method the water is regenerated from the adsorbent as vapor and subsequently condensed; hence there is an additional process step over simple cold trapping. Another disadvantage is that there will be an appreciable tritium inventory in the residual moisture in the adsorber beds.

If large masses of helium must be processed to remove the water, the chemical absorption using a powerful desiccant like magnesium perchlorate would appear to be an attractive alternative. Again there is the disadvantage that the water is regenerated as vapor and only with considerable difficulty at fairly high temperatures. Furthermore the

desiccant undergoes changes in crystalline structure in successive absorption-deabsorption cycles with the result that the turnover of desiccant in a given absorber would be fairly high necessitating a considerable handling of solids. Because of this last difficulty, we shall limit our consideration to physical adsorption.

Principal Elements and Flows

The principal elements in the coolant helium loop and related processing system are shown in Fig. 14.8, and the principal flows and stream compositions are summarized in Table 14.3.

The helium stream to be processed is taken from the helium effluent leaving the boiler feedwater heaters, where the helium is at its lowest temperature in the coolant loop, 66°C. The size of the helium stream depends on the amount of water to be removed and the maximum allowable concentration of water in the helium. For a removal rate equivalent to 12 g atoms per day of hydrogenic species and a maximum water pressure of 10^{-5} atm, approximately 5000 kg helium per hour must flow through the processing system. While this flow rate is small relative to the total coolant flow, comprising less than 0.05%, for a cold trapping system it would impose a large refrigeration load because the processing stream would have to be cooled below -80°C to condense out the water.

As shown in the figure the sequence of steps in the processing system is first a modest compression to maintain the drag stream gas flow through the system followed by adsorption of the water on commercial molecular sieve adsorption in the on-stream adsorber and then return of the dried helium to the coolant loop. For an adsorbent like Linde Type 5A (average pore diameter 5Å) there is no need to reduce the temperature of the incoming helium, and because the mass of water adsorbed is small there is no heating effect from the adsorption. The water in the off-stream adsorber is vaporized by heating the adsorber to 260°C and subjecting it to a vacuum of a micron or so (10^{-5} atm), then it is condensed to a solid in a liquid-nitrogen cooled trap. Periodically the water is melted out of the trap and collected in a small receiver from which it flows to a simple purification unit and thence to the small electrolysis cell, where it is split into oxygen and hydrogen.

The oxygen emerges from the electrolysis cell at near atmospheric conditions of temperature and pressure and saturated with water vapor from the cell. It passes through a nitrogen cold trap to recover the water and thence to storage for subsequent reintroduction to the coolant helium or other disposition. The stream of hydrogen

744449

Fig. 14.8 Fuel recovery system on coolant helium loop

Table 14.3. Principal Fuel Flows

Coolant Helium Loop

Species Entering Coolant (g/day)	<u>H</u>	<u>D</u>	<u>T</u>
From Divertor	-	14	12
From Blanket	-	-	2
From Steam	0.2	-	-
<u>Coolant Composition (atm)</u>	<u>HTO, DTO etc.</u>	<u>O₂</u>	<u>He</u>
To Adsorbers	10 ⁻⁵	5 x 10 ⁻³	51
Exit Adsorbers	10 ⁻⁷	5 x 10 ⁻³	50

Coolant Flows

To Adsorbers: 5 x 10³ kg/hr.

Table 14.4. Flows and Concentrations

Tritium Processing System on Breeder Salt Loop

Salt Flow through Disengagers: 4.00 x 10⁶ kg/hr.

Mole Fraction TF in Salt:

Entering Disengagers: 2.1 x 10⁻⁷; Exit Disengagers: 1.4 x 10⁻⁷

Disengager Exhaust Gas Composition (atm)

TF	1.0 x 10 ⁻⁴
He	1.54 x 10 ⁻⁴
Salt	1.3 x 10 ⁻⁴

Tritium Recovery 548 g/day

Helium Discharge 1120 g/day

isotopes also passes through the same kind of trap before being drawn over to the primary fuel loop processing system.

Major Process Units

1. Pumps

There are only two pumps in this system, namely the drag stream compressor and the vacuum pump. The latter has to produce a relatively modest vacuum on a volume of less than a cubic meter over perhaps a period of 24 hr, and hence even with a small purge stream a small, laboratory-scale, one-or-two-stage, mechanical vacuum pump should be adequate.

The compressor, however, is somewhat larger. Its pumping duty may be computed by a material balance on the processing system. Thus, for removing n g mole water per hour from the coolant helium processing stream, the necessary flow rate of the stream is given by

$$N_{\text{He}} (y_i - y_o) = n \quad (14.7)$$

where N_{He} is the molar flow rate of the gas stream in g atoms helium per hour and the y are the mole fractions of water in the inlet and outlet streams respectively. Strictly speaking this equation is correct only if N is the molar rate of the helium alone and the y are the molar ratios of water to helium, but the water concentrations are so low that the equation is quite accurate as we use it.

Although the gas pressure at 50 atm is high for ideal gas behavior, and the gas species are too disparate chemically for ideal solution behavior, there is no advantage at this juncture in taking into account non-ideal behavior, and hence to a useful first approximation we may assume Dalton's law of partial pressures applies and $y_i = p_i/P$, where p_i is the vapor pressure of water in the inlet stream and P is the total gas pressure, 50 atm.

Fig. 14.9 shows the vapor pressure of ice as a function of temperature. If the gas stream leaves the adsorber with a dew point of 184 K, $p_o = 10^{-7}$ atm, and Eq. 14.7 becomes, for $p_i = 10^{-5}$ atm and 12 g atoms hydrogen to be removed per day

$$N_{\text{He}} \frac{(10^{-5} - 10^{-7})}{50} = \frac{12 \text{ g atom hydrogen/day}}{\frac{2 \text{ g atom hydrogen}}{\text{g mole water}} \cdot 24 \text{ hr/day}}$$

744389

Fig. 14.9 Vapor pressure of ice (H₂O).
(Recomputed and extrapolated from CRC Handbook
of Chemistry and Physics, 48th edition).

or

$$N_{\text{He}} = \frac{50 (0.25)}{0.99 \times 10^{-5}} = 1.26 \times 10^6 \text{ g atom per hr}$$

or 1260 kg atom per hr or 1260 (4.00) = 5040 kg per hr.

In order to estimate the pumping power required to move this quantity of helium through the processing system we must know the pressure drop across the system, which for all practical purposes is the pressure drop across the adsorbent bed. As shown in the description of the adsorbers below, the actual amount of adsorbent required for drying the helium is small relative to the size of vessel necessary to accommodate the helium flow, and consequently the pressure drop can be correspondingly small. We shall make the arbitrary assumption that the design of the adsorbers will be such that the average pressure drop across them when onstream will be of the order of one atmosphere.

From the temperature-entropy diagram for helium in Barron¹¹ we find that the enthalpy change for raising pressure from 40 atm to 60 atm at an entropy of 23 joule/g K, corresponding to temperatures near ambient and above, is constant at approximately 200 joule/g. Thus the ideal work of adiabatic compression from 50 atm to 51 atm is equivalent to

$$200 \frac{\text{joule}}{\text{g}} \cdot \frac{(51 - 50)}{(60 - 40)} \cdot \frac{5040 \text{ kg}}{3600 \text{ sec}} \cdot \frac{1 \text{ W sec}}{\text{joule}} \cdot \frac{1 \text{ kW}}{1000 \text{ W}} \cdot \frac{1000 \text{ g}}{\text{kg}} = 14.0 \text{ kW.}$$

Although the absolute gas pressure is moderately high, the incremental pressure differential generated by the compressor is small suggesting that a relatively simple centrifugal compressor with correspondingly simple lubrication and sealing might be appropriate for this duty.

2. Adsorbers

Adsorption Equilibrium

A convenient representation of the equilibrium characteristics of an adsorbent for drying gases is to plot the equilibrium dew point of the gas effluent from the adsorber bed versus the bed temperature for various equilibrium loadings of water on the adsorbent. For each particular loading the resulting plot is a straight line called an isostere. Fig. 14.10 shows a few isosteres at low loadings for Linde Type 4A^o molecular sieve adsorbent adapted from Lee.¹⁶ This plot in conjunction with Fig. 14.9 permits an immediate identification of the water vapor pressures achievable for various loadings and bed temperatures. It also shows what conditions of vacuum and temperature are necessary for regenerating the adsorbent bed to particular equilibrium loadings. Thus for a bed temperature of 66°C at a loading of the adsorbent of 0.5 g water per 100 g adsorbent the

741108

Fig. 14.10 Isosteres for water on type 4A Molecular Sieve.

(Union Carbide)

gas leaving the adsorber will have a dew point of -87°C or 186 K, which from Fig. 14.9 corresponds to an equilibrium water pressure of 1.1×10^{-7} atm. To reduce the equilibrium loading from 0.5 to 0.2 g per 100 g adsorbent on the regeneration cycle will require that the bed be heated to 260°C and either purged with a gas having a dew point of -67°C or pumped down to a pressure corresponding to that dew point, namely, the equilibrium vapor pressure of ice at -67°C or 206 K, 2×10^{-6} atm.

Adsorber Size

The adsorbers are typically vertical, cylindrical vessels with diameter dependent on the gas throughput and the height dependent on the required depth of packing.

For a gas rate of 1260 kg atom helium per hour the volumetric flow rate at 66°C and 50 atm is

$$1260 \frac{\text{kg atom}}{\text{hr}} \cdot \frac{22.4 \text{ m}^3}{\text{kg atom}} \cdot \frac{\text{hr}}{3600 \text{ sec}} \cdot \frac{1}{50} \cdot \frac{(66 + 273.2)}{(273.2)} = 0.195 \text{ m}^3/\text{sec}.$$

and at a reasonable superficial gas velocity (i.e. the gas velocity ignoring any packing within the adsorber) of 0.2 m/sec the necessary vessel cross-sectional area is approximately 1 m^2 , corresponding to a diameter of about 1 m.

In principle the depth of adsorbent packing will depend on the kinetics of adsorption and desorption duty. However, the adsorption rates are so rapid and the duty so minimal that the total depth of the packing will depend more on what is done to achieve uniform distribution of flows across the adsorber cross section than on the actual requirements for adsorption.

The bulk density of typical molecular sieves is 41 lb/ft^3 for 1/8 inch spheres or $41 \frac{\text{lb}}{\text{ft}^3} \times 0.0160 \frac{\text{g}}{\text{cm}^3} \cdot \frac{\text{ft}^3}{\text{lb}} = 0.66 \text{ g/cm}^3$. Thus in an adsorber with a cross-sectional area of 1 m^2 the mass of adsorbent per cm depth of packing is $0.66 \times 10^4 = 6600 \text{ g}$. If the adsorbers are operated on a daily cycle, picking up 120 g water at loadings operating between 0.5 and 0.2 g/100 g adsorbent, the effective bed depth required is

$$\frac{120 \times 100}{(0.50 - 0.20) 6600} = 6.1 \text{ cm}$$

In order to ensure that there are no channeling effects in this shallow bed an inert packing would be placed atop the adsorbent to an additional depth of say 10 cm. The overall height of the adsorber would be roughly the same as the diameter, 1 m.

Adsorber Operation

There is obviously a considerable flexibility in the manner of operating the adsorbers depending on what temperatures are chosen for adsorption and for regeneration. The optimum cycles and conditions will depend on the actual economics. A constraint on the regeneration step is that temperatures in excess of the 260°C suggested in the illustration above lead to increasing possibilities for permeation losses. From the standpoint of the stability of the adsorbent, 320°C is a safe regeneration temperature.

3. Purification System

Because of uncertainties as to the nature and amount of impurities which will condense out with the water in the cyclic condensers it is not practicable to attempt a detailed description of the purification system which lies before the electrolysis cells. At the very least it might consist of a small laboratory still, and at the most a more elaborate chromatographic separator. In any case the equipment will be quite small, having to process a daily accumulation of only 10 or so g mole water.

4. Electrolysis Cell

Since this unit is also quite small, we shall not describe it in detail. According to Mantell¹⁷ a laboratory scale Levin cell occupying less than three cubic feet will electrolyze over 5.0 g mole water per hour. We may expect that this unit can be custom designed to operate leak-tight with a minimum inventory of water.

5. Traps

The only other major equipment in the coolant helium processing system are the three traps, the primary one before the vacuum pump on the offstream adsorber and the other two on the gas exit lines on the electrolysis cell.

All three traps must remove water to vanishingly small concentrations. Since the condensing duty in each case is minimal, an economically reasonable coolant would be liquid nitrogen, which at its normal boiling point of 77.4 K permits reducing the vapor pressure of water in the gas effluent to well below 10^{-20} atm, in principle at least.

There should be no problem in obtaining this low water pressure in the traps on the electrolysis cell because the concentration of water in the gases entering the traps is high enough (0.023 atm at 20°C) to ensure nucleation of frost crystals on the cold surfaces of the traps. In the primary traps, however, the very low water vapor pressure at the outset may necessitate the introduction of a small amount of seed water to initiate the frost deposition.

We shall not elaborate the design of the traps. Bailey¹⁸ has analyzed the problem of rising pressure drop across the trap as the frost builds up in the flow

passages for the nitrogen-helium system. This analysis is applicable here, but it is likely to be irrelevant because the small duty on each trap will permit a substantial oversizing of the equipment. Presumably we could adapt to this service compact, low-temperature, heat exchangers characterized by extended surfaces for heat transport. Plate-fin exchangers, for example, are available having surface areas as large as 450 ft² per ft³ exchanger volume.¹⁹

Since each trap operates in parts of the system that are inherently intermittent, only one is needed at each point.

Energy Requirements for Coolant Helium Processing System

The principal energy requirements are those for moving the gas through the adsorber and for cooling and heating the traps.

The pumping duty was shown above to be 14 kW, which at an overall pumping efficiency of 70% would be 20 kW.

If we make the arbitrary and conservative assumption that the effective thermal capacity of each trap is 100 k cal/°C, and if we assume further that each trap operates on a daily cycle between 20°C and -190°C, the cooling duty is equivalent to

$$\frac{300 \text{ kcal}}{\text{day}^\circ\text{C}} \cdot (20 - (-190))^\circ\text{C} \cdot \frac{\text{day}}{24 \text{ hr}} = 2630 \text{ kcal/hr}.$$

If this cooling is provided by evaporating liquid nitrogen at its normal boiling point, where the latent heat of vaporization is 85.3 Btu/lb,²⁰ the amount of nitrogen to be evaporated is

$$2630 \frac{\text{kcal}}{\text{hr}} \cdot \frac{1.8 \text{ Btu g}}{\text{cal lb}} \cdot \frac{\text{lb}}{8.53 \text{ Btu}} = 55.6 \frac{\text{kg N}_2}{\text{hr}}.$$

According to Dodge²¹ the reversible work of liquefaction for air is 2.56 kW hr/lb mole beginning at room temperature, and the thermodynamic efficiency is of the order of 20 percent. The energy requirement for the trap cooling duty therefore is approximately

$$\frac{55.6 \text{ kg}}{\text{hr}} \cdot \frac{\text{kg mole}}{28 \text{ kg}} \cdot \frac{2.205 \text{ lb}}{\text{kg}} \cdot \frac{2.56 \text{ kW hr}}{0.20 \text{ lb mole}} = 56 \text{ kW}.$$

Thus the total energy requirement for the processing system probably will not exceed 100 kW.

Equipment Cost

The costs of the major equipment items estimated either arbitrarily or from the correlations in Chilton²² are tabulated below.

Compressor (30 hp)	\$ 6,000
Adsorbers (2) (40 in. diam, 3 ft tall) stainless steel	4,000
Traps (\$10/ft ² surface)	8,000
Electrolysis Cell	10,000
Total	<hr/> \$28,000

At a Lang factor of 5.0 the total installed cost of this equipment and its accessories will be \$140,000.

Breeder Salt Loop

Choice of Breeder

The choice of the tritium breeding medium is severely limited by the requirements for stability toward neutron irradiation and high temperatures, for high lithium density, and for relative inertness toward confining structures. Eutectic or near-eutectic mixtures of lithium and beryllium fluoride meet these requirements without having to resort to refractory metals or other unproved technology, and they offer the additional advantage of permitting ready recovery of the tritium generation. In this last regard they are likely to be far superior to liquid lithium but inferior to molten lithium nitrate and nitrite. These latter salts, however, are marginal in thermal and radiation stability and in lithium density.²³

Choice of Processing System

We assume that the tritium born out of the capture of neutrons by lithium (Li-6 and Li-7) will combine with the simultaneously liberated fluorine to form the stable tritium fluoride, which together with the helium ash generation will be dissolved in the breeder salt. To the extent that any free tritium is present, either from inadequate opportunity to combine with fluorine or from the result of chemical interaction between TF and metal confining walls, there will be permeation of tritium through metal walls and ultimately into the coolant helium system. In principle such permeation is advantageous because of the sure and easy recovery of tritium from the helium as described above. In practice, however, an appreciable permeation could occur

only at the expense of chemical attack on the metal walls, and although the corrosive rates would be low relative to the very large metal surfaces, it would probably be insuperably difficult to devise a means of avoiding localized attack and subsequent perforation.

A potential difficulty for the processing system is that appreciable amounts of oxygen are produced by n, α reactions on fluorine. Price²⁴ has estimated that about 480 g/day oxygen will be formed, principally O-16, and something of the order of 5 g protium. We shall assume that the enormous mass of LiF and BeF₂ will effectively tie up the oxygen as oxides of lithium and beryllium and preclude any formation of T₂O. The protium will form HF and be processed with the TF.

The processing system involves three steps: first the separation of the TF from the salt at very low concentrations, second the concentration of the tritium in form for regeneration, and finally the regeneration of pure tritium.

Fortunately, as shown below, TF is relatively insoluble in the breeder salt, and hence it can be separated from the salt readily by outgassing a chamber through which the salt is circulated in a form offering a large surface area between the melt and the gas phase. The concentration of the tritium thus vaporized can be accomplished either by condensation in a cold trap or by chemical absorption, for example by potassium fluoride, which at room temperature will take up all the TF as KTF₂.²⁵ Physical adsorption does not seem to be a practicable alternative because of the high chemical reactivity of the TF with all the usual adsorbents.

Chemical absorption on KF is disadvantageous because the only simple regeneration procedure is to electrolyze a solution of TF and KF, and consequently the TF would have to be reevaporated from the acid fluoride and subsequently condensed and transferred to the electrolysis cell or else the whole bed of KF and KTF₂ would have to be so transferred.

An alternative possibility for the concentration and regeneration steps would be to take advantage of the reactivity of TF toward metal oxides. The hot gases after detachment from the breeder salt would pass over a solid bed of oxides forming the metal fluoride and water. The water would be trapped out and subsequently electrolyzed to oxygen and tritium as in the coolant helium processing system. Two disadvantages of this procedure are that the oxide bed would have to be renewed from time to time, and an extra piece of apparatus would be necessary. The principal advantage is that water is a more manageable chemical than TF.

For reasons of simplicity we shall choose the processing sequence in which the TF is concentrated by condensation in cyclic cold traps. This system is described

briefly in the next section followed by a more detailed description of the major items of equipment.

Summary of Processing Scheme

The principal features of the tritium processing system on the salt loop are shown in Fig. 14.11, and the flows and concentrations are summarized in Table 14.4.

Breeder salt is recirculated continuously through eight disengagers arranged in parallel around the periphery of the reactor. The salt enters the disengagers through spray nozzles, which produce a shower of tiny droplets presenting an extended surface area to ensure that the dissolved He and TF will diffuse from the liquid salt into the vapor phase. The droplets collect in the bottom of the disengager and flow back through the reactor blanket. A small drag stream, most probably intermittent, is drawn from the salt flow leaving the disengager and passed through a doctor system where impurities are removed and any necessary makeup materials are added to maintain desired purity and composition of the salt.

The vapor effluent from each disengager is drawn first through a water-cooled salt trap and then through cyclic condensers cooled with liquid nitrogen. The non-condensable gases are discharged to the helium storage system for final control.

The solid TF which had accumulated in the offstream cyclic condenser is melted out and collected in a small receiver, from which it flows intermittently through a purification system and then into an electrolysis cell, where a solution of KF in TF is electrolyzed to F_2 and T_2 . These gases emerge from their electrodes saturated with TF, and hence they are passed through traps to remove the TF. These traps consist of small vessels cooled with liquid nitrogen, at which temperatures essentially all TF will be condensed out. The pure tritium gas is drawn into the primary fuel loop, and the pure fluorine is disposed of in an economical manner.

Principal Components

1. Disengagers

The eight disengagers are little more than large, vertical, cylindrical vessels each equipped with spray nozzles near the top for the salt entry, baffles at the top to prevent salt entrainment into the vapor discharge vent at the very top, and the salt discharge line at the bottom. In the absence of experimental data on the droplet forming characteristics of the salt we can gauge roughly the likely size of each disengager from the required salt flow rates and corresponding vapor flow rates.

744450

Fig. 14.11 Salt processing system

For a fixed TF recovery rate the salt flow rate is inversely proportional to the concentration of TF at which the TF is recovered from the salt. A material balance on TF at the disengager for very dilute solutions of TF in the salt is given by

$$N_{\text{salt}}(x_i - x_o) = N_{\text{TF}} \quad (14.8)$$

where N_{salt} = kg mole salt/hr flowing through disengager

x_i = mole fraction TF in entering salt

x_o = mole fraction TF in exit salt

N_{TF} = kg mole TF vaporized from salt, which at steady state is the net production of TF in the blanket.

From the standpoint of ease of recovery of the TF it would be desirable to operate at high x , but from the standpoint of tritium inventory the reverse would be true. An overriding constraint however is the tendency for TF to react with metal walls releasing molecular tritium according to equations like

where M represents the metal.

The T_2 tends to dissolve in the metal wall, and if this tendency is excessive because of high TF concentrations, the wall will become embrittled. Maroni²⁶ has estimated that the maximum tolerable T_2 pressure in the salt to avoid wall embrittlement is 10^{-2} torr, i.e. 1.32×10^{-5} atm. The corresponding maximum TF pressure can be estimated from free energy data for the above reaction (Eq. 14.9) using a reasonably representative metal and making some assumption as to the concentration of metal fluoride dissolved in the salt. Grimes and Cantor²⁷ have made this kind of analysis for nickel with the assumption that the nickel is dissolved as fluoride in the salt to the extent of 6 ppm nickel. Their free energy data yield the following equation for equilibrium compositions at 1000 K:

$$K_{1000^\circ} = 4 \times 10^3 = \frac{x_{\text{NiF}_2}^{1/2} p_{\text{H}_2}^{1/2}}{a_{\text{Ni}}^{1/2} p_{\text{HF}}} \quad (14.10)$$

where x_{NiF_2} is mole fraction NiF_2 in salt, 3.2×10^{-6}

a_{Ni} = activity of nickel metal in the wall=1.0

p = respective vapor pressures, atm.

If we make the reasonable assumption that this equation applies to TF, the allowable TF pressure corresponding to a T_2 pressure of 1.32×10^{-5} atm and the same concentration of nickel fluoride in the salt is 1.62×10^{-3} atm. Thus the TF pressure of the salt entering the disengager is 1.62×10^{-3} atm, and if conditions are maintained in the disengager such that the salt leaving it has a TF pressure of, say, 10^{-4} atm, these data together with phase equilibrium information and Eq. 14.8 permit our calculating the required salt rate.

Field and Shaffer²⁸ have measured the solubility of HF and DF in $2 \text{ LiF} \cdot \text{BeF}_2$, and their data together with estimated values for TF are presented in Table 14.5 as Henry's Law constants K_H in the equation

$$pK_H = x \quad (14.11)$$

where p = equilibrium vapor pressure of TF, atm

x = mole fraction TF in the salt.

Table 14.5

Solubilities of Hydrogen Fluorides in $2 \text{ LiF} \cdot \text{BeF}_2$.

Temperature °C	$K \times 10^4$ HF	$K \times 10^4$ DF	$K \times 10^4$ TF (extrapolated)
500	3.37 ± 0.13	2.96 ± 0.07	2.55
600	2.16 ± 0.05	1.83 ± 0.03	1.50
700	1.51 ± 0.06	1.25 ± 0.03	0.99

Thus at 660°C $K_{\text{TF}} = 1.29 \times 10^{-4}$, hence

$$x_i = 1.29 \times 10^{-4} \times 1.62 \times 10^{-3} = 2.09 \times 10^{-7} \text{ mole fraction and}$$

$$x_o = 1.29 \times 10^{-4} \times 10^{-4} = 1.29 \times 10^{-8} \text{ mole fraction.}$$

The tritium generation rate in the blanket is 548 g/day or $548/(3.017)(24) = 7.57$ g atom T/hr, and if we assume all of this is removed as TF, N_{TF} in Eq. 14.8 is 7.57×10^{-3} kg mole TF/hr. Hence the minimum salt rate through the disengagers must be

$$\begin{aligned} N_{\text{salt}} &= (2.09 \times 10^{-7} - 1.29 \times 10^{-8})^{-1} \times (7.57) \times 10^{-3} \\ &= 3.86 \times 10^4 \text{ kg mole salt/hr.} \end{aligned}$$

or for an average molecular weight of 36.9 for 48:52 mole ratio LiF: BeF₂ the minimum mass rate is $(36.9) 3.86 \times 10^4 = 1.42 \times 10^6$ kg/hr. To provide a reasonable design margin we shall set the salt rate at 4×10^6 kg/hr.

At a density of 2.27 g/cm^3 ²⁹ the corresponding volumetric rate then is

$$4.0 \times 10^6 \frac{\text{kg}}{\text{hr}} \cdot \frac{\text{hr}}{3600 \text{ sec}} \cdot \frac{\text{cm}^3}{2.27 \text{ g}} \cdot \frac{10^3 \text{ g}}{\text{kg}} \cdot \left(\frac{\text{m}}{10^2 \text{ cm}} \right)^3 = 0.489 \text{ m}^3/\text{sec}.$$

The volumetric throughput in each of the eight disengagers then would be $0.0612 \text{ m}^3/\text{sec}$, which flow rate would be readily accommodated in a cylinder 1 m in diameter.

The vapors leaving the disengagers comprise the TF, very small amounts of HF, the helium ash from the blanket breeding reactions along with any helium produced from n, α reactions in the blanket, and vaporized salt. The helium ash production is 7.57 g atom He per hr and the n, α reactions bring the total up to something like 11.6 g atom He/hr.

Cantor, Hsu and Ward³⁰ have measured the vapor pressures of various compositions of lithium fluoride-beryllium fluoride mixtures over wide ranges of temperatures. For melts with a molar ratio of 48:52, Davison's interpolation of the data³¹ converted to pressure units in atmospheres results in the equation

$$\log P_{\text{atm}} = 6.434 - 9,963/T \quad (14.12)$$

where T is absolute temperature K. At 660°C (933 K) the vapor pressure of the salt is 1.3×10^{-4} atm. If 7.57 g mole TF are pumped off the disengagers at a TF pressure of 10^{-4} atm, the accompanying salt vapor flow rate must be $(7.57) (1.3 \times 10^{-4}) / (10^{-4})$ or 9.84 g mole salt per hour. Thus the total gas rate is of the order of $7.57 + 11.6 + 9.84$ or 29.0 mole/hr.

In the vapor phase the tritium fluoride is an equilibrium mixture of TF and T₆F₆ according to the reaction

Simon³² gives the temperature dependence for this equilibrium for DF as

$$\log K = (8970/T) - 43.65 \quad (14.14)$$

from which it can be seen that at the temperatures in the disengagers the equilibrium lies far to the right and all the TF is in the form of TF.

If the pressure of the TF in the vapor phase is 10^{-4} atm, the pressure exerted by the helium must be $10^{-4} (11.6/7.57)$ or 1.53×10^{-4} atm, and the total pressure, assuming these and the salt are the only species present, must be the sum $(1.0 + 1.53 + 1.3) \times 10^{-4}$ or 3.83×10^{-4} atm. At 660°C (933 K) the volumetric gas rate is

$$\frac{0.0224 \text{ m}^3}{\text{g.mole}} \cdot 29.6 \frac{\text{g mole}}{\text{hr}} \cdot \frac{\text{hr}}{3600 \text{ sec}} \cdot \frac{1}{3.83 \times 10^{-4}} \cdot \frac{933}{273}$$

= 1.61 m³/sec, or for each of the disengagers 0.201 m³/sec. This rate can be accommodated without entrainment in a vessel 1 m in diameter, hence it is likely that 1 m in diameter by perhaps 3 m in height will be reasonable dimensions for the disengagers.

Because of the high temperatures in the disengagers there can be permeation losses of tritium through the disengager walls. These losses will be mitigated to the extent that interactions between TF and the walls can be minimized. The losses will be controlled absolutely by secondary containment, but, since the only hot regions in the salt processing system (hence the only regions where permeation may be a problem) are the disengagers and the lines connecting the disengagers to the blanket, it may be economic to provide a local secondary containment in these regions. A relatively thin, cool metal sheath outside the insulation should suffice.

2. Cooler and Salt Trap

The size of the cooler atop the disengager is dictated as much by the need to pass the attenuated gas volume at very low pressure drop as it is to accomplish the heat transfer. The functions of the cooler are to prevent any further passage of salt vapor and of salt droplets which might escape the baffles in the disengager and to drop the temperature of the gas quickly so that the cooling load on the liquid nitrogen traps is minimized, and any opportunities for tritium permeation of metal walls after the disengagers are eliminated.

We shall not attempt a design of this apparatus. On the basis of the cooling requirements alone it will be quite small since a rough estimate using the most conservative assumptions indicates that a surface of less than 0.1 m² would easily accommodate the heat transfer duty for all disengager exhausts. It is possible that some provision for trapping salt mist might be necessary, but that problem will require experimental study as part of the ultimate program in optimizing the design of the disengagers.

3. Cyclic Condensers

The cool gas leaving the salt trap contains only helium and tritium fluoride with small amounts of hydrogen fluoride. It is drawn through an onstream cyclic condenser and cooled to liquid nitrogen temperatures 77.4 K.

Simon³³ presents equations for the vapor pressures of HF and DF as functions of temperature. Conversion of these equations into pressures in atmospheres and linear extrapolation of their constants lead to the following equation for TF:

$$\log p_{TF} = 4.1505 - \frac{1205.53}{T} \quad (14.15)$$

where p_{TF} is the equilibrium vapor pressure of TF in atmospheres and T is K. According to this equation the vapor pressure of TF at 77.4 K is 3.76×10^{-12} atm, which assures an adequate control of tritium losses in the helium ash exhaust.

Since the actual mass of tritium fluoride being condensed is small and the amount of gas being cooled is small, the principal component of the heat exchange duty in the cyclic condensers is the periodic cooling and heating of the mass of metal comprising the condensers. It is premature at this stage to attempt to determine the optimum deployment of condensers, i.e., whether one pair of condensers should serve all eight disengagers, or one pair for each disengager, or some intermediate combination. Few condensers means larger vacuum piping, larger valves and larger vacuum pumps. We shall assume that the advantages of having small, multiple units will make it desirable to provide each disengager with its own pair of cyclic condensers.

The gas flow rate leaving the salt traps at 10^{-4} atm and 100°C is

$$19.2 \frac{\text{g mole}}{\text{hr}} \cdot \frac{0.0224 \text{ m}^3}{\text{g mole}} \cdot \frac{\text{hr}}{3600 \text{ sec}} \cdot \frac{1}{10^{-4}} \cdot \frac{373}{273} = 1.63 \frac{\text{m}^3}{\text{sec}}$$

or $0.204 \text{ m}^3/\text{sec}$ for each unit. At flow velocities not in excess of 1 m/sec to avoid excessive pressure losses, the corresponding duct diameter must not be less than 0.5 m.

We may estimate that each cyclic condenser at this scale of piping size may be constructed reasonably with a total thermal capacity of $60 \text{ k cal}/^{\circ}\text{C}$ (equivalent to 500 kg steel). With eight condensers onstream on staggered 24-hr periods there would be one condenser melting out every three hours.

If we assume that the energy loss during a cooling cycle equals the cooling duty and that the cooling duty on the liquid nitrogen system is equivalent to dropping the condenser temperature from the melting point of TF (20°C) to 78 K, the total duty per condenser is

$$2.0 \cdot 60 \frac{\text{k cal}}{^{\circ}\text{C}} \cdot \frac{(20 + 273 - 78)^{\circ}\text{C}}{24 \text{ hr}} = 1075 \text{ k cal/hr}$$

In the section on the coolant helium processing system it was shown that the power requirement for a liquid nitrogen cooling system was 56 kW for 2630 k cal/hr. Hence for the TF cyclic condenser the power requirement is

$$56 \cdot \frac{1075}{2630} = 22.9 \text{ kW, and for all eight units it is } 183 \text{ kW.}$$

The corresponding ohmic heating requirement for the melting out period is for the same allowance of losses

$$1075 \frac{\text{k cal}}{\text{hr}} \cdot 8 \text{ traps} \cdot \frac{10^3 \text{ cal}}{\text{k cal}} \cdot \frac{\text{hr}}{3600 \text{ sec}} \cdot 4.186 \frac{\text{joule}}{\text{cal}} \frac{10^{-3} \text{ kWsec}}{\text{joule}} = 10.0 \text{ kW}$$

for all eight units.

4. Receiver

Liquid tritium fluoride from the melting-out periods flows by gravity into a small receiver. Again the question of whether there should be one receiver for each disengager or fewer receivers will depend on a systematic economic appraisal. From the standpoint of maintaining small tritium inventories wherever possible it would be advantageous to have each disengager unit self-sufficient down to the final point of returning pure tritium to the primary fuel system. A liter vessel of appropriate metal alloy at each disengager will easily accommodate the TF accumulation of

$$7.57 \frac{\text{g mole TF}}{\text{hr}} \cdot \frac{24 \text{ hr}}{8 \text{ traps}} \cdot \frac{22.02 \text{ g TF}}{\text{g mole TF}} = 500.1 \text{ g}$$

The density of liquid HF is given by Simon³⁴ as

$$d = 1.0020 - 0.0022625t + 0.000003125t^2 \quad (14.16)$$

for density in g/cm³ at t°C.

At 20°C, $d = 1.002 - 0.452 + 0.00125 = 0.958 \text{ g/cm}^3$, and hence the accumulated liquid per cycle is $500.1 \text{ g}/0.958 \text{ g/cm}^3$ or 522 cm^3 .

5. Electrolysis Cells

Tritium fluoride cannot be electrolyzed directly to produce pure tritium, but in solution with potassium fluoride the electrolysis goes readily at moderate temperatures to yield tritium at one electrode and fluorine at the other. Both gases emerge saturated with TF, hence small nitrogen cooled traps on each line will be necessary to recover the vaporized TF.

The cells will have to be custom designed to ensure high productivity at low melt inventories. It is likely that they will be operated continuously or nearly so, which means that they will be relatively small units. Even so, we may expect that the capital costs for each of the eight cells together with their traps might run to \$20,000.

The effluent fluorine, free of TF, would be collected in a storage vessel for final control and disposition. If there is any appreciable amount of free molecular tritium in the gases drawn from the disengagers, it will be necessary to add some of the effluent fluorine to those gases and pass them across an appropriate catalyst to burn the free tritium to TF to ensure recovering all the tritium in the cyclic condensers.

The effluent tritium product from the electrolysis cells is drawn into the primary fuel processing system. It may be desirable to interpose between the trap in the effluent line and the primary fuel system a small tower containing KF as a nearly absolute barrier to contamination of the primary fuel system with TF.

Over time the melts in the electrolysis cells will have to be processed in some fashion to prevent excessive buildup of impurities.

6. Vacuum Pumps

The volumetric gas flow rate at the entry to each of the eight vacuum pumps for 11.6 g mole He/hr at 0°C and 10^{-4} atm is

$$11.6 \frac{\text{g mole}}{\text{hr}} \cdot \frac{22.4 \ell}{\text{g mole}} \cdot \frac{\text{hr}}{60 \text{ min}} \cdot \frac{1}{8 \text{ pumps}} \cdot \frac{1}{10^{-4}} \cdot \frac{273}{273} = 5420 \ell/\text{min}.$$

Commercial scale rotary vane vacuum pumps costing roughly \$5000 each can handle this flow.

7. Salt pumps

The pumping power to circulate the breeder salt through the disengagers is given by

$$w = (p_i - p_o) m/\rho \quad (14.17)$$

where the bracketed expression is the pressure drop across the pump, m is the mass flow rate of the salt, and ρ is the density of the salt. But m/ρ is merely the volumetric

flow rate, which has been shown to be $0.489 \text{ m}^3/\text{sec}$, whence for a pressure drop of 2 atm (based on the restriction that the salt pressure never exceed 2 atm and the fact that the disengagers are at essentially 0 atm pressure)

$$w = 2.0 \text{ atm} \cdot 1.0132 \times \frac{10^6 \text{ dyne}}{\text{cm}^2 \text{ atm}} \cdot \frac{0.489 \text{ m}^3}{\text{sec}} \cdot \frac{(10^2 \text{ cm})^3}{\text{m}^3} \cdot \frac{\text{W sec}}{10^7 \text{ dyne cm}} = 99,100 \text{ W or } 99.1 \text{ kW}$$

Given the relatively modest duty for each pump on a disengager unit it might be advantageous to make use of sump pumps, which would simplify somewhat the sealing and lubrication problems.

At 70% overall pump efficiency the pump horsepower requirement for each disengager unit is

$$\frac{99.1 \text{ kW}}{8 \text{ pumps}} \cdot \frac{1.341 \text{ hp}}{\text{kW}} \cdot \frac{1}{0.70} = 23.7 \text{ hp}$$

According to Chilton³⁵ an alloy steel pump of this capacity will cost \$3,000 at ENR Index of 400. For a scaling factor to 1973 dollars of 2.4 the cost is \$7,200.

8. Salt Doctor

It is not at all clear how large and elaborate the salt doctor will have to be. At the very least it will include a small filter for preventing buildup of solids in the breeder salt. It may also have to include small chemical reactors and possibly electrolytic cells to effect desirable adjustments of melt compositions. For lack of better information we shall assume that the capital cost of this system may be of the order of \$100,000.

Summary of Energy Requirements

The principal energy consumption for the salt processing system occurs in the production of liquid nitrogen for cooling the cyclic condensers and cold traps. For the former the power requirement has been shown to be 183 kW, and including the latter the total for the nitrogen system might be 200 kW.

The salt pumping power requirement at 70% efficiency is $99.1/0.70 = 142 \text{ kW}$. If we assume all other power requirements for the salt system will round this value up to 200 kW, the total energy consumption is of the order of 400 kW.

Total Cost of Salt Processing System

The likely costs of the major equipment requirements for the salt processing system, either arrived at as described above or estimated arbitrarily, are as follows: for each disengager unit:

disengager	\$10,000.
salt pump	7,000.
cooler	2,000.
cyclic condensers (two)	6,000.
vacuum pump	5,000.
electrolysis cell, receiver and traps (two)	20,000.

Total per unit	\$50,000.
----------------	-----------

Thus the total major equipment cost will be:

eight units	400,000.
salt doctor	100,000.
liquid nitrogen system	60,000.

Total equipment cost	\$560,000.
----------------------	------------

At a Lang factor of 5.0 the total capital cost for the fuel processing system on the breeder salt loop including the liquid N₂ system is

$$5.0 (560,000) = \$2,800,000.$$

Total Energy Requirements for Fuel Handling

The total energy requirements for the fuel handling system exclusive of the fuel injection step and the vacuum pumps on the divertors are:

Primary Fuel Loop	200 kW
Coolant Helium Loop	100
Breeder Salt Loop	400

Total	700 kW.
-------	---------

Total Capital Cost for Fuel Handling

The total capital cost for all elements of the fuel handling system exclusive of the fuel injectors and the vacuum pumps is:

Primary Fuel Loop	\$ 215,000.
Coolant Helium Loop	140,000.
Breeder Salt Loop	2,800,000.
<hr/>	
Total	\$3,155,000.

This figure includes all instrumentation, piping, ancillary equipment, engineering fees, contractor fees etc.

Areas of Principal Uncertainty

Uncertainties in Primary Fuel Loop

The principal uncertainties in the primary fuel loop have to do with the injection of the fuel into the reactor and the control of the reactions within the reactor. These problems relate immediately to the basic problems of plasma physics and are treated elsewhere.

If we exclude from our consideration these problems and also the plasma physics problems associated with the initial cooling of the reactor exhaust in the divertors, the principal uncertainty in the primary fuel loop is the extent to which deuterium and tritium will be lost from the loop by dissolution in the walls of the divertors and subsequent permeation through the walls into the coolant helium system. Higher losses here will increase the load on the tritium recovery system on the coolant helium loop with attendant increases in capital and operating costs.

Other uncertainties are likely to have minor consequences. For example, to what extent will high-Z elements enter the fuel loop and will tritium exchange with forepump and compressor lubricants, and what complications will the accommodation of these problems entail?

Uncertainties in Coolant Helium Loop

Aside from the uncertainty as to the amounts of hydrogenic isotopes that will enter the helium system, the principal question here is how surely the hydrogen can be bound up as water. What are the mechanisms of hydrogen oxidation at the metal - gas

interface or metal oxide - metal interface, and what are the rates associated with these mechanisms? Must catalysts be employed to ensure equilibrium binding of the hydrogen as water?

What impurities will tend to build up in the coolant helium, and how can they be controlled without subverting the tritium recovery? Are there possibilities for tritium exchange and hence losses or inventory buildups in amounts that could be objectionable?

Uncertainties in Breeder Salt System

Because of the chemical complexity of the breeder salt there are many uncertainties. What species will build up in the salt loop; what are their volatilities and hence the implications for processing the gases withdrawn from the salt? How do these species interact with metal walls, and what are the requirements for preserving the integrity of the walls? In particular, how does TF at very low concentrations attack metal walls, and does the attack favor dissolution of tritium in the walls and subsequent permeation? How can small amounts of dissolved gases be disengaged from large amounts of salt melt? How much doctoring of the breeder salt will be required to maintain desirable properties, and what kinds of processing will comprise the doctoring?

Can the electrolysis of TF be effected reliably in small-dimensioned cells with foolproof, automatic operation?

Implications for Future Research Programs

On the basis of the foregoing listing of uncertainties we may conclude that the major confusions are related to the likely transports and distributions of tritium throughout all elements of the fuel handling system. Hence a considerable research effort should be focussed on improving our understanding of what occurs at metal surfaces where tritium is present at very low concentrations and in forms likely to lead to solution in the metal or conversely where tritium permeates from the metal lattice into the surface of the metal. We also need quantitative information on the transport processes involving tritium and tritium bearing species at very low concentrations in molten salts and in attenuated gases in condensation processes, for example.

Many of our uncertainties can be dispelled only in large scale development programs involving loops subjected to conditions approaching those of the prototype reactor. Nevertheless, well-designed bench-scale studies can provide a sound data base for the development programs.

References

- ¹T. M. Flynn, K. D. Timmerhaus, D. H. Weitzel, J. W. Draper, Separation of Hydrogen Isotopes by Multicomponent Distillation, Advances in Cryogenic Engineering, K. D. Timmerhaus, ed. (Plenum Press, Inc., New York) Vol. 3, pp. 58-63.
- ²L. J. Anastasia and V. A. Maroni, Fuel-Ash Removal and Reprocessing System, Argonne National Laboratory, (1973), unpublished report.
- ³E. H. Kobisk, private communication, 1972.
- ⁴Chemical Engineers' Handbook, J. H. Perry, ed., 3rd edition (McGraw-Hill Book Co., New York, 1950) p. 150.
- ⁵C. S. Robinson and E. R. Gilliland, Elements of Fractional Distillation, 4th edition, (McGraw-Hill Book Co., New York, 1950) p. 175.
- ⁶Handbook of Chemistry and Physics, R. C. Weast, ed., 48th edition (Chemical Rubber Publishing Co., Cleveland, Ohio, 1967).
- ⁷Chemical Engineers' Handbook, op. cit., p. 210.
- ⁸H. M. Mittelhauser and G. Thodos, Vapour Pressure Relationships up to the Critical Point of Hydrogen, Deuterium, and Tritium, and their Diatomic Combinations, Cryogenics 4, 368 (1964).
- ⁹K. Denbigh, The Principles of Chemical Equilibrium, 2nd edition (Cambridge University Press, Cambridge, England, 1968) p. 266.
- ¹⁰Chemical Engineers' Handbook, op. cit. p. 210.
- ¹¹R. Barron, Cryogenic Systems, (McGraw-Hill Book Co., New York, 1966).
- ¹²Chemical Engineers' Handbook, op. cit., pp. 266-267.

- ¹³E. F. Johnson, Process Economy, Princeton University Department of Chemical Engineering, 1971, unpublished.
- ¹⁴Lubrication and Lubricants, E. R. Braithwaite, ed. (Elsevier Publishing Co., New York, 1967) pp. 195-196.
- ¹⁵C. K. Herish, Molecular Sieves (Reinhold Publishing Corp., New York, 1961).
- ¹⁶M. N. Y. Lee, Novel Separation with Molecular Sieves Adsorption, Recent Developments in Separation Science, N. N. Li, ed. (Chemical Rubber Publishing Co., Cleveland, Ohio, 1972) Vol. 1, pp. 75-112.
- ¹⁷C. L. Mantell, Ref. 4. Section on Electrochemistry, p. 1796.
- ¹⁸B. M. Bailey, Freeze-out Purification of Gases in Heat Exchangers, Advances in Cryogenic Engineering, K. D. Timmerhaus, ed. (Plenum Press, Inc., New York, 1960) Vol. 2, pp. 45-53.
- ¹⁹R. Barron, op. cit., p. 134.
- ²⁰ibid., p. 634.
- ²¹B. F. Dodge, Ref. 4, Section on Low Temperature Processing, p. 1703.
- ²²C. H. Chilton, Cost Engineering in the Process Industries (McGraw-Hill Book Co., New York, 1960).
- ²³A. K. K. Lee and E. F. Johnson, Thermal Decomposition of Lithium Nitrite, Inorg. Chem. 11, 782 (1972).
- ²⁴W. G. Price, private communication, 1973.
- ²⁵E. F. Johnson, Fusion Reactor Fuel Processing, The Chemistry of Fusion Technology (Plenum Press, Inc., New York, 1973) D. M. Gruen, ed., pp. 191-214.

²⁶V. A. Maroni, Chapter 15, this report.

²⁷W. R. Grimes and S. Cantor, Molten Salts as Blanket Fluids, The Chemistry of Fusion Technology (Plenum Press, Inc., New York, 1973) D. M. Gruen, ed., pp. 161-190.

²⁸P. E. Field and J. H. Shaffer, The Solubilities of HF and DF in Molten Fluorides, J. Phys. Chem. 71, 3218 (1967).

²⁹W. R. Grimes, Molten Salt Reactor Chemistry, Nucl. Appl. Tech. 6, 137 (1970).

³⁰S. Cantor, D. S. Hsu, and W. T. Ward, Oak Ridge National Laboratory Report ORNL-3913 (1965) pp. 24-26.

³¹P. W. Davison, private communication, 1973.

³²Fluorine Chemistry, J. H. Simon, ed. (Academic Press, New York, 1950) Vol. 1, pp. 228-229.

³³ibid., pp. 228-229.

³⁴ibid., p. 228.

³⁵C. H. Chilton, op. cit., p. 50.

Chapter 15

Tritium Distribution and Leakage

Victor A. Maroni*

Introduction

In this chapter an attempt is made to evaluate the distribution and leakage profiles for tritium in the reference design model. These profiles are determined by the technical features set forth in previous chapters, and, as we shall show, have an important bearing on the operational characteristics of the reference model. The results presented in this chapter make a substantial contribution to the evaluations of the system features (hazards, environmental impact, operational procedures, maintenance, and cost analysis), which follow in Part III.

A recent assessment of the radiological impact of tritium releases from fusion power plants by Steiner and Fraas¹ indicated that a daily release of ~ 3 Curies/1000MW(t) through steam system effluents might be acceptable based on existing AEC guidelines for light-water fission reactors. (These guidelines require that the dose rate at the plant perimeter be ≤ 5 mrem/yr, and that the average tritium levels in the liquid effluents discharged to natural waterways be $\leq 5 \times 10^{-3}$ Ci/liter.) Prototypical fusion power plants will probably require tritium inventories in excess of 10^7 Curies (> 1 kg), hence the release levels applied in the study by Steiner and Fraas correspond to daily tritium loss rates of less than 1 ppm of the total inventory. Based on anticipated standards for radioactivity releases from nuclear plants it is likely that an annual loss rate of tritium in excess of the above value may begin to constitute an objectionable radiological hazard. In the absence of reasonable guidelines other than those used by Steiner and Fraas, the reference model has been designed so that tritium releases via the steam system are less than 3 Ci/day/1000MW(t).

In addition to the environmental considerations, there are other potentially significant consequences of the tritium in fusion reactors, and they too must be assessed. These include holdup of tritium in the structure, accelerated corrosion and loss of mechanical integrity due to hydriding, and assorted maintenance problems. Some of these problems have been touched on in previous chapters, but in this chapter we shall attempt to deal with them in greater detail, using the calculational model that follows as a point of departure.

The principal objectives with regard to control and handling of tritium will be to minimize the total inventory and loss rate of tritium in every sector of the plant.

* Argonne National Laboratory

For the most part, the minimization of both the tritium inventory and the loss rate will be accomplished by (a) processing the blanket so as to achieve the lowest possible concentration of tritium, (b) employing a contiguous system of barriers (both metal and ceramic) that are impervious to the hydrogen isotopes in any form, and (c) operating a processible gaseous purge flow system around and throughout the plant on a continuous basis. In the final analysis we shall show that the principal loss mechanism for tritium in the reference design model is diffusion (of tritium) through the heat transfer circuits into the steam system.

Important Properties That Affect Tritium Distribution and Leakage

The important properties of the hydrogen isotopes that will determine their distribution and leakage characteristics in currently conceived fusion reactors are their solubilities in the materials with which they come in contact, their diffusion rates through the working fluids and structural components, and the stable chemical forms in which they exist. In this section, we review briefly the formalisms that permit us to express these properties in terms of parameters that we expect to control.

In order to make a reasonable estimate of the tritium held up (dissolved) in the structure and fluids, we must have expressions for the solubility of tritium in the materials of construction. For the case of dilute solutions of gaseous hydrogen (H_2 , D_2 , T_2) in metals, the relationship between solubility and pressure is given by Sieverts' law,^{2,7}

$$C = \sqrt{P} \cdot K_S \quad (15.1)$$

where C is the concentration, P is the H_2 partial pressure, and K_S is the Sieverts' constant. It can further be shown^{3,5} that K_S is exponentially dependent on temperature, hence the general expression relating concentration to H_2 pressure and temperature is given by

$$C = \sqrt{P} \cdot [K_S^0 \cdot \exp(Q_S/kT)] \quad (15.2)$$

in which K_S^0 is a material related constant, Q_S is the activation energy for solution of hydrogen in the metal, k is the Boltzmann factor (on a molar basis), and T is the

absolute temperature. Values of K_S^O and Q_S for materials of interest to the subject model are given in Table 15.1.

The solubilities of the important tritium bearing species in the molten salt blanket fluid (HT, T₂, TF, etc.) are directly related to their partial pressure in accord with Henry's law and can be expressed as follows

$$C = P \cdot K_H, \quad (15.3)$$

where C and P are the concentration and pressure of a given species in the blanket, and K_H is the Henry's⁸ law constant for that species. Since K_H is an exponential function of temperature, the general expression that relates concentration to temperature and pressure is given by Eq. (15.4),

$$C = P \cdot \left[K_H^O \exp (Q_S/KT) \right] \quad (15.4)$$

Malinauskas et al.^{9,10} have measured the solubility of H₂ and D₂ in molten Li₂BeF₄ at pressures in the range of 1.0 to 2.1 atm and for temperatures between 500 and 700°C. Values of K_H^O and Q_S for H₂ and D₂ determined from their data are given in Table 15.2, together with estimated values of these parameters for T solutions in Li₂BeF₄. The solubilities of HF and DF in lithium fluoroberyllates have been determined by Field and Shaffer.¹² Values of K_H^O and Q_S for HF and DF and estimated values of these parameters for TF are also given in Table 15.2. An assumption inherent in the calculations that follow is that the Henry's law constants determined from the data in Table 15.2 are applicable at pressures that are 10⁴ to 10⁶ times smaller than the pressures used by the experimenters.

The rates of migration of H, D, and T through structural interfaces are determined from available permeation data for the materials of construction. (Webb¹³ and Stickney¹⁴ have reviewed the principles of the permeation process for hydrogen isotopes.) The expression that relates permeation to the operating parameters is given by Eq. (15.5),

$$\phi = \frac{A}{X} \cdot \left(\sqrt{P_H} - \sqrt{P_L} \right) B \cdot \exp(-Q_p/kT) \quad (15.5)$$

Table 15.1. Parameters for the Sieverts' Law Constants,
 K_S , of H_2 , D_2 , and T_2 in The Reference Design
 Construction Materials

$$K_S = K_S^O \exp (Q_S/kT)$$

Material	K_S^O (wppm ^a /Torr ^{1/2})			Q_S (cal/mole)			Reference
	H_2	D_2	T_2	H_2	D_2	T_2	
PE-16	0.4	0.7	1.1	-1,680	-1,750	-1,830	(b)
Incoloy 800	0.4	0.7	1.1	-1,680	-1,750	-1,830	(b)
Copper	13.6	27.2 ^c	40.8 ^c	-13,911	-13,911 ^c	-13,911 ^c	5

^a wppm = weight parts per million H, D, or T

^b Calculated by using the data of Ref. 4 for 347-SS and assuming the values of K_S^O and Q for PE-16 and Incoloy 800 would be essentially the same as for 347-SS. (Values of K_S for PE-16 and Incoloy 800 are not expected to be significantly different from those for 347-SS based on previous studies⁶ of the effect of composition on solubility of hydrogen in Fe-Cr-Ni alloys.)

^c Approximated by assuming the same parameters that are given for H_2 in Ref. 5.

Table 15.2. Parameters for the Henry's Law Constants, K_H , of H_2 , D_2 , T_2 , HF, DF, and TF in Li_2BeF_4

$$K_H = K_H^O \exp(Q_S/kT)$$

Species	K_H^O	Q_S	Reference
	(wppm ^a /Torr)	(cal/mole)	
H_2	3.92×10^{-3}	-7,692	(b)
D_2	4.35×10^{-3}	-6,578	(b)
T_2	(4.97×10^{-3})	(-6,086)	(c)
HF	9.14×10^{-5}	5,981	(d)
DF	12.0×10^{-5}	6,432	(d)
TF	(14.8×10^{-5})	(6,642)	(c)

^awppm - weight parts per million H, D, or T

^bObtained by least squares refinement of the data from Refs. 9 and 10.

^cEstimated using the method in Ref. 11.

^dObtained from Table IV of Ref. 12.

where ϕ is the flux of hydrogen through the interface; A and X are the area and thickness of the interface; P_H and P_L are the hydrogen pressures on each side of the interface ($P_H > P_L$); B and Q_p are parameters related to the material under consideration; and k and T are the Boltzmann factor and absolute temperature. Values of B and Q_p for the permeation of protium through materials used in the reference design model are listed in Table 15.3. Little or no data are available for deuterium and tritium permeation through these materials; hence, we have (of necessity) estimated the deuterium and tritium permeation rates by using the values of B and Q_p for protium and multiplying the result obtained via Eq. (15.5) by the square root of the mass of the hydrogen isotope of interest.

The properties above, particularly the permeation rates for the hydrogen isotopes, are strongly affected by the nature of the hydrogen bearing species at various interfaces. Several species, notably H_2 , D_2 , T_2 , HF , DF , and TF , have already been identified as being important in a LiF - BeF_2 blanket system. The extent to which other species such as HD , HT , and DT are formed is, of course, fixed by the concentrations of H_2 , D_2 , and T_2 . In the case of the reference design model considered herein, a number of other hydrogen bearing species will also be important. For instance, the circulating helium purge flow system and the pressurized helium in the blanket cooling loop will contain some amount of molecular oxygen, O_2 . Because of the conditions that will exist with regard to temperature and oxidizing potential in these circuits, most of the molecular hydrogen entering them should react with the oxygen present to form water. This is true thermodynamically as we show below, but the kinetics of the oxidation reaction must be made favorable by catalytic or electrostatic discharge type initiators in order to overcome a rather large activation barrier. (Johnson²⁰ and Watson²¹ have considered ways of using the oxidation of tritium to advantage in creating barriers to tritium migration.)

For the purposes of our calculational model, we shall assume that the following equilibria are the only ones of any consequence.

Table 15.3. Parameters for the Permeability, ϕ , of
Protium^a through the Reference Design
Construction Materials.

$$\phi \sim B \exp(-Q_p/kT)$$

Material	B (g · cm/cm ² · day · Torr ^{1/2})	Q_p (cal/mole)	Reference
PE-16	0.65 x 10 ⁻²	16,100	(b)
Incoloy 800	0.65 x 10 ⁻²	16,100	(b)
Copper	0.86 x 10 ⁻²	18,562	(c)

^a Values of ϕ for D₂ and T₂ were obtained by multiplying the values for H₂ by $\sqrt{2}$ and $\sqrt{3}$, respectively.

^b The values of ϕ for hydrogen in PE-16 and Incoloy 800 are not expected to be significantly different from those for 304-SS (Ref. 13) based on previous studies¹⁵ of the effect of composition on permeability of hydrogen in Fe-Cr-Ni alloys.

^c Obtained by least squares refinement of the data of Ref. 16.

The relationships between the partial pressures, P , of the species involved in these equilibria and the equilibrium constants, k_1 , k_2 , and k_3 , are given by Eqs. (15.6) through (15.8).

$$k_1 = \frac{P_{HT}^2}{P_{H_2} P_{T_2}} \quad (15.6)$$

$$k_2 = \frac{P_{H_2O}}{P_{H_2} P_{O_2}^{1/2}} \quad (15.7)$$

$$k_3 = \frac{P_{HTO} P_{H_2}}{P_{H_2O} P_{HT}} \quad (15.8)$$

Existing literature data¹⁷⁻¹⁹ on the temperature dependence of k_1 , k_2 , and k_3 were analyzed by a least squares method to give the best fit to Eq. (15.9).

$$\ln(k_i) = A_i + B_i \ln(T) + C_i/T \quad (15.9)$$

The values for the fitting parameters (the A_i 's, B_i 's, and C_i 's) obtained in this analysis are given in Table 15.4.

Calculations of the constants, k_1 , k_2 , and k_3 , over the operating temperature range of the reference design model, $\leq 660^\circ\text{C}$, have revealed several interesting features.²⁰ For example, by rearranging Eqs. (15.7) and (15.8) to give the relationships in Eqs. (15.10) and (15.11),

$$P_{O_2}^{1/2} k_2 = \frac{P_{H_2O}}{P_{H_2}} \quad (15.10)$$

$$P_{O_2}^{1/2} k_2 k_3 = \frac{P_{HTO}}{P_{HT}} \quad (15.11)$$

it is possible to calculate the relative amounts of water and molecular hydrogen as a function of the oxygen partial pressure. At temperatures between 200 and 660°C the value of k_2 is greater than $10^{12} \text{ Torr}^{-1/2}$, and k_3 is a dimensionless number in the range from 1 to 5. If an O_2 partial pressure of 10^{-2} Torr in the helium streams is acceptable from a materials point of view, then according to Eqs. (15.10) and (15.11), the predominant hydrogen bearing species in the helium will be water (H_2O , HTO , or T_2O) by a factor of

Table 15.4. Parameters for the Equilibrium Constants,
 k_1 , k_2 , and k_3 , in Eqs. (15.6) through (15.8)
 $\ln(k_i) = A_i + B_i \ln(T) + C_i/T$

<u>Equilibrium Constant</u>	<u>Units</u>	<u>Reference</u>	<u>Fitting Parameters</u>		
			<u>A_i</u>	<u>B_i</u>	<u>C_i</u>
k_1	Unitless	(a)	1.2787	0.0265	-141.34
k_2	Torr ^{-1/2}	(b)	-3.7675	0	30,428
k_3	Unitless	19	-2.4292	0.2920	774.82

^aObtained by least squares fitting of the data of Ref. 17.

^bObtained by least squares fitting of the data of Ref. 18.

more than 10^{11} , providing of course that the kinetics of the water producing reactions can be made favorable. This is true regardless of what the relative amounts of H_2 and T_2 entering the helium circuits turn out to be. (Note that the H/T ratio in the helium only affects the relative amounts of H_2O , HTO and HT formed.)

The net result of these water-producing reactions is that the partial pressure of the molecular forms of hydrogen in the helium carrying zones may be reduced to a value many orders of magnitude lower than in adjacent systems.^{20,21} Therefore, the primary helium coolant loop can act as a buffer region that inhibits the migration of protium (from the steam generators) to the blanket and of tritium (from the blanket) to the steam system. The helium purge flow system will act in a similar way to prevent migration of tritium beyond the immediate containment shell of the equipment under purge. Since this purge will undoubtedly operate at a lower temperature than the primary coolant helium, the formation of water will be even more favorable thermodynamically if we again assume that we can induce favorable kinetics. In order to take advantage of the hydrogen-trapping capability of low levels of oxygen in the helium circuits, a processing scheme has been devised to (a) remove and collect the HTO and H_2O , (b) recover and separate the hydrogen isotopes, and (c) recycle the tritium to the fueling system. This scheme is described in Chapter 14.

Philosophy and Description of the Calculational Model

In our study of the hydrogen isotope distribution and migration characteristics of the reference design model, we consider those primary reactor systems that come in contact with the most significant quantities of tritium, i.e., the primary fuel loop, the blanket, and the heat transfer and energy conversion circuits. The important subsystems of each of these main systems are listed in Table 15.5. The hydrogen isotope inventories and migration rates for these subsystems can be determined on an individual basis and then summed to give an estimate of the overall hydrogen isotope profile for the entire plant. For the purposes of analytical simplicity, each subsystem can be divided into N isothermal zones in such a way that the temperature drop, ΔT , across each zone is relatively small. (A ΔT of about $20^\circ C$ was used in most of the calculations that follow.) The fractional area of a particular subsystem in each temperature zone is established by the temperature profile over the entire subsystem. In the calculational model, the subsystems are considered to fall into one of two categories; isobaric or interfacial. The isobaric systems are those throughout which the chemical activities, and, hence, the partial pressures of the hydrogen bearing species

Table 15.5.

Important Reactor Systems and Subsystems to Be Mapped
for Hydrogen Isotopes

I. Primary Loop Processing System

Subsystems	Fuel Make-Up Equipment
	Injector
	Divertor
	Ducting to Vacuum Pumps
	Vacuum Pumps
	Compressor
	Cryogenic Distillation Unit
	Fuel Storage Container

II. Blanket System

Subsystems	Blanket Fluid
	Vacuum Wall (First Wall)
	Blanket Outer Wall
	Structural Material in the Blanket
	Coolant Ducts in the Blanket

III. Heat Transfer and Energy Conversion System

Subsystems	Outlet Ducting from Blanket
	to Heat Exchanger
	Inlet Ducting from Heat Exchanger
	to Blanket
	He/Steam Heat-Exchanger Wall
	Helium

are constant. Subsystems that will be treated as isobaric include the blanket fluid, the helium circuits, and the structural material in the blanket. The interfacial subsystems are those through which there is a hydrogen concentration gradient (e.g., the helium carrying ducts in the blanket) and consequently a net flow of hydrogen through the interface. In all cases, we assume that the partial pressure drop across the interface is greater than one hundredfold (i.e., $\sqrt{P_H}/\sqrt{P_L} > 100$). As a result of this assumption, the concentration of each hydrogen isotope in the structural material of an interfacial subsystem is approximately one-half that calculated by Eq. (15.1), since the average concentration is approximately given by $0.5 (\sqrt{P_H} + \sqrt{P_L}) K_S$.

In order to calculate the amount of dissolved hydrogen, $S(I)$, in the I th temperature zone we use Eq. (15.12).

$$S(I) = [0.5(\sqrt{P_H} + \sqrt{P_L}) K_S^0 \cdot 10^{-6}] [F(I) \cdot A \cdot X(I) d] \exp[Q_S/kT(I)] \quad (15.12)$$

where $F(I)$ = the fraction of the total area in the I th zone,

A = the total area,

$X(I)$ = the thickness of the material in the I th zone,

d = the density of the material,

$T(I)$ = the temperature of the I th zone in K ,

and K_S^0 and Q_S are as given in Table 15.1.

The rate of hydrogen permeation, $PERM(I)$, through the I th temperature zone is determined using Eq. (15.13).

$$PERM(I) = \frac{AF(I)}{X(I)} (\sqrt{P_H} - \sqrt{P_L}) B \exp(-Q_p/kT(I)) \quad (15.13)$$

where the parameters on the right-hand side are the same as those defined for Eqs. (15.5) and (15.13) and in Table 15.3.

The $S(I)$'s and $PERM(I)$'s are then summed over the N temperature zones to give the total concentration, C , and permeation rate, Φ , for the subsystem being investigated.

$$C = \sum_{I=1}^N S(I) \quad (15.14)$$

$$\phi = \sum_{I=1}^N \text{PERM}(I) \quad (15.15)$$

Equations 15.12 through 15.15 were programmed into a computer code (HDTMAP) so that the very large number of tedious calculations that needed to be made for the subsystems listed in Table 15.5 could be carried out rapidly. Another advantage of programming the calculations in this way is that it has been possible to test a large number of variations of dimensional and operational parameters for the reference model. The text of the computer program together with examples of output for isobaric and interfacial subsystems has been reported elsewhere.²²

Application of the Calculational Model to the Reference Design

In applying the calculational model to the reference design, several restricting conditions had to be employed. For the blanket and heat transfer systems, we have assumed that only protium and tritium are present in significant amounts. The predominant hydrogen bearing species in the blanket are assumed to be HF, TF, H₂ and T₂. Tritium is, of course, bred in the blanket. The important sources of protium to the blanket are considered to be (n,p) reactions and back permeation from the helium coolant. Because of the isotopic swamping effects that should occur in the steam system, only H₂, HTO, and H₂O are considered to be important species there. As we showed above, the predominant hydrogen-bearing species in the primary helium coolant are H₂O and HTO with trace amounts H₂, HT, and T₂. The primary fuel loop is composed almost exclusively of D₂, DT, and T₂, with very little H₂, HT, or HD, because the steady state protium level of the fuel stream is, of necessity, very low (on a relative basis) as indicated previously (Chapter 14).

Several other restrictions were mentioned in previous sections of this chapter. One of these is the assumption that the hydrogen isotope partial pressure drop across any interfacial barrier is over two orders of magnitude (i.e., $P_H/P_L \geq 100$). The driving forces that maintain these pressure drops are as follows: As a result of the fueling and burning characteristics of the reference design (Chapters 5 and 6), the partial pressures of deuterium and tritium species on the plasma side of the vacuum wall will stay in the range 10^{-2} to 10^{-4} Torr, while the partial pressures of protium bearing species will be considerably lower. The blanket fluid (LiBeF₃) will be processed so that the partial pressure of the tritium bearing species, T₂ and HT, is $\geq 10^{-2}$ Torr. According to Grimes and Cantor,²³ this leads to the requirement that the processor

also maintain the HF and TF pressures in the blanket at levels < 1 Torr. As a result of Eq. (15.11), the partial pressures of HT and T_2 in the helium coolant could be reduced to levels $\leq 10^{-12}$ Torr by maintaining the O_2 partial pressure in the helium at 10^{-2} Torr and removing the H_2O and HTO at a fast enough rate so that their partial pressures in the helium do not exceed 10^{-2} Torr.

For all of the subsystems included in the primary loop processing system in Table 15.5, it is assumed that the partial pressures of HT and T_2 just outside the primary containment of each subsystem are maintained at levels $\leq 10^{-15}$ Torr by the helium purge flow system according to Eqs. (15.10) and (15.11).

Mapping studies of the subsystems listed in Table 15.5 were performed (with HDTMAP) using the appropriate dimensional data for each subsystem as given in Chapter 11, the parameters listed in Tables 15.1 through 15.4, and the assumptions about partial pressure levels of important protium, deuterium, and tritium bearing species discussed above.

Result of the Mapping Calculations

The intention of the first round of calculations was to determine the magnitude of the most significant tritium losses; hence, the study concentrated on the series of heat exchangers that are described in detail in Chapter 11. It is held that these represent the only significant tritium leakage path because they are the only interfacial components that are not amenable to disengagement from the environment by encapsulation in a migration barrier formed by a relatively impermeable coating, a purge jacket, or a combination of both. Since there is continuous leakage of water from the steam system to the environment, it is conceivable that all the tritium entering this system would eventually escape. Thus, the worst case is assumed, and the amount of tritium that would be emitted to the environment if all of the water in the steam system were routinely dumped into an existing lake or river is calculated.

The parameters listed in Table 15.6 for the heat exchangers and the solubility and permeability constants given in Tables 15.1 through 15.3 were used to calculate the tritium distribution and leakage characteristics of the heat transfer network. The partial pressure of tritium in the $LiBeF_3$ was fixed at 10^{-2} Torr, in accord with the extraction system proposed in Chapter 14. The oxygen level in the helium coolant was held at 10^{-2} Torr and the partial pressure of tritium gas (HT and T_2) was assumed to not exceed 10^{-12} Torr in the helium circuits if the desiccators (Chapter 14) maintain the HTO and H_2O partial pressures at levels $\leq 10^{-2}$ Torr.

Table 15.6.
Parameters for the Heat Exchanger
Mapping Study on the Reference Model

Heat Exchanger	Material	Total Area ^a (cm ²)	Thickness (cm)	System Temperature ^b (°C)	
				Highest	Lowest
LiBeF ₃ /He	PE-16	2.16×10^8	0.285	663	360
Main Steam Generator	Incoloy - 800	6.49×10^8	0.47 ^c	550	320
High Pressure Reheater	Incoloy - 800	1.05×10^8	0.38 ^c	590	470
Low Pressure Reheater	Incoloy - 800	0.84×10^8	0.38 ^c	595	495

^a The combined area of all independent units.

^b Temperature profiles are given in Appendix B.

^c Incoloy-800 only. (This does not include the 0.25-cm thickness of copper used in some of the calculations.)

In order to test a means of providing an additional limitation to the tritium flow through the helium/steam heat exchanger network, we have also considered a case where the tubing in this network is fabricated from a laminated composite consisting of a layer of copper sandwiched between two layers of Incoloy-800. The copper must be tightly clad to the Incoloy-800 at both interfaces in this composite so that there is minimal impedance to heat transfer and to prevent streaming of hydrogen isotopes across the interface surfaces in the event that the copper layer develops holes.

A series of calculations with and without a 0.25 cm copper layer in the helium/steam heat exchanger are outlined in Table 15.7. The copper layer proves to be a reasonably good limiter to tritium migration if the continuity of the laminated structure is maintained; however, without the copper layer, the indicated loss rate of < 6 Curies/day is still well within the limits of acceptability previously discussed. The results of these calculations are illustrated in Fig. 15.1.

The amounts of dissolved tritium in the heat exchanger structure and fluids (see Fig. 15.1) are small considering the volumes of these materials. For example, if it becomes necessary to remove and refabricate all the helium-carrying ducts in one of the 24 blanket segments, and if there is no opportunity to remove the tritium held up in these ducts prior to disassembly, the duct material will contain about 1,000 Curies of relatively immobile tritium, an amount that does not present a severe handling or disposal problem in comparison to the problems associated with other activation products in the ductwork. Under the conditions considered in the above analysis, the total amount of tritium held up (dissolved) in the blanket structure should not exceed 30 grams. As indicated in Fig. 15.1, there will be about 5 grams of tritium dissolved in the helium carrying ducts in the blanket region. The remaining structural material within the plant that is not directly interfaced with the blanket fluid will contain no more than 2 grams. Concentration of most of the tritium holdup in the blanket region is a consequence of the fact that the chemical activity of tritium as HT or T_2 is much higher in the blanket fluid ($< 10^{-2}$ Torr) than in any other sector of the plant ($< 10^{-6}$ Torr). Even so, hydriding of the structural materials is not expected to become a serious problem anywhere in the plant because the maximum tritium concentration does not exceed 0.1 ppm in the blanket structure and is appreciably lower in other regions.

The leakage of tritium through walls and other containment structure into the helium purge flow system is calculated based on a model that assumes the total area of the structure to be roughly 10^{10} cm² of stainless steel 1-cm thick at a temperature of 500°C. The assumed area and thickness appear to be in line with current and projected

Table 15.7.
 Summary of Leakage Calculations
 on the Helium/Steam Heat Exchanger
 Network for the Reference Model

<u>Helium/Steam Heat Exchanger Wall</u>	<u>Daily Tritium Leakage to Steam System</u>	
	<u>grams</u>	<u>Curies</u>
Without Copper Layer	6×10^{-4}	< 6
With 0.25 cm Copper Layer	1×10^{-4}	~ 1

744379

Fig. 15.1 Tritium map of the heat exchanger network for the PPL Tokamak design.

design details for most of the important systems, but these details remain to be established in future design efforts. The assumed temperature is somewhat higher than the anticipated average temperature of the containment structure, which will be shrouded by the helium purge flow system; hence, the results of a leakage calculation at 500°C should be reasonably conservative. Using this model, the parameters for tritium permeation through PE-16 and Incoloy-800 given in Table 15.3, and an average T_2 driving pressure of 10^{-6} Torr, we estimate a tritium leakage rate to the purge flow system of 2 grams/day. Although this leakage rate is small compared to the tritium injection rate of 250 grams/hr or breeding rate of 23 grams/hr, the tritium collected in the purge must still be recovered, processed, and returned to the fueling system in order to avoid long-term economic penalties and additional waste disposal problems. If the leakage rate to the environment from the purge flow corresponded to 100 ppm per day (a conservatively large fraction) then the daily tritium release to the atmosphere as a gaseous discharge would be about 2 Ci. This release coupled with the 6 Ci/day (or less) loss from the steam system gives a total tritium leakage of < 2 Ci/day/1000 MWt.

Heat Exchanger Breakdown

It is beyond the scope of this study to consider in detail the consequences of leaks that develop through the heat exchangers. Small leaks in the cooling tubes that carry helium through the blanket could in principle lead to a situation where the tritium flow into the helium exceeds the limits of the oxidizing potential of the helium circuit and/or overloads the desiccators. This situation is somewhat mitigated by the fact that helium sprays into the blanket fluid; thus, any tritium passing through leaks in the heat exchanger wall must flow against the thrust of a high pressure helium jet. This is true in the case of the helium/steam heat exchanger as well, where the steam sprays into the helium. Experience with helium/steam heat exchangers from other programs (like the High Temperature Gas Cooled Reactor) is not adequate to allow the establishment of reliable criteria for helium/steam heat exchanger integrity. However, these problems should be less severe than for sodium/steam systems where additional wastage due to the sodium-water reaction can cause failure propagation.²⁴ Since there appears to be no wastage mechanism in a helium/steam system, there is no reason to expect rapid, large scale breakdown of the heat exchanger structure to develop from a small number of scattered pinhole leaks.

References

- ¹D. Steiner and A. P. Fraas, Preliminary Observation on the Radiological Implications of Fusion Power, Nuclear Safety 13, 353 (1972).
- ²W. M. Mueller, J. P. Blackledge, and G. G. Libowitz, Metal Hydrides (Academic Press, New York, 1968) p. 71.
- ³M. Smialowski, Hydrogen in Steel (Addison Wesley, Reading, Massachusetts, 1962) p. 32.
- ⁴N. J. Hawkins, Solubility of Hydrogen in Nickel and Type - 347 Stainless Steel, Knolls Atomic Power Laboratory, Schenectady, New York, Report KAPL-868 (1953).
- ⁵A. Sieverts, Zur Kenntnis der Okklusion und Diffusion von Gasen Durch Metalle, Z. Phys. Chem. (Leipzig) 60, 129 (1907).
- ⁶M. Smialowski, op. cit., pp. 37-41.
- ⁷M. Smialowski, op. cit., p. 28.
- ⁸M. Blander, Molten Salt Chemistry (John Wiley and Sons, New York, 1964) pp. 230-233.
- ⁹A. P. Malinauskas and D. M. Richardson, Hydrogen Solubility in Lithium-Beryllium-Fluoride, Oak Ridge National Laboratory Report ORNL-4832 (1973) p. 38.
- ¹⁰A. P. Malinauskas, Private communication.
- ¹¹C. G. Swain, E. C. Stivers, J. F. Reuwer, Jr., L. J. Schaad, Use of Hydrogen Isotope Effects to Identify the Attacking Nucleophile in the Enolization of Ketones Catalyzed by Acetic Acid, J. Am. Chem. Soc. 80, 5885 (1958).
- ¹²P. E. Field and J. H. Shaffer, Solubilities of Hydrogen Fluoride and Deuterium Fluoride in Molten Fluorides, J. Phys. Chem. 71, 3218 (1967).

¹³R. W. Webb, Permeation of Hydrogen through Metals, North American Aviation, Downey, California, Report NAA-SR-10462 (1965).

¹⁴R. E. Stickney in The Chemistry of Fusion Technology, D. M. Gruen, ed. (Plenum Press, New York, 1972) pp. 241-320.

¹⁵M. Smialowski, op. cit., pp. 102-107.

¹⁶C. J. Smithells and C. E. Ransley, The Diffusion of Gases Through Metals, Proc. Roy. Soc., (London) 150A, 172 (1935).

¹⁷R. Viallard, Tritium Moléculaire Hydrure de Tritium et Deuteriure de Tritium. T₂, HT, DT, Nouveau Traité de Chimie Minerale 1, 924 (1956).

¹⁸R. H. Perry, C. H. Chilton, and S. D. Kirkpatrick, Chemical Engineers' Handbook, fourth edition (McGraw-Hill Book Co., New York, 1963) Section No. 9, p. 40.

¹⁹D. G. Jacobs, Sources of Tritium and Its Behavior upon Release to the Environment, USAEC Report TID-24635 (1968) p. 8.

²⁰E. F. Johnson in The Chemistry of Fusion Technology, D. M. Gruen, ed. (Plenum Press, New York, 1972) pp. 191-214.

²¹J. S. Watson, An Evaluation of Methods for Recovering Tritium From the Blankets or Coolant System of Fusion Reactors, Oak Ridge National Laboratory Report ORNL-TM-3794 (1972).

²²V. A. Maroni, An Analysis of Tritium Distribution and Leakage Characteristics for Two Fusion Reactor Reference Designs, Argonne National Laboratory Report CEN/CTR/TM-9 (1974).

²³W. R. Grimes and S. Cantor, Molten Salts as Blanket Fluids in Controlled Fusion Reactors, Oak Ridge National Laboratory Report ORNL-TM-4047 (1972).

²⁴D. A. Greene, The Leakage of Water into Sodium in Steam Generators and Test Rigs, Nucl. Technology, 18, 267 (1973).

Chapter 16

Hazards

P. W. Davison *

Introduction

In this chapter the major systems of the plant are reviewed from the point of view of identifying the hazards associated with their normal and abnormal operation. Both public hazards and service hazards are considered.

A public hazard is defined as one whose consequences could include injury or radiation dosages in excess of regulatory limits to persons located outside the plant site boundary, or damage to or radioactive contamination of public or private property outside the site boundary. A service hazard is defined as one that could result in injury or radiation dosage in excess of regulatory limits to persons within the site boundary, could lead to damage to or radioactive contamination of plant property, or could result in the interruption of the electric power output of the plant.

Engineered safeguards and operational procedures are identified for eliminating these hazards or minimizing their consequences. The most serious credible accident is identified as the design basis accident and the design requirements specified for assuring that the consequences of that accident will not exceed the regulatory limits for accident conditions.

In assessing the hazards involving radiation and radioactive materials, use has been made of guidelines established by the U. S. Atomic Energy Commission and published in the Code of Federal Regulations.¹ Those guidelines, which it is believed will be applicable to the hazards associated with the normal operation of a fusion power plant, are given in Table 16.1. The first two lines in the table are taken from Ref. 1, Part 20 - Standards for Protection Against Radiation, Paragraphs 20.101, 20.103, 20.105, and 20.106. The last two lines in the table are taken from Part 50 - Licensing of Production and Utilization Facilities, Proposed Appendix I, Section II,² which gives some specific objectives for the design of light water reactors. For hazards due to abnormal operation, the guides have been used that are given in Part 100 - Reactor Site Criteria, Paragraph 100.11 and quoted with adaptations below; they consist of changing the phrase "fission product release" to "volatile radioactive material release" and deletion of references to the thyroid dose from iodine exposure.

* Westinghouse Electric Corporation

Table 16.1.

Maximum Allowable Levels of Radiation and Radioactive
Materials for Normal Reactor Operation

Area	Whole Body Radiation Dose	Tritium Concentrations above Natural Background*		
		Inhalation $\mu\text{Ci}/\text{m}\ell$	Submersion** $\mu\text{Ci}/\text{m}\ell$	In Water $\mu\text{Ci}/\text{m}\ell$
Restricted, on Site	1.25 rem/qtr	5×10^{-6}	2×10^{-3}	1×10^{-1}
Unrestricted, on Site	2 mrem/hr	2×10^{-7}	4×10^{-5}	3×10^{-3}
At Site Boundary	5 mrem/yr(liquid) 20 mrem/yr(gaseous)	***	-	***
Sizable Population Groups	1 mrem/yr	-	-	-

* Averaged over a period of not greater than one year except for Restricted Areas, in which case concentrations are based on exposures of 40 hours in any period of seven consecutive days; for shorter exposures in restricted areas, the concentrations can be increased accordingly.

** Values given in this column are for submersion in a semi infinite cloud of airborne material.

*** The most recent version of the Proposed Appendix I to 10CFR50² gives no limitation on tritium release concentrations except in terms of total annual doses to personnel in unrestricted areas. If it is assumed that the total annual dose is due to inhalation, the allowable concentration of HTO in air is 1.2×10^{-9} Ci/m ℓ based on a dose conversion factor of 1.7×10^{-6} (rem/yr)/(pCi/m ℓ) from Table D.1 of Ref. 5. For tritiated water the allowable concentration is 5×10^{-5} $\mu\text{Ci}/\text{m}\ell$ based on the International Commission on Radiological Protection Committee 2 dose conversion factor of 1×10^{-5} (mrem-yr)/($\mu\text{Ci}/\text{m}\ell$).

"100.11 (a) As an aid in evaluating a proposed site, an applicant should assume a volatile radioactive material release from the reactor, the expected demonstrable leak rate from the containment and the meteorological conditions pertinent to his site to derive an exclusion area, a low population zone and population center distance. For the purpose of this analysis, which shall set forth the basis for the numerical values used, the applicant should determine the following:

1. An exclusion area of such size that an individual located at any point on its boundary for two hours immediately following onset of the postulated volatile radioactive material release would not receive a total radiation dose to the whole body in excess of 25 rem.
2. A low population zone of such size that an individual located at any point on its outer boundary who is exposed to the radioactive cloud resulting from the postulated volatile radioactive material release (during the entire period of its passage) would not receive a total radiation dose to the whole body in excess of 25 rem.
3. A population center distance of at least one and one-third times the distance from the reactor to the outer boundary of the outer low population zone. In applying this guide, due consideration should be given to the population distribution within the population center.

Where very large cities are involved, a greater distance may be necessary because of total integrated population dose consideration."

Identification of Hazards by Major Systems

Reactor (Including Magnet Coils)

1. Normal Operation

The reactor and plant will be designed so that there are no public hazards during normal operation of the reactor. The public hazards associated with neutron and gamma radiation and tritium leakage will be eliminated by adequate shielding and containment design. There are, however, several service hazards which must be considered.

Tritium Leakage

This is considered to be the most serious service hazard during normal operation of the reactor. The public is protected from this hazard by the reactor building containment system. However, plant personnel must routinely enter the reactor building to maintain the auxiliary systems and equipment located there.

The presence of a significant inventory of tritium, about 540 g or 5.4 MCi, in the reactor and associated systems represents a radiological hazard that must be evaluated. Tritium emits only β -rays and thus, as long as it is contained, it presents no hazard. However, because of the high rate of permeation of tritium through many materials, including stainless steel, particularly at elevated temperatures, the potential exists for leakage of tritium into the atmosphere of the reactor building. There it would represent a hazard to personnel entering the building. The direct hazard would be the result of ingestion by inhalation, but there is also the indirect hazard from absorption in the skin and clothing:

The primary safeguard against this hazard is that of taking all practical means to contain the tritium within the various systems. The reactor vacuum chamber must be extremely leak tight in order to maintain the very high vacuum and low impurity levels required for reactor operation. This will be accomplished by using welded joints wherever possible, and where gasket seals must be used they will be double with the space between evacuated. A low pressure helium purge will be maintained in the region between the flibe blanket and the shield blanket. All pipes carrying fluids containing tritium at elevated temperatures will be double walled with a helium purge between the inner and outer walls. By these means the tritium leakage to the atmosphere of the reactor building will be maintained at essentially zero during normal reactor operation. In addition the building atmosphere will be continuously monitored and should the tritium level exceed safe levels, the access of personnel to the building will be restricted until the levels are reduced by the building atmosphere control system.

Radiation Leakage

The blanket shield is designed to furnish protection from radiation heating and damage for the reactor components located in the reactor compartment. This shielding will not be adequate to reduce the radiation to biologically acceptable levels during reactor operation. Thus, an additional shield will be placed outside the reactor structure.

The neutrons produced in the plasma and the gamma rays generated in the flibe blanket and structural materials surrounding the plasma are a potential hazard to equipment and personnel. The hazard to equipment is that of radiation damage and heating. The hazard to personnel is that of radiation induced biological effects.

This hazard is eliminated by the use of adequate shielding around the reactor. Of the equipment which must be located in the immediate vicinity of the reactor, the most sensitive to radiation damage and heating are the superconducting magnet coils (Chapter 13). A shield located just outside the flibe blanket and divertor throat has been designed (Chapter 10) to furnish adequate protection for these coils. Additional shielding between the vertical field coils has been added to furnish biological shielding to allow access to the reactor area when the reactor is shut down.

The radiation levels outside the blanket shield are, however, during operation 65.6 rad/hr from gamma rays and 3625 rem/hr from neutrons, which are much too high for personnel exposures. Therefore, a biological shield has been placed outside and completely around the reactor structure. This two-meter-thick concrete shield reduces the neutron level to 0.2 mrem/hr, which is a factor of 10 below the maximum level allowed

for an on site unrestricted area. Physical barriers and mechanical interlocks prevent personnel entry inside the biological shield during reactor operation.

When the reactor is shut down, the radiation levels are significantly reduced in a matter of minutes. The neutron levels are essentially negligible - there are no delayed neutrons as in fission reactors. However, because of induced radioactivity in the reactor materials, there is a radiation level within the biological shield of 27 mrad per hour due to gamma rays. This level is low enough to allow access of personnel to the areas outside the blanket shield but will require procedures and health physics monitoring to assure that the accumulated doses to personnel are maintained below the levels given in Table 16.1 and Paragraph 20.101 of Ref. 1.

High Magnetic Fields

The hazards associated with the presence of a large volume of high magnetic field fall into the category of service hazards. There are hazards to personnel (Ref. 3), but because of the distance to the site boundary and the rapid falloff of the magnetic fields with distance, there will be no hazards to the public. A plot of the magnetic field as a function of distance from the center of the reactor is shown in Fig. 16.1.

The hazards to equipment are those associated with the performance of rotating machinery and electrical equipment in high magnetic fields. These can be eliminated or minimized by proper location of such equipment and by use of magnetic shielding. The biological effects of magnetic fields have been speculated on for several centuries, but it has only been in the past decade that any attempts to obtain quantitative data have been made. There are certainly hazards to personnel and equipment when loose magnetic materials, such as tools, are used in high magnetic fields. These hazards are well known, and procedures and techniques exist to eliminate or minimize them.

2. Abnormal Operation

The plant will be designed to eliminate public hazards from abnormal operation wherever possible. A review of the credible abnormal operations of the reactor indicates that this can be done. The only hazards to the public are those whose consequences involve the release of radioactive material. The reactor building containment plus proper system design prevent the release of excessive quantities of radioactive material to the environment due to any conceivable accident or abnormal conditions.

As discussed in Chapters 10 and 17, significant quantities of radioactive materials are produced in the structures and breeding blanket of the reactor. At reactor shutdown after 30 years of operation - the worst case - over half of the activity, and all of the long lived activity, is from metals. The remainder have an initial activity

744407

Fig. 16.1 Reactor poloidal magnetic field strength (no control coils).

of 7100 megacuries. They all have half-lives of less than half a minute except for the fluorine-18 with a half-life of 110 minutes and an activity of 660 megacuries. They are contained in the flibe system at low pressure - less than 2 atmospheres - so that even if the flibe system were to rupture, the radioactive gases would be retained within the containment building.

The helium system does contain 7 grams, 70,000 curies, of tritium and is at a fairly high pressure, 50 atmospheres. However, as indicated in Fig. 11.22 of Chapter 11, shutoff valves are located at strategic points in the reactor cooling system so that if a helium line or tube ruptures in one or more sectors of the reactor it or they can be isolated, and only a fraction of the total helium would be released. Based on the analysis of a major helium duct rupture, which is discussed below, it has been concluded that the helium and the tritium contained in it which are released in this type of accident would be contained within the reactor building and thus would not be released to the atmosphere.

Rupture of a Helium Coolant Line

The rupture of a tube or header in the reactor cooling system is considered the most serious service hazard associated with the reactor system. As mentioned above, shutoff valves will isolate the rupture from the rest of the cooling system; relief valves will be connected to the breeding blanket and vacuum systems to prevent them from overpressurization and rupture; however, except for minor, pinhole type leaks, the reactor will have to be shut down and the leak isolated or repaired. Incidentally, if the leak is in the radiation shield wall or the inner side of the vacuum wall, it will probably extinguish the plasma and thus, shut down the reactor. Pressure transducers located in the headers in each reactor sector and cooling subsystem will allow the defective subsystem to be isolated and normal cooling to be maintained to the remainder of the reactor with one exception. In the present design of the vacuum wall and radiation shield wall cooling systems they have a common exit header. It would probably be feasible to use separate headers, but this has not been analyzed. Even so, unless the rupture were massive, cooling under shutdown conditions could be maintained until the decay power dropped to a level where the cooling could be discontinued. Also, the vacuum wall would be cooled by the flibe, and thus the radiation shield wall is the one most likely to overheat; however, it is also the one that could withstand some overheating because with little or no helium pressure in the tubes, the mechanical requirements placed on it are minimal.

If the rupture occurred in the flibe blanket cooling system, the tubes and headers would be cooled by the flibe. It is possible that they would exceed their design maximum wall temperature of 680°C for a period of the order of a half hour because the bulk flibe temperature at shutdown would be about 800°C . However, without the helium pressure in the tubes, this should cause no damage to them.

The walls of the divertor receive only low levels of neutron and gamma ray flux and will not have any significant decay heat removal requirements. There would probably be an overheating problem during the reactor shutdown period - less than one minute - but cooling could be maintained during this interval.

It has been concluded, therefore, that in the event of a reactor cooling system malfunction, the damage could probably be confined to the tube or line involved or, at worst, to the reactor sector in which it occurred. If only one or several tubes are damaged they could be plugged using techniques that have been developed for remotely plugging defective tubes in steam generators. Because of the good thermal contact between adjacent cooling channels in the vacuum wall, there would be no overheating problems in the plugged channel. For a cooling tube in the flibe blanket, its temperature would rise to that of the flibe, but with no internal pressure this would be acceptable. In the radiation shield wall, the thermal contact between adjacent tubes is poor so the plugged tube would probably heat up. However, the tube would have little in the way of mechanical loads to support and could withstand overheating.

The reactor building will have an atmosphere control system for maintaining the tritium levels below acceptable values during normal operations and to reduce them to acceptable values in a short period of time after an accident such as this. So within a day or so after the malfunction, personnel access to the reactor area would be possible without special clothing and breathing equipment.

The tube plugging operation could be performed from the region just outside the blanket shield; as mentioned above during shutdown periods the radiation levels outside that shield are low enough to allow extended, though monitored, occupation by personnel. Therefore, it is estimated that a shutdown of the order of several days to a week would be all that would be required.

If the damage were extensive enough to require replacement of a reactor sector, the shutdown time would be longer. However, since it is normally planned to replace sectors on a periodic basis, they will be designed, and procedures will be developed to do this expeditiously - the goal for this operation is to perform it within a period of one month or less.

Thus, it is concluded that while plant operation will probably have to be interrupted as a consequence of this malfunction, the interruption would not be longer than that required for the periodic replacement of a reactor sector. This is undesirable but is not considered serious.

As mentioned above relief valves are provided to protect the vacuum chamber and the flibe blanket structure. It might be possible to vent those valves to an accumulator, but it would be preferable to vent them to the upper portion of the reactor building because of the need for a low back pressure system. As will be discussed below, the portion of the reactor building above the reactor floor and the compartments containing the helium ducts (see Fig. 11.25) are hermetically sealed relative to the rest of the reactor building. Accordingly, this malfunction would not represent a hazard to personnel in the other portions of the reactor building. Personnel would not normally be in the area above the reactor floor or in the duct compartments. However, should individuals happen to be there at the time the malfunction occurred, they would receive radiation exposure due to the release of the tritium contained in the helium. The vents would be directed upward so that the personnel would not be subjected to direct exposure to the high temperature helium and would be able to leave the area rapidly through airlock type portals. It is difficult to estimate the radiation dose they would receive, but it would most probably be less than the acceptable once in a lifetime dose of 25 rem.

Malfunction of the Divertor Field System

Another serious service hazard is one associated with a malfunction of the system that establishes the magnetic field configuration required to produce the divertor action (see Chapter 6). One of the functions of the divertor is to prevent the cold plasma striking the walls of the plasma chamber and the divertor chamber. Thus, if this system malfunctions, the cold plasma will strike the radiation shield wall. The resulting damage will depend on the extent and duration of the malfunction. It is expected to be self-limiting in that the impurities released from the radiation shield wall will extinguish the plasma. This will probably occur before a significant amount of material is removed from the wall. The increase in radiation because of the impurities in the plasma will produce a thermal transient in the inner surface of the wall, but the wall can be designed to withstand it.

Should the malfunction be such that the cold plasma strikes only a small portion of the radiation shield wall, it could remove enough of the wall material to cause a rupture. The consequences in that event would be similar to those discussed above for a coolant tube rupture.

In any event, the plant would have to be shut down, the cause of the malfunction corrected, the extent of the damage determined and any needed repairs made. In what is considered to be the more probable case of no damage to the radiation shield wall, the shutdown period would be less than a day. In the unlikely event of the rupture of a number of the coolant tubes in the wall, one or more sectors of the reactor would have to be replaced, and the plant would be down for the order of a month.

Malfunction of the Vertical Field System

The consequences of a malfunction of this system would be similar in many respects to those discussed above for a malfunction of the divertor field system. A reduction in the magnitude of the vertical field would cause the separatrix to move out along the major radius with the result that the cold plasma would strike the radiation shield wall. If the reduction were large enough, the hot plasma would also strike the wall. In the latter event, there would probably be extensive damage to the wall, and the plant would be down for several months.

Malfunction of the Toroidal Field or Control Field System

For anything except a major malfunction of these two field systems, there would be no damage to the reactor. As discussed in Chapter 13, up to eight of the toroidal field coils can be out of service, and as long as adjacent coils are not involved reactor operation could be continued at a reduced level. Should the malfunction be that one or more of the coils went normal, provision will be made for dumping the stored energy into a small lake adjacent to the reactor building.

The results of a malfunction of the control field system would depend on when it occurred in the operating cycle and the effect it had on the field (see Chapter 5). If one of the field coils had to be taken out of service, reactor operation could be resumed using a shorter operating cycle.

Heat Transport System

The heat transport system transfers the thermal power from the reactor to the steam generators, which in turn supply power to the turbine generator. This system is described in detail in Chapter 11. Normal operation of this system will not involve any public hazards nor any serious service hazards. However, certain abnormal conditions have been identified that do present significant though not excessive public and service hazards.

1. Normal Operation

The only significant hazard during normal operation that has been identified is that due to leakage of tritium from the system. This hazard and the procedures to eliminate or minimize its consequences are discussed above in the section Reactor (Including Magnet Coils), 1. Normal Operation, Tritium Leakage. That discussion is equally applicable here with the difference that the tritium contained in this system is only 7 grams or 70,000 curies.

2. Abnormal Operation

One mode of failure of this system, the rupture of one of the main ducts, is considered to be the service hazard with the most serious consequences. That is because it could result in the serious injury to or death of plant personnel. It also is the loss of coolant accident and could result in major damage to the reactor. However, the exposure of off site personnel as a result of this accident while high is not excessive.

Rupture of a Main Duct

It is considered very unlikely that one of the main helium ducts between the reactor and the steam generators would rupture. They will be double walled to furnish thermal insulation and tritium containment. They will be conservatively designed to withstand any foreseeable abnormal condition such as thermal transients and earthquakes. However, the consequences of such an accident are so great from the point of view of plant operation that they must be recognized.

Should this accident occur, the high pressure helium (50 atmospheres) at high temperature (638°C, maximum) would be released into the Steam Generator Building, the duct compartment and the Reactor Building above the operating floor. If this helium were contained within those areas, an approximate calculation using the air and helium volumes, temperatures and pressures given in Table 16.2 shows that the pressure would reach 3.0 atm (29 psig) above ambient and the temperature 200°C. The design and construction of a containment to confine this helium-air mixture should pose no special problems - the conditions are similar to those that current nuclear plant containments are designed to withstand. The tritium could be removed by the atmosphere control system and very little released to the outside. However, if it were rapidly released accidentally through the 100 m stack, under the worst weather conditions (fumigation), the atmospheric dispersion factor at 200 m would be 5.2×10^{-4} sec/m (Fig. 2.1 of Ref. 4) so the maximum off-site dose would be 2 rem. This is over a factor of 10 below the 10CFR100 guideline of 25 rem. If the release were, either accidentally or deliberately,

Table 16.2
Fluid Inventories - Normal Full Power Condition

	Volume m^3	Temperature $^{\circ}\text{C}$	Pressure atm
Free Air in Containment & S. G. Bldg.	2.04×10^5	20	1.0
Free Air in Turbine Hall	5.00×10^4	20	1.0
Helium in Heat Transport Systems Hot Leg	4,820	638	49.0
Helium in Cold Leg	4,820	360	51.0
H ₂ O in Hot Leg	1,290	538	251
H ₂ O in Cold Leg	487	294	301
Total Water in Secondary Systems	2,400	-	-

more gradual the dose would be much less. Using Fig. 2.2 of Ref. 4, and assuming a uniform release rate over an 8-hour period, the maximum dose would be 80 mrem at 600 m. For a 4-day release the maximum would be 43 mrem at 600 m, and for a 30 day release, 1.2 mrem at 600 m.

As mentioned earlier, in the discussion of the rupture of a reactor system coolant line, personnel will not normally be on the reactor floor or in the duct compartments. However, should any be present in those areas in which this accident occurred, they would receive excessive radiation exposures. But that would probably be academic because they would more likely die because of exposure to the high temperature helium long before the effects of the radiation exposure became serious.

As pointed out above, it is believed that this malfunction is the one that would result in the most serious damage to the reactor. As discussed in the section above on rupture of a reactor coolant line, the flibe in the breeding blanket would protect the vacuum wall and the blanket cooling tubes under conditions with zero or low pressure in them. However, the radiation shield wall would not be so protected and it is possible that all or a significant fraction of it would have to be replaced. This would require the plant to be shutdown for a number of months.

Other Malfunctions

The other malfunctions identified for the heat transfer system are those that are related to the operation of the valves in the system. These failures are the following: the valve doesn't open or close when commanded to do so; the valve becomes open when it should be closed; and becomes closed when it should remain open. During normal operation all of the valves shown in Fig. 11.22 are fully open except the three on the outlets of each of the helium circulators (36 valves); the positions of those valves are adjusted in accordance with the level of power being generated. Thus, the valves would normally be operated during startup, power change or shutdown, and the failure of the valves to operate would only delay the planned operation. Once the plant is in operation, the only valves that must be operated normally are those on the outlets of the circulators. Because of the twelvefold redundancy, failure of one of these valves could be compensated for by adjusting the others. Also the actuators for those valves are accessible during reactor operation, and if the cause for the failure were in the actuator, it could be corrected with only a minor perturbation on the plant operation.

However, during certain accident conditions, such as a pipe or duct rupture, some or all of the valves would be commanded closed depending on the location and nature

of the malfunction. If the proper valve or valves did not close, the consequences of the accident would be worse than otherwise but in any event would be no worse than the complete loss of coolant accident discussed above.

Power Conversion System

During the normal operation of this system and also during most of the modes of abnormal operation, with the exception of the additional hazards introduced by the presence of a significant quantity of tritium in the system, the hazards and their consequences are the same as those of a conventional fossil fired or nuclear power conversion system of the same rating using supercritical steam. While the consequences of these hazards are significant, methods have been and will continue to be developed to eliminate or minimize those consequences. This plant will take full advantage of those methods. Some of the hazards due to the presence of tritium in the system are discussed in Chapter 17 in the section on Waste Disposal, but those will also be considered here.

1. Normal Operation

During normal operation, 6 Ci/day or less of tritium diffuse through the steam generator tubes into the steam system (see Chapter 15). In the analysis, the results of which are given in Chapter 17, Waste Disposal, it was concluded that the preferred method of disposing of this tritium is to bleed off 1.5% of the steam system inventory per full power day of operation and vent that steam through a 100 m stack. For average weather conditions, this results in 0.18 mrem/yr maximum exposure to off-site personnel - a factor of about 100 below the recommended guidelines for light water reactors given in the proposed Appendix I to 10CFR50². The tritium inventory in the steam system would be 400 Ci, and the components of the system would be saturated with tritium. Therefore, any maintenance operations will have to be planned with this in mind. However, because tritium emits only β -rays, protective gloves and clothing and breathing equipment are all that would be required. During maintenance periods, the tritiated water in the system would be dumped into the condensers and auxiliary storage tanks.

2. Abnormal Operation

The presence of tritium in the steam system also introduces additional hazards during abnormal operation of the system. The consequences of these hazards are significant but acceptable.

Main Steam Line Rupture in Turbine Hall

From the public hazard and service hazard point of view, the most serious abnormal operation of the power conversion system would be a rupture of the main steam

line in the turbine hall at or near the very high pressure turbine or the high-pressure turbine. There will be shutoff valves in the outlets of the steam generators, throttle valves at the turbines, and provision for steam dump to the condenser, but it is assumed that these do not operate. If they all function properly, within 30 seconds the steam and tritium release will be reduced by a factor of 50. Assuming that the total steam system inventory is released to the turbine hall and using the volumes, temperatures and pressures given in Table 16.2, the resulting temperature would be about 200°C and the pressure about 7.7 atm or 98 psig. Therefore, it is assumed that the turbine hall walls would rupture, and the steam and tritium would be released to the atmosphere. For a ground level release the atmospheric dispersion factor at 200 m would be $1.1 \times 10^{-2} \text{ sec/m}^3$ (Ref. 4, Fig. 2.3). Using a dose conversion factor of $1.7 \times 10^{-6} (\text{rem/yr})$ per 1 p Ci/m^3 , (Ref. 5, Table D.1), the maximum off-site dose would be 240 mrem which is about a factor of 100 below the guidelines for accidental releases given in 10CFR100.¹ While this consequence is considered acceptable, it can be reduced by engineered safeguards. As mentioned above, if the shutoff valves on the steam generators close within 30 seconds, the off-site dose would be reduced by a factor of about 50 to 4.8 mrem. Also, blowout panels could be placed in the roof of the turbine hall; this would give an elevated release of the tritiated steam which would further reduce the off-site dose. This would also reduce the damage to the turbine hall.

With regard to injury to plant personnel, this is the most serious plant malfunction. It is probable that a number of individuals would be in the Turbine Hall performing routine maintenance and checking the performance of equipment, and it is very likely that they would be killed by the steam. For this reason the radiation exposures would be of little, or no significance.

As for the damage to the plant, that would depend on the location of break and whether or not the engineered safeguards function properly. The worst case would be one in which a turbine were seriously damaged. This would probably result in the plant being off line for at least several months.

Rupture of a Steam Line in the Steam Generator Building

If a main steam line ruptures in the steam generator building, which is connected to the reactor containment, the resulting temperature and pressure in the atmosphere of the containment would be less than in the case above because of the larger volume. An approximate calculation which used only two temperatures and pressures for the H_2O (see Table 16.2) and assumed equilibrium mixing of the H_2O and air gave 80°C and

0.75 atm (11 psig). The calculation did not take into account condensation of the steam on the walls and floors, which would reduce both the temperature and pressure. Thus, it is reasonable to assume that the release would be retained within the containment. However, if the 400 Ci inventory were all released through the 100 m stack, the resulting maximum off-site dose would be 11.2 mrem under adverse weather conditions or 0.45 mrem under average weather conditions. Neither of these is excessive for accident conditions.

With regard to service hazards, the plant would undergo an emergency shutdown with the associated rapid thermal and pressure transients. However, as is current practice for nuclear power plants, the systems will be designed to withstand a certain number of such transients during the life of the plant without degradation. The most serious service consequences of this malfunction would result if personnel were in the Steam Generator Building or Duct Vault at the time it occurred. They would be seriously injured and probably killed as a result of exposure to the high temperature steam. For individuals not killed by the steam, their radiation exposures would be less than those given in Table 16.1 for restricted areas if they are exposed for less than 8 hours. Personnel in the Reactor Containment above the operating floor at the time of the incident would be injured by the high temperature (80°C) atmosphere. The extent of their injuries would depend on how promptly they escaped or were removed from that area.

Normally personnel would not be in the Steam Generator Building, Duct Vault or on the reactor operating floor. Those portions of the Reactor Building that will be routinely occupied will be isolated by pressure barriers so that personnel in them would not be exposed to the steam or tritium released in the abnormal occurrence.

The damage to the plant would be primarily in the Steam Generator Building and would depend on the exact nature and location of the pipe rupture. The worst case would be a double ended rupture, i.e., complete severance, at a location where the two ends of the pipe could whip around and damage nearby equipment. The main items in the building will be the steam generators, the helium circulators, the steam lines, the helium ducts and associated valves. The layout of the steam lines and their supports and the arrangement of equipment will be such that the damage will be confined to one steam generator system. Thus, it is very probable that after such a malfunction all that would be required would be one to two weeks of shutdown time - to determine the cause of the rupture, to investigate the extent of the damage, to perform partial decontamination and to make preliminary repairs - then the plant could be restarted and operated at about 90% of full output until the next scheduled shutdown. At that time, the damaged systems could be repaired and decontamination completed.

Fuel Injection System

1. Normal Operation

This system is described in Chapter 5. During normal operation it will not present any public hazards nor any significant service hazards. Abnormal operation will not result directly in any public hazards because of the low tritium inventory. As discussed below, it is possible that certain abnormal operations could result in damage to the reactor if the backup safety systems do not function, but as pointed out above in the section on the Reactor System no injury or exposure to the public would result.

2. Abnormal Operation

Abnormal operation of the fuel injection system does present some service hazards. In most instances the consequences would be no more serious than an emergency shutdown of the plant and a delay of restart until the cause of the malfunction could be corrected - a matter of probably several days to a week if on-hand replacement components can be used. If the backup safety systems do not work and automatically adjust the reactor operation or shut the plant down, there would be time, in most cases, for operators to take the necessary actions.

Some of the malfunctions, such as under fueling, would result in a reduction in the plasma power output. The consequences would be a reduction in the electrical power delivered, which, while undesirable, is not serious. Other malfunctions, such as limited overfueling, would result in an increase in plasma power which, if not checked by one of the multiple safety systems on the reactor and the power conversion system or by the operator action, would lead to damage to the turbine-generators and/or the reactor. The consequences of such an occurrence, which is considered incredible, would be similar to those considered above for those systems. It should be mentioned that excessive overfueling would result in "putting out" the plasma and shutting down the plant.

Primary Fuel Loop Processing System

This system and its operation are described in Chapter 14. During normal operation, the only public hazards are those associated with the relatively large inventory of tritium. These hazards will be eliminated by the use of double walled pipes and vessels and all welded connections as discussed above in the sections on the reactor, the heat transport system and the power conversion system and by locating this system within the containment building which has an atmospheric control system. In addition, most of the components of the system - except for the lines from the reactor vacuum system and to the fuel injection system - will be located in a separate sealed room below

the reactor operating floor. This room will have its own atmospheric control and cleanup system.

1. Normal Operation

The main service hazards during normal operation are likewise due to the presence of tritium. Techniques have been developed in AEC programs, such as the weapons program, for handling tritium safely, and it is expected that the results of that experience will be available to allow full use in designing and operating this system. The main components of the system, compressors, precoolers and fractionators, are in current use for similar applications. The techniques that have been and will continue to be developed for minimizing the hazards associated with their operation will be utilized in designing and operating this system.

2. Abnormal Operation

The most serious conceivable abnormal operation of the primary fuel loop processing system is one in which a major portion of the tritium inventory in the plant is released. Since the resulting consequences to the public would be very serious, this has been designated the Design Basis Accident and is discussed in that section below.

The service hazards associated with such a tritium release would be significant but probably not serious. The most serious consequence would result if personnel were in the process equipment room at the time. Unless they were seriously injured, they would have time to leave the room, through an airlock, before they received excessive doses. If the tritium escaped into the other portions of the reactor containment building, personnel who were there would have ample time to leave the building.

If the reserve fuel supply were lost in the malfunction, that is, the primary fuel processing loop and the blanket processing system were and continued to be operating normally, the plant operation could be continued without interruption during the cleanup and repair period. However, if a significant fraction of the tritium leaked into the reactor containment building, it would be wise to shut the plant down to avoid the possibility of another accident such as a helium duct rupture forcing that tritium up the stack. A rapid release under the worst weather conditions would give a maximum off-site dose of about 600 rem.

Other malfunctions in the primary fuel processing system such as rupture of one of the lines or components would have less serious public hazards associated with them because of the lower tritium inventories involved. The next most serious would be a rupture of the primary fractionator, which would contain, in liquid form, a large

fraction of the 253 grams of tritium in the main fuel loop. However, even if that total quantity were released, the consequences would be down by a factor of 12.5 from those considered above. The effect on plant operation could be more serious in that the plant would have to be shut down. Also, unless the malfunction were such that the reserve fuel supply could be used, the plant would have to remain shut down until the cleanup and repairs had been accomplished.

Blanket Processing System

This system, which is also described in Chapter 14, has the primary function of retrieving the tritium bred in the reactor breeding blanket. It also has the important secondary functions of removing impurities from the breeding material, flibe, and of improving the heat transfer from the flibe to the helium cooling tubes in the blanket (see Chapter 11). In addition, it is a part of the energy storage system used to maintain the plant power output during the periodic shutdown periods (see Chapter 18). Because of the last named function, the flibe reservoirs will be included as part of this system even though they are not a part of the basic system described in Chapter 14.

1. Normal Operation

As in the case of the systems discussed above, the public hazards and the major service hazards associated with the normal operation of this system are due to the inventory of radioactive materials in the system. However, in this case the hazards are greater because, in addition to the tritium - 0.1 MCi - there is a large inventory of other radioactive materials - 7100 MCi (see Chapter 10). The majority of those materials have very short half-lives, and others are not gaseous. From the hazards point of view, the most significant material is the F-18 with a half-life of 110 minutes and a saturated activity of 660 MCi. The method used to eliminate these hazards during normal operation is similar in principle to that used for the other systems containing tritium - double walled pipes and vessels, welded connections and location within the reactor containment building.

The non-radiological service hazards associated with the normal operation of this system will be similar to those of molten salt systems used in the chemical industry and in the Molten Salt Reactor program. The experience obtained with those systems will be used in designing and operating this system to eliminate or minimize the hazards associated with its operation.

2. Abnormal Operation

As with normal operation of this system, the most serious public and service hazards are those associated with the large inventory of radioactivity. A major

malfunction, such as the rupture of one of the main pipes or components, which released a large fraction of that inventory, would represent a serious public hazard.

The most serious accident involves a double-ended break in the piping system between the reactor and the disengagers in the Blanket Processing System (see Chapter 14). Since the disengagers continuously scavenge the tritium, the radioactive inventory downstream of them would be significantly less than upstream. If no corrective actions are taken, the flibe inventory will be pumped out of the system and release any volatile radioactive material into the reactor building. If the building were not sealed, the radioactive gas would diffuse through the walls and be carried beyond the site boundaries and result in radiological exposure to any persons in the path of the cloud.

As calculated in Chapter 10, the only volatile radioactive material in the blanket with a half-life long enough to represent a public hazard is the tritium, most probably in the form of tritium fluoride. Since the removal of this dissolved gas from the flibe is difficult (the disengagers keep the vapor pressure above the salt very low), there should be only a slow release of TF to the air. Flibe, as are many salts, is very stable and even at the temperature of 800°C will not react to any significant degree with air. Thus, there would be no fire; however, if the contact between the air in the reactor building and the flibe is not inhibited in some way, the air will be rapidly heated up by the hot flibe to a temperature close to that of the flibe, about 800°C . The resulting increase in pressure to about 2.7 atm, or 40 psig, could rupture the reactor building walls if they were not designed to withstand it. An unrealistic assumption would be that essentially all of the tritium is released in a few hours and at ground level.

The atmospheric diffusion factor for a 0-to-8-hour release at ground level is 0.011 sec/m^3 (Fig. 2.3 of Ref. 4). Assuming the whole flibe inventory of tritium, 0.1 MCi, is in the cloud, the exposure of a person downwind at the site boundary would be 1100 Ci-sec/m^3 . Using a dose conversion factor of $0.054 \text{ rem/(Ci-sec/m}^3)$ (derived from Table D.1 of Ref. 5), the dose to individuals downwind at the site boundary would be 60 rem, which is a factor of about twice the 25 rem guideline in 10CFR100. However, the building containment described in conjunction with the Design Basis Accident should reduce atmospheric releases to negligible values.

The service consequences of a flibe system rupture would be quite substantial. Plant personnel would be subjected to gamma radiation from the 660 MCi of fluorine-18 in the flibe, as well as the thermal and mechanical aspects of the accident. The reactor

building would not be seriously damaged or highly radioactive, and it would probably be a question of just cleanup and repair.

Other malfunctions of the Blanket Processing Systems, such as small leaks or equipment failures would not represent significant public hazards but could have serious service consequences. If personnel were in the immediate vicinity of a leak when it developed, they could be seriously burned by the high temperature (800°C) flibe and receive a significant radiation dose. The extent of the burns and the doses would depend on the size of the leak, how close the personnel were to it, and how rapidly they could leave, or be removed from, the area. The probability of this event occurring is very small, but the consequences could be serious enough that special precautions and procedures would be required whenever personnel entered the areas in which the blanket processing system are located. These precautions would include carefully planning any operations to be performed to minimize the time in the area, use of protective clothing and following the "buddy" system.

If the malfunction were a leak or a reduction in the flibe flow rate, the plant would have to operate at reduced power or be shut down until repairs could be made. If only one or two of the 8 parallel processing systems were involved, the malfunctioning unit(s) could be isolated and the flow through the others increased to maintain full power operation. If the malfunction were in the tritium recovery portions of the system, makeup tritium could be obtained from the fuel reserve system, which contains sufficient tritium to supply the full burnup requirements for 4 days.

Coolant Processing System

The function of this system, which is also described in Chapter 14, is to remove tritium from the helium coolant in the heat transfer system. It is located in the cold leg return from the feedwater heaters to the reactor (see Chapter 11, in particular Fig. 11.22).

1. Normal Operation

The public and service hazards associated with the normal operation of this system are due to the presence of tritium. The methods used to eliminate these hazards are similar to those described above for other tritium containing systems. Here the problem of tritium diffusion through vessel and pipe walls is greatly reduced because of the relatively low temperature of the system. Most of it is at 66°C , and the highest temperature is 260°C in a desiccator when it is being regenerated.

2. Abnormal Operation

The public hazards associated with the abnormal operation of this system will be significantly less than those of the systems considered above. This is because of the smaller size of the system, the lower flow rate of the helium through the system (about 0.005% of the total helium flow rate) and the lower inventory of tritium in the helium in the system. During the regeneration of the desiccators, tritiated steam is produced, but the quantity available for release at any given time is small.

With this system out of service, the tritium content of the main heat transport system and the power conversion system would build up. However, this would be a slow process, and it is possible that the malfunction could be corrected before it was necessary to shut down the plant because of the increased hazards associated with a malfunction of those systems.

Other Major Systems

The hazards of the other major systems will be considered only briefly because the public hazards and in most instances the service hazards associated with their normal and abnormal operation are minor.

In all critical systems, such as the cryogenic system, vacuum system and auxiliary power system, there is sufficient redundancy of major components that a malfunction of one of them, or, in the case of the first two systems, several, will not interfere with the normal operation of those systems.

The most serious malfunction of any of these systems that has been identified would be the loss of a significant portion of the liquid helium from the cryogenic system. This would result in the superconducting magnets going normal; however, this process would be slow enough that the reactor could be shut down and the stored energy in the magnets dumped into the lake in an orderly fashion. There would be a pressure buildup in the containment building because of the vaporization of the liquid helium, but this could be relieved by venting through the stack so that the pressure could be kept well below the design value. Thus, there would be no adverse consequences to the public.

The most serious service consequences would be that any personnel in the immediate area would receive frostbite injuries and/or be asphyxiated. Here again it is believed the buildup of helium in the air would be slow enough that the personnel could leave the building before the latter possibility occurred; the "buddy" system would be used to assure the evacuation of incapacitated persons. The malfunction would have to be repaired, the cryogenic system recharged and the magnet re-cooled before the plant could

be restarted. This would require several days to several months to accomplish depending on the extent of the damage and whether the magnets warmed up significantly.

Design Basis Accident

The design basis accident was selected as being the credible accident whose consequences would be the most serious to the public if the plant design features and engineered safeguards were not adequate to reduce those consequences to tolerable levels. This accident would not necessarily be the one with the most serious service consequences since it is assumed injury to plant personnel and damage to the plant are secondary to the safety of the public (this is not to imply that careful design and engineered safeguards are not used to the fullest extent to protect plant personnel and the plant).

The only hazards to the public during normal or abnormal operation of the plant are those due to the presence of radioactive materials. Thus, the design basis accident was chosen as being the one that could result in the release of the largest quantity of volatile radioactive material. As mentioned above, the system meeting this requirement is the Primary Fuel Loop, which contains 2.525 kg of tritium.

Description of the Accident and Its Consequences

The tritium inventory of the Primary Fuel Loop consists of 0.505 kg in the ash cleanup and H-D-T separator systems plus 2.020 kg in the fuel feed reserve tank. Most of the latter will be kept in the form of stable metal hydrides, but could be released by an intense fire. Since the processing equipment in the Fuel Loop is of very modest size, it could probably be contained in a room of perhaps 5 by 6 by 3 meters, constructed as a blast- and fireproof vault. All tritium reservoirs can be fitted with double walls and fast-acting valves so as to isolate any system failures.

Nevertheless, the hypothetical loss of the whole tritium inventory to this room will be considered. This would raise the pressure by only 0.1 atm and should easily be contained, unless the release should cause a hydrogen explosion (but an inert working atmosphere or quick flush should prevent this). If, however, the vault were already ruptured, the tritium would escape to the reactor building.

As described below, the reactor will be enclosed in a high quality final containment shell to prevent further spread of hazardous material. The need for this is demonstrated by considering the hypothetical loss of the tritium to the atmosphere. If it were released in a short time (0 to 8 hours) and at ground level, the dose to an individual 200 m downwind would be 15,000 rem, which is intolerable. If the release were

through a 100-meter stack, the maximum off-site dose would be reduced to 29 rem, comparable to the 10CFR100 guideline of 25 rem. Release through a stack over a longer period (which is the most probable case because the driving pressure would be small) should reduce the maximum dose to perhaps 5 rem.

Methods for Minimizing the Consequences

The public consequences of the Design Basis Accident can be greatly reduced by enclosing the reactor and its associated systems in a containment structure designed to withstand the worst expected conditions. Such containment buildings are required for all fission reactors with few exceptions. Thus, the design and construction of this type of building is a well developed engineering process. The design pressure for containments in existence and under construction are well above the requirement here; for example, the design pressure for the Connecticut Yankee Plant is 40 psi with a leakage rate of less than 0.1% by volume per day.⁷

The leakage rate is a design parameter and could be reduced below 0.1% by volume per day if necessary. However, with that leakage rate the initial tritium release rate is reduced to 18 Ci/min. For a ground level release, the atmospheric dispersion factor is 0.011 sec/m^3 at 200 m (Fig. 2.3 of Ref. 4) for 0 to 8 hrs. and 0.003 sec/m^3 for 8 to 24 hrs. Using these values and a dose conversion factor of $0.054 \text{ rem/Ci-sec/m}^3$, we obtain an integrated dose of 1.25 rem at the site boundary during the first 2 hours after the accident. This is well below the 10CFR100 guideline of 25 rem. During the first 8 hours, the integrated dose due to tritium is 5 rem; during the next 16 hours the added dose would be 2.4 rem. Thus throughout the first day following an accident, during which any affected people could surely be evacuated, the maximum expected off-site dose would 7.4 rem.

Even though it has been shown above that the addition of a containment can reduce the public consequences to tolerable levels, there are additional straightforward engineered safeguards that will reduce them still further. The atmosphere control system can be used to remove the tritium. The Connecticut Yankee atmospheric cleanup system is designed to reduce the iodine concentration by a factor of about 10 within two hours.⁷

Conclusions and Recommendations

As a result of this review of the hazards associated with the operation of this fusion power plant, it has been concluded that the plant could be operated without any significant hazards to the public or to the plant personnel during normal operation. The main hazards were due to radiation from the reactor and to tritium contained in many of the systems. The use of properly designed shielding and the leak-tight, double-walled construction for all systems containing tritium will reduce the consequences of these hazards to satisfactorily low levels - below the AEC guidelines given in 10CFR20 and 10CFR50 proposed Appendix I for light water moderated fission reactors.

For most modes of abnormal operation of the plant the hazards to the public are not significant, and by the use of engineered safeguards the consequences of the worst credible accident can be reduced to below the AEC guidelines in 10CFR100. The hazards to plant personnel are considered to be no greater than those associated with a conventional power plant or any large plant using high pressure, high temperature fluids. There are credible malfunctions that could cause serious injury or death to plant personnel, but most of those would occur in areas not normally occupied by personnel.

Although the hazards associated with the presence of tritium are acceptable, it is recommended that work be continued on developing barriers that can be applied to or incorporated in conventional materials such as stainless steel to reduce significantly the diffusion of tritium through them. It is also recommended that the public information efforts be continued and expanded to acquaint the public with the fact that there are radioactive hazards associated with fusion power.

References

¹Code of Federal Regulations, Title 10, Atomic Energy, Chapter 1 (Office of the Federal Register, Washington, D. C., January 1973 - February 1974).

²Proposed Appendix I, Docket No. RM-50-2, (U. S. Atomic Energy Commission, Washington, D. C., Feb. 20, 1974).

³W. C. Gough, unpublished.

⁴1000 MWe Central Station Power Plants - Investment Cost Study, U. S. Atomic Energy Commission Report WASH-1230, Vol. I (1972).

⁵Environmental Dose Commitment - an Application to the Nuclear Industry, EPA-520/4-73-002, (U. S. Environmental Protection Agency, Washington, D. C., 1974).

⁶W. R. Grimes and S. Cantor, Molten Salts as Blanket Fluids, The Chemistry of Fusion Reactors, D. M. Gruen, ed. (Plenum Press, New York, 1972) pp. 161-190.

⁷Facility Description and Safety Analysis, Haddam Neck Plant Topical Report No. NYO-3250-5 (Connecticut Yankee Atomic Power Co., Haddam, Conn., 1966) 2 Vols.

Chapter 17
Environmental Compatibility
P. W. Davison*

Introduction

During the past five years a number of excellent studies have been made of the environmental implications of the construction and operation of fusion power plants. References 1, 2 and 3 are several good examples. Use of the studies has been made, directly and indirectly, in the development of the assessment of the plant environmental compatibility given in this chapter.

Following the enactment of the National Environmental Policy Act, NEPA, in 1969, regulations and guidelines have been developed for evaluating the environmental impacts of many types of activities and plants, including nuclear power plants. However, it has not been considered feasible to follow these guidelines, except in a general sense, in this study because they require detailed knowledge of the environment in which the plant is located.

As a result of the study described below, it has been concluded that this plant could be constructed and operated without adverse impacts on the environment. However, if a great many plants of this type were constructed, it is probable that there would be some significant conflicts in the use of certain resources - beryllium and lithium in the flibe blanket; chromium, molybdenum and nickel in the structural materials exposed to high neutron fluxes; and, possibly, helium for use in the heat transfer system.

Use of Land

Plant Site and Exclusion Area

The plant site plan is shown in Fig. 17.1. In the present design the site size has been set by radiological safety requirements (see Chapter 16) because it has been assumed that venting of tritium will take place routinely and under certain accident conditions. A site boundary distance of 200 meters from the venting stack is needed. For a circular site boundary this would require 0.126 square kilometer or 59 acres. For a square site, the requirement would be 0.16 square kilometer or 75 acres. Both of these are smaller than many of the modern suburban shopping centers. The proposed site boundary radius is 50% greater than the 200 m figure adopted throughout this chapter (as shown in Fig. 17.1). This provides an added safety factor.

*Westinghouse Electric Corporation

744406

Fig. 17.1 Plant site plan.

Mining Operations

The mining operations required by the plant fall into two categories, those associated with providing the construction materials needed to build the plant and those required to provide the fuel and other consumable materials needed to operate the plant. In most instances the separation of materials into these two categories is clear-cut. However, in the case of the material required for the structure and cooling tubes in the breeding blanket, vacuum wall and radiation shield wall, the situation is uncertain at this time. This is because the expected lifetime of this part of the reactor cannot be established until radiation damage effects are much better understood (see Chapter 12). Aside from radiation damage effects, the design criterion has been for 30-year life, which would clearly put the materials in the construction category. However, based on the very limited radiation damage information currently available, a design life of 5 years has been selected. This lifetime is comparable to that of the fuel in today's fission power plants, and it is frequent practice in those plants to include the cost of the first fuel loading in the capital cost of the plant and that of subsequent loadings in the operating cost. In this case a similar practice will be followed. Thus, in this section only the materials required for initial construction will be considered. The material requirements for replacement of the components due to radiation damage will be considered in the sections on Other Consumables below.

The materials required for construction are listed in Table 17.1 for the major structures and components. The requirements for small components, such as valves, pumps, electrical and electronic equipment, have not been included. The material is identified as to the alloy or chemical form required. The associated mining operation requirements are indicated by the ore requirements given in Table 17.2. There the individual chemical elements are listed without identification as to structure or component. The values for the approximate average yield from the crude ore were obtained from Table III on page 20 of Ref. 3. The material and ore requirements for consumables are discussed in the section on Use of Resources below.

Waste Disposal

The use of land for the disposal of wastes from the plant is not expected to present significant problems. The non-radioactive waste disposal requirements will be comparable to those from an office-type operation with a similar size staff, about 150 people. The major consideration will be the disposal of radioactive wastes and, as discussed below, most, if not all, of these can be buried within the site boundary.

Table 17.1
Materials Required for Construction
of Reactor and Reactor Building

Structure or Component	Material	Quantity Metric Tons
Radiation Shield Wall	PE-16	31
Vacuum Wall	PE-16	94
Blanket Structure & Tubes	PE-16	965
Divertor Vessel & Tubes	PE-16	62
Divertor Vacuum Manifold	316-SS	38
Main Support Structure	316-SS	4100
Helium Ducts to S.G.'s	316-SS	3922
Containment Lining	Carbon Steel	3682
Concrete Reinforcing Bars	Carbon Steel	3900
Flibe	Flibe	6800
Superconductors	Nb ₃ Sn/Cu/SS	6800
Concrete	Concrete	32000
Helium-Cryogenic	He	81.3
Helium-Reactor Cooling	He	35.7

Table 17.2

Metal Ore Requirements

Element	Quantity Metric Tons	Approx. Average Yield from Ore % *	Ore Requirement Metric Tons
Aluminum	17.3	10	173
Beryllium	875.2	2	43,760
Chromium	1,658.2	5	33,164
Copper	3,264.	0.9	362,667
Iron	11,920.7	45	26,490
Lithium	597.7	5	11,954
Manganese	163.5		
Molybdenum	287.9	2	14,395
Nickel **	1,646.9	1	164,690
Niobium	102.	2	5,100
Tin	34.	10	340
Titanium	17.3		
Zirconium	0.6	5	1.2

* From Table III, p. 20 of Ref. 3.

** Includes cobalt as an impurity.

The disposal of gaseous and liquid wastes is discussed below in the section on Air and Water Pollution. It would be possible to convert some of these to solid form, but this is not considered necessary. Here we shall consider only the solid wastes. The major source of solid radioactive wastes is the breeding blanket and associated structures and tubes, the vacuum wall and the radiation shield wall (see Figure 11.1). The material formed in the flibe and deposited in it from corrosion will be removed on a continuous or periodic basis. This material can be disposed of when it reaches any arbitrary volume or level of radioactivity. The structural materials, on the other hand, will be removed only after they fail or reach the end of their useful lives, which will be of the order of 5 years (see Chapter 12). Thus, these two sources will be considered separately.

The radioactivity induced in the flibe and the structural materials is discussed in Chapter 10. The half-lives of all of the radioactive materials produced in the flibe itself are all short, the longest being that of 109.8 minutes for fluorine 18 and the next longest being 26.9 sec. for oxygen 19. Because of their rapid decay, the disposal of these elements should present no problems. The only long-lived materials will be those corroded from the structural materials exposed to the flibe. In test loop experiments during the Molten Salt Reactor Program the corrosion rate for stainless steel was found to be the equivalent of the removal of 1.1 mils per year or 0.00279 cm per year. This corresponds to a removal rate of 1.1 volume percent per year from the total structure. Chromium is the element that is preferentially removed, and by inhibiting the corrosion of chromium the corrosion rate is reduced by a factor of 18.⁴ Thus, it is assumed that the corrosion products contain 95% chromium and 5% other materials in proportion to their presence in the structures. Using this assumption and the composition of PE-16⁵ it is concluded that the chromium is removed at 6.3% per year and the other materials at 0.066% per year.

The activation analysis reported in Chapter 10 gives the total activity of the blanket, vacuum wall and radiation shield wall due to long-lived isotopes (half-lives greater than 30 days) as 9000 megacuries immediately following shutdown after 30 years of operation at a 75% capacity factor. Using these activities and the material removal rates given in the previous paragraph we obtain the following for activity of the material removed by the flibe during the last year of operation: from chromium, 31.4 megacuries, from the other materials, 5.6 megacuries or a total of 37.0 megacuries. Of this material the majority of the elements have half-lives less than one year, including all of the chromium (half-life, 27.8 days); 1.2 megacuries of material have half-lives longer than one year.

The decay power generated in the corrosion products is rather modest, and, of course, decreases after removal from the reactor. Thus, while cooling will be required for freshly removed material, it will not be very much. The corrosion products removed from the flibe will be mainly in the form of oxides and fluorides that have specific volumes 2 or 3 times those of the metals. Thus, the volume of waste will be approximately 3 times that of the metal removed or 45 cubic meters per year that could be stored in 24 fifty-gallon drums. If stored in this fashion, 720 drums would be required for the 30-year life of the plant. This would require a shielded vault about ten meters square by two meters high. The shielding would be about 2 meters of ordinary concrete. The amount of land used by such a storage vault would not have a noticeable impact on the environment.

The major portion of the radioactive waste will be PE-16 structural material from the radiation shield wall, vacuum wall and breeding blanket. The volume of material and its activity will depend on the lifetimes of these structures. As discussed above, these are uncertain at this time because of lack of information on radiation damage effects, and a tentative lifetime of 5 years has been selected. There is the possibility that part of these structures can be reused. Another possibility is that the material can be recycled (Ref. 3, p. 18) and reused in this or another plant. Both of these possibilities would greatly reduce the waste disposal requirements. However, here the pessimistic assumption will be made that the whole structure will have to be replaced on a five-year cycle - not all of it at one time but on a schedule such as five sectors each year for four years and four sectors after the fifth year. Since the sectors removed during the first four years would not have their allotted exposure, they could be stored and used as replacements during the last four years of operation.

In the blanket design given in Chapter 11, a 30-year life at a 75% capacity factor was assumed, and the corrosion allowance of 0.063 cm was based on this. For a five-year design life, the corrosion allowance would be one-sixth of that or 0.0105 cm, and the volume of the structural material would be reduced by $2.83 \times 10^7 \text{ cm}^3$ from $13.75 \times 10^7 \text{ cm}^3$ to $10.92 \times 10^7 \text{ cm}^3$. In addition, after five years of corrosion, the volume would be further reduced to $10.35 \times 10^7 \text{ cm}^3$. Of this material, 40% is in the form of flat plates, and the remainder is thin wall (0.079 cm or 31 mils) tubing. Also, the expected limitation on design life is embrittlement. Therefore, compaction of the material should present no serious problems other than that it would have to be done by remotely operated equipment, but that would be a relatively simple operation. Thus it is assumed that the volume of the compacted material would be no more than 20% greater than

the volume of the metal and that during the 30-year life of the plant $6.21 \times 10^8 \text{ cm}^3$ of material would have to be disposed of. As discussed below, the power developed in the material due to radioactive decay is great enough to require cooling; it is about 7 MW shortly after shutdown and 600 kW after one year for a five sector batch and about 2 kW for the total 50 years after plant closedown. Therefore, another 10% has been allowed for cooling passages to bring the total volume requirement up to $6.83 \times 10^8 \text{ cm}^3$. This would require a cubical underground vault 8.8 meters (29 feet) on a side with two meters of concrete shielding or the equivalent thickness of earth over it. Space for such a vault could be fitted into the site plan very easily. Provision would have to be made to maintain the cooling for about five years after the plant is closed down. At that time the decay power would be about 180 kW (the rate of temperature rise about 485°C per year), which could be dissipated by conduction and/or radiation to the surrounding earth.

Another category of radioactive waste that must be considered is tritium. As discussed in Chapter 16, all of the systems and components in which tritium is present will have containment systems with cold outer walls to prevent the diffusion of any significant quantities of tritium from them. In addition, except for the turbines, those systems will be located inside a secondary containment, that is, within the reactor and steam generator buildings. The atmosphere in the secondary containment will be monitored and the tritium levels maintained below the safe levels as specified in 10CFR20⁶ or the equivalent regulations in force when the plant is operated. This will be accomplished in a manner similar to that described in Chapter 14 for removing hydrogenic species from the main helium coolant system; the effluent will be in the form of tritiated water. An estimate of the activity of this effluent has not been made, but it is expected to be low compared to that of the other sources discussed below. Another low level source of tritiated water is the flibe processing system described in Chapter 14. The specific activity of this water may be high enough to warrant processing for recovery of the tritium to use as fuel as is done in the helium cleanup system. In that event, no provisions for waste disposal will be required.

The system that is used to purge the space between the inner and outer walls of components containing tritium will be a source of tritium at high levels of activity (see Chapter 15). However, the effluents will also have a high specific activity, and thus will be processed to recover the tritium.

The only source of tritium that has been identified for consideration from the radioactive waste disposal point of view is the diffusion of tritium from the helium heat

transfer system through the steam generator tube walls into the steam system. An analysis of this process, discussed in Chapter 15, concluded that the tritium leakage rate would be 6 curies per day for Incoloy-800 tubes in the steam generator or 1 curie per day if a copper barrier were built into the tubes. Because of the large volume of water in the steam system - the equivalent of 2.40×10^6 liters (based on scaling data given in Ref. 2 for the Zion nuclear plant) - it would not be worth the processing costs to recover the small quantities of tritium (0.6 mg/day). There are three methods of approaching this tritium leakage problem. One would be to make the losses from the steam system as small as possible, collect and store the leakage that did occur and then, when the plant is decommissioned, arrange for the storage or disposal of the tritiated water. The second approach would be to dilute and discharge routinely to a body of water the leakages and/or an arbitrary fraction of the steam system inventory. The third would be to release the leakage to the atmosphere by evaporation and blowdown from the plant's cooling towers or by venting steam through an elevated vent. Each of these has its advantages and disadvantages as will be pointed out below.

The storage approach has the advantage that under normal conditions the complete tritium inventory can be maintained within the site boundary with no impact on the environment. It also avoids the use of the relatively large quantities of water that would be required (see below) if the tritiated water were discharged beyond the site boundary. The disadvantages are related to the buildup of the tritium concentration in the system. At the end of one year's operation at a 75% plant capacity factor, the inventory for 6 Ci/day leakage would be 1640 Ci or 684 $\mu\text{Ci}/\ell$. At the end of 30 years' operation at a 75% capacity factor the inventory would be 24kCi or 10 m Ci/ ℓ . With the copper barrier in the steam generator tubes these quantities would be reduced by a factor of 6; however, in the discussion that follows the higher values will be used. The concentrations given above are high compared to the allowable levels given in 10CFR20-3 $\mu\text{Ci}/\ell$ in unrestricted areas and 100 $\mu\text{Ci}/\ell$ in restricted areas - but not excessively so. Care will have to be taken during maintenance of the steam system, but provision can be made for draining the system into the condensers and/or a special storage tank. The materials in the system will contain absorbed tritium, and special procedures and shielding will be needed when working on the components of the system. However, this will be true, though to a lesser extent, for the dilution and discharge approach. Another disadvantage is that after the plant is decommissioned provision will have to be made for storage or disposal of the inventory. The storage method is being considered for use in currently planned pressurized water reactor plants⁷ for quantities and concentrations similar to ours. If an allowance of 50% is made for accumulated losses,

then the storage volume required would be about 3.6 million liters or less than one million gallons, which is not an unusually large size of storage facility. However, this would have to be stored for 145 years before the concentration decayed to the level allowed by 10CFR20 for discharge to a natural body of water; the level suggested for light water reactors in the proposed Appendix I to 10CFR50⁸ is 5×10^{-2} $\mu\text{Ci}/\ell$, which would require 215 years of decay.* These decay periods are long, but it is believed that methods will be developed in the fission reactor waste disposal program that can be used for these periods of storage. The dilution approach does not appear feasible because of the large quantities of water required - 8.0×10^9 liters or 6500 acre feet to meet 10CFR20; or 4.8×10^{11} liters or 3.9×10^5 acre feet to meet proposed 10CFR50 requirements. The latter is about three percent of the average daily rainfall in all of the United States.⁹

Another matter that must be considered is the consequences of the accidental release of the tritiated water. To handle the event of release in the form of water, the plant will be designed to contain the release and provide the means for transferring it to the storage tank. Procedures will be developed for cleaning up the plant and restoring it to a safe operating condition. In the event of release as steam, it will be vented to the atmosphere. If the release is in the steam generator building, the venting will be through a 100-meter stack. The atmospheric dispersion factor at a distance of 200 m downwind for release from a 100 m stack under fumigation weather conditions is 5.2×10^{-4} sec/m^3 (Fig. 2.1 of Ref. 10). Thus, for a 24 kCi release, the tritium exposure would be $12.5 \text{ Ci sec}/\text{m}^3$. The air concentration-to-dose conversion factor for tritium is 1.7×10^{-6} $(\text{rem}/\text{yr})/(\text{pCi}/\text{m}^3)$ (Table D.1 of Ref. 11) or $0.054 \text{ rem}/(\text{Ci sec}/\text{m}^3)$. Thus, the dose to an individual at the 200 m site boundary would be 0.675 rem. For average weather conditions, the atmospheric dispersion factor for a 100 m stack has a peak value of 2.1×10^{-5} sec/m^3 at 600 m (Fig 2.2 of Ref. 10). Thus, a more realistic maximum dose to off-site personnel is 0.027 rem. Both of these dosages are well below the guideline of 25 rem whole body dose given in 10CFR100¹² for accidental releases. However, if the release of steam occurred in the turbine hall, it would probably be at ground level, and at 200 m the atmospheric dispersion factor would be 1.1×10^{-2} sec/m^3 (Fig. 2.3 of Ref. 10). The dose to personnel at that distance would be 14 rem, which is 56% of the 10CFR100 guideline. Because of the uncertainties involved in this dose calculation, engineered safeguards will be used to reduce the credible tritium release. The steam system will be designed with stop valves that under any credible steam line

* This version of the proposed Appendix I has no limitation on the concentration of tritium releases except in terms of annual doses to personnel in unrestricted areas. The value of 5 mrem per year for liquid effluents was converted to 5×10^{-2} $\mu\text{Ci}/\ell$ by using a dose conversion factor of 1×10^5 $(\text{mrem}/\text{yr})/(\mu\text{Ci}/\text{cm}^3)$ for internal exposure due to ingestion of tritiated liquids (International Commission on Radiological Protection, Committee 2).

rupture conditions would limit the release to about 25% of the total inventory. For personnel within the turbine hall, the doses would be significantly higher (of the order of ten times); however, the consequences of their exposure to the high temperature steam would probably be fatal and the radiation exposure of no consequence. Likewise, the damage to the plant would be that caused by the steam, and procedures would be used as in the water release discussed above to decontaminate the facility so that it could be repaired and placed back in operation. Neither the long storage period nor the large end-of-life inventory make this approach very attractive.

The approach of routinely diluting and disposing of the tritium leakage into the steam system has the advantages associated with lower tritium inventories. The equilibrium level of the inventory would depend on the fraction of the total steam system inventory that was discharged per day. In pressurized water reactor plants this discharge rate is of the order of 1.5% per day,² which in our plant would amount to 3.6×10^4 liters per day or 9500 gallons per day. This would require the treatment of a like quantity of raw water as makeup, but this would pose no significant problems. The tritium concentration in the steam system would build up until the amount of tritium in the discharge equaled that diffusing into the system, that is, 6 Ci/day, which gives a concentration of 167 $\mu\text{Ci}/\text{l}$. Under these conditions, the tritium inventory would be 400 Ci or less than 2% of the end-of-plant life inventory considered above. The radiological consequences of any accidental releases from the steam system would be correspondingly reduced. On the other hand the tritium concentrations are sufficiently high that the radioactive maintenance procedures necessary would not be much less stringent in their requirements than those for the no discharge case considered above.

If the continual discharge is handled by release to off-site bodies of water, considerable dilution will be required to reduce the specific activity to acceptable levels. If a once through cooling system for the condensers were used, there would be no problem. Scaling from data given in Ref. 2, the cooling requirements would be 8.3×10^9 liters per day so the tritium concentration would be 7.2×10^{-4} $\mu\text{Ci}/\text{l}$, which is well below even the 10CFR50 in the proposed Appendix I requirement of 5×10^{-2} $\mu\text{Ci}/\text{l}$. However, it is considered unrealistic to assume that many land based sites would exist at the time this plant might be built at which such large quantities of water would be available. If the requirement of 10CFR50 in the proposed Appendix I must be met, 1.2×10^8 liters per day would be required, which is a large quantity - over the life of the plant (30 years at a 75% capacity factor) 9.9×10^{11} liters would be required, which is twice that needed for diluting the end-of-life inventory discussed above. However, if

wet or wet/dry cooling towers were used, the quantity of water required for blowdown would be of the same order of magnitude - the exact value would depend on the quality of the makeup water and the atmospheric conditions at the plant site; thus, it may be possible to supplement the cooling tower blowdown with the required additional water to bring the total up to 1.2×10^8 l/day into which the discharge from the steam system is dumped. The dilution requirement when the plant is decommissioned would be 8×10^9 l. If the 10CFR20 criterion of 3 $\mu\text{Ci/l}$ were used, the dilution requirements would be 2.0×10^6 l/day or 530,000 gallons per day, which would probably be acceptable at many sites. The cooling tower blowdown requirements would probably be greater than this and thus could be used. The dilution requirement at decommissioning would be 1.3×10^8 l.

Probably the most satisfactory method of handling the tritium release would be by direct discharge to the atmosphere. One method to accomplish this would be to dump the discharge from the steam system into the condenser cooling system and allow the tritium to be carried away with the evaporative losses from the cooling towers. However, the magnitude of those losses would depend on the atmospheric conditions and whether the towers were being operated in the wet or dry mode. For wet mechanical draft cooling towers at a site with normally low relative humidity, the evaporative losses would be about 1.4×10^8 l/day and the blowdown requirements 0.7×10^8 l/day.¹³ For 6 Ci/day discharge the equilibrium concentrations would be 2.9×10^{-2} $\mu\text{Ci/l}$, and the blowdown water could be discharged without dilution. Because of the complexity of the analysis involved, the annual dose to off-site personnel due to the tritium in the cooling tower plume has not been calculated. However, it will probably be of the same order of magnitude as that calculated below for direct steam release and perfectly acceptable.

A more direct way of discharging the tritium to the atmosphere would be to vent the releases from the steam system through a 100 m high vent. For adverse weather conditions, i.e., fumigation, the atmospheric dispersion factor at 200 m downwind would be 5.2×10^{-4} sec/m³. For a release of 6 Ci/day or 69 $\mu\text{Ci/sec}$, the tritium concentration would be 3.6×10^4 pCi/m³, and the dose to an individual at that location would be 61 mrem per full power year or 46 mrem/yr for a plant capacity factor of 75%, which is about 2.3 times guidelines given in 10CFR50 proposed Appendix I. However, for average conditions the atmospheric dispersion factor has a maximum value of 2×10^{-6} sec/m³ at 600 m downwind (Fig. 2.2, 4 to 30 days, Ref. 10) and the corresponding annual dose for a plant capacity factor of 75% would be 0.18 mrem or a factor of about 100 lower than the proposed guidelines.

Use of Resources

The construction and operation of this plant will not, when considered by itself, require a major use of the resources that are expected to be available at that time. However, as pointed out in Ref. 3, in the context of a fully developed fusion power economy producing 10^7 MWe, there are expected to be some resource use conflicts.

Fuel Supply

The fuel consumed in operating the plant is deuterium and lithium, both of which are in plentiful supply with a minimum impact on the environment. Quantities of beryllium and fluorine, in the breeding blanket, will be consumed and will also be considered here even though they are not fuel in the strict sense.

The deuterium requirements will be 352 grams per day of full power operation. At a plant capacity factor of 75% and a recovery efficiency of 99% in the fuel processing system, the deuterium consumption will be 97.4 kilograms per year and a total of 2920 kilograms during the 30-year life of the plant. Neither of these will have a noticeable impact on the available supplies. A proposal has been made that the plant produce its own deuterium and consequently be largely independent of outside fuel supplies. This would require of the order of 2500 liters or 660 gallons of water per day, which is negligible in light of the other water requirements of the plant.

The daily lithium consumption will be 1.003 kg of lithium-6 and 101 grams of lithium-7. There are indications that the replacement of these materials may not be required during the life of the plant; this is because of the large initial inventory in the breeding blanket and is discussed further in Chapter 10. Nevertheless, they do represent a consumption of resources. If the lithium-6 were replaced from natural lithium (7.56% lithium-6) at a separation factor of 70% and no other use were found for the lithium-7 tailings except the small amount mentioned above, this would require about 20 kg per day of operation or 165 metric tons for 30 years operation at a 75% capacity factor. The latter represents only about 0.1% of the estimated total U. S. production in the year 2000 of 10,000 metric tons (Ref. 3, Table IV).

The beryllium consumption will be 359 grams per day or 2,950 kilograms during the 30-year life of the plant. The latter represents about 0.15% of the estimated U.S. annual production of 2,000 metric tons in the year 2000 (Ref. 3, Table IV). The fluorine consumption will be 609 grams per day or 5,000 kilograms during the 30-year life of the plant, which represents about $2.4 \times 10^{-4}\%$ of the estimated U. S. annual production of 2.2×10^6 metric tons.

Other Consumable Materials

As discussed above in the sections on Mining Operations and on Waste Disposal, it appears likely that the radiation shield wall, the vacuum wall and the structures and cooling tubes in the breeding blanket will have to be replaced every five years because of radiation damage. Thus, the materials used for these structures are considered in the category of other consumables. If it should develop that the lifetimes of these structures is significantly longer or that a significant portion of the materials can be recycled, then the use of these resources will be accordingly less than indicated in the following discussion.

The quantities of PE-16 required for these structures are given in Table 17.1. It is assumed that on the average, one fifth of those quantities are "consumed" each year, and that the expected U.S. annual production in the year 2000 will be as given in Table IV of Ref. 3. The main elements "consumed" are iron - 63 metric tons/yr. or $3.5 \times 10^{-5}\%$ of production; nickel - 102 metric tons/yr. or $2.1 \times 10^{-2}\%$ of production; chromium - 41 metric tons/yr. or $4.1 \times 10^{-3}\%$ of production; molybdenum - 9.2 metric tons/yr. or $1.2 \times 10^{-1}\%$ of production; titanium - 3.4 metric tons/yr. or $1.5 \times 10^{-4}\%$ of production; and aluminium 3.4 metric tons/yr. or $1.2 \times 10^{-4}\%$ of production. The consumption of iron, aluminum, titanium and, possibly, chromium should be acceptable. However, the consumption of nickel and molybdenum would be cause for concern and emphasize the need for developing methods of recycling those materials if the operation of a large number of plants is planned.

The other consumable materials needed to operate this plant will be mainly similar to and in quantities comparable to those required by fission nuclear reactor plants of this size at that time. The techniques that will be developed during the intervening years to conserve those materials will be applicable to this plant. One material is somewhat unique to this plant and that is the large inventory of helium. The high temperature gas-cooled fission reactors will develop techniques that are applicable to conservation of this material and will obtain experience on what loss rates should be expected. That information is not available at this time.

Structural Materials

The resources that must be used for the construction of this plant are given in Table 17.3 together with the expected U. S. annual production (Ref. 3, Table IV) and the percent of that production required. Although they are not structural materials, the helium inventory and the molten salt, flibe, for the breeding blanket have been included.

Table 17.3

Resource Requirements for Construction

Material	Requirements Metric Tons	Estimated U. S. Annual Production In Year 2000* Metric Tons	Per Cent of U. S. Annual Production %
Aluminum	17.3	30×10^6	5.8×10^{-5}
Beryllium	875.2	2,000	43.8
Carbon	125.7	1×10^5	0.1
Chromium	1,658.2	1×10^6	0.2
Copper	3,264.	6×10^6	0.05
Fluorine	5,327.1	2.2×10^6	0.2
Helium	117.0	1.2×10^4	0.98
Iron	11,920.7	180×10^6	6.6×10^{-3}
Lithium	597.7	10,000	6.0
Manganese	163.5		
Molybdenum	287.9	8×10^4	0.36
Nickel	1,646.9	5×10^5	0.3
Niobium	102.	9×10^3	1.1
Tin	34.	1.2×10^5	0.03
Titanium	17.3	2.3×10^6	7.5×10^{-4}
Zirconium	0.6		

* From Table IV, p. 24 of Ref. 3.

As indicated there, the beryllium, and possibly lithium and helium, requirements will represent significant fractions of the expected annual production of those materials.

Air and Water Pollution

The plant has been designed to minimize the air and water pollution associated with its operation. The pollution has been reduced to what are considered acceptable levels, but as indicated below thermal pollution of the air is not negligible.

Thermal Considerations

In designing the plant, the aim has been to obtain the highest thermal to electrical conversion efficiency consistent with the guidelines for the study, such as, the use of current state-of-the-art technology and use of helium coolant. As presented in Chapter 11, the net conversion efficiency is 38.3%, which means that of the total reactor output, 5305 MW of thermal power, 3275 MW is not converted to sellable electric power. Some of this power can be used for productive purposes such as heating and air conditioning in the plant. However, the bulk of it will appear as thermal pollution. It is planned to use wet-dry type cooling towers for dissipating the heat from the turbine condenser cooling water. Also a portion of the other "waste" power is consumed driving electrical motors and equipment. Therefore, essentially all of the 3275 MW ends up as thermal pollution of the air. This level of thermal pollution should cause no problems for many plant locations but must be taken into consideration in selecting the plant site - for example, for a site in or near a high population density area, this level of thermal pollution may be objectionable to the nearby residents under certain climatic conditions. The use of wet-dry cooling towers would eliminate the vapor plume that is an objectionable feature of the wet cooling towers under certain weather conditions; this would, however, result in a slight reduction in turbine performance because of the higher condenser back pressure when operating in the dry mode.¹⁴

Radioactivity

As discussed in Chapter 16 and earlier sections of this chapter the plant will be designed to eliminate the discharge of radioactive materials beyond the site boundary during normal operation of the plant. It is also planned to store the radioactive wastes within the site boundary except, possibly, for the tritiated water wastes. In the latter case, those wastes will be diluted to low enough levels of activity so that they will not be pollutants.

Chemical, Sanitary and Other Considerations

The pollution of the air and water caused by this category of potential pollutants will be essentially identical with that caused by fission power plants of a comparable size. Techniques and procedures to minimize this type of pollution are being developed. This plant will take full advantage of the state of the art in this area at the time the plant is built.

Environmental Consequences of Accidents

In the discussion of hazards associated with the operation of the plant given in Chapter 16 many of the environmental consequences of accidents were included either explicitly or implicitly. The significant consequences from the environmental point of view are the release of tritium. The consequences of a steam line rupture are discussed above in the section on Waste Disposal. As shown in Chapter 16, the rupture of one of the main helium ducts in the heat transfer system could lead to a release of 70,000 Ci of tritium with a resulting dose of 2 rem to an individual 200 m downwind under adverse (fumigation) weather conditions. This dosage is a factor of 12.5 less than the 10CFR100 guideline for accidental releases.

If the concept proposed above for on-site storage of radioactive wastes is accepted, it will not be necessary to transport to or from the site any radioactive materials except the tritium required for initial startup. It is believed that the tritium transportation can be made accident proof. Therefore it is concluded that there will be no adverse effects of transportation accidents.

Conclusion and Recommendations

As a result of the considerations presented above, it has been concluded that this plant can be constructed and operated with no significant adverse environmental impacts. However, if a large number of plants of this design were to be built there would probably be problems with regard to the availability of certain resources - namely, beryllium, chromium, lithium, molybdenum, nickel and possibly helium. Therefore, it is recommended that techniques be developed for recycling these materials. If the radiation damage resistance of the materials for the radiation shield wall, vacuum wall and breeding blanket can be significantly improved, the requirements for chromium, molybdenum and nickel will be accordingly reduced.

It has been concluded that the tritium release in the event of a steam line break is acceptable; however, it would be desirable to reduce the levels of tritium in the steam system. Thus, it is recommended that studies be continued on developing tritium barriers for use with steam generator tube materials.

References

¹F. Morley and J. W. Kennedy, Fusion Reactors and Environmental Safety, Nuclear Fusion Reactor Conference Proceedings (British Nuclear Energy Society, London, 1969) pp. 54-65.

²J. E. Draley and S. Greenberg, Some Features of the Impact of a Fusion Reactor Power Plant on the Environment, Technology of Controlled Thermonuclear Fusion Experiments and the Engineering Aspects of Fusion Reactors, E. L. Draper, Jr., ed. (National Technical Information Service, Springfield, Va., 1974) Conf. 72111, pp. 644-667.

³Fusion Power - An Assessment of Ultimate Potential, U. S. Atomic Energy Commission Report WASH-1239 (1973).

⁴Molten Salt Reactor Program Semiannual Progress Report for Period Ending August 31, 1970, Oak Ridge National Laboratory Report ORNL-4622 (1971).

⁵Nimonic Alloy PE-16, Henry Wiggin and Co., Ltd., Hereford, England, Publication 3349B (1969).

⁶Code of Federal Regulations, Title 10, Atomic Energy, Part 20, Standards for Protection against Radiation (Office of the Federal Register, Washington, D. C., 1973).

⁷G. H. Farbman, private communication.

⁸Proposed Appendix I, Concluding Statement of Position of the Regulatory Staff, Public Rulemakers Hearing on: Numerical Guides for Design Objectives and Limiting Conditions for Operation to Meet the Criterion "as Low as Practicable" for Radioactive Material in Light Water Cooled Nuclear Power Reactors, Docket No. RM-50-2, U. S. Atomic Energy Commission, Washington, D. C., February 20, 1974.

⁹Handbook for Mechanical Engineers, T. Baumeister, L. S. Marks, eds., 7th edition (McGraw-Hill Book Co., New York, 1967) pp. 6-234.

¹⁰1000 MWe Central Station Power Plants - Investment Cost Study, U. S. Atomic Energy Commission Report WASH-1230, Vol. I (1972).

¹¹Environmental Dose Commitment - An Application to the Nuclear Industry, U. S. Environmental Protection Agency Report EPA-520-73-002 (1974).

¹²Code of Federal Regulations, Title 10, Atomic Energy, Part 100, Reactor Site Criteria (Office of the Federal Register, Washington, D. C., 1973).

¹³Scaled from requirements for a 300 MWe LMFBR plant located at Hanford, Washington, J. Mangus, private communication.

¹⁴K. A. Olesen, Economics of Wet/Dry Cooling Towers Show Promise, Electrical World (December 15, 1972).

Chapter 18

Operational Procedures

K. C. Sokolosky and J. File

Introduction

In this chapter, we describe the startup and shutdown of the reactor under various conditions. The initial startup procedure is first described. This is followed by a description of operation at reduced power and the normal shutdown procedures. Short, intermediate and long term shutdowns are defined and discussed. Finally, we cover emergency shutdowns and methods of restoration of service after all shutdowns.

Emphasis is placed on those parts of the power plant, especially the nuclear island, that are unique to a fusion reactor. The conventional components such as the steam plant and electrical plant are described in more general terms.

Initial Startup

Preparation for Power Production

The first step in the initial startup procedure is to prepare all systems for power production. The subsequent sections describe this process for the various components.

Upon completion of construction and component tests, the reactor is ready to produce saleable electric power. All components are started and operated simultaneously for the first time. The initial startup procedure is described below.

Vacuum System

The vacuum system, described in Chapter 6, must be among the first components to be operated. In addition to the vacuum required in the reaction vessel, the vacuum jackets of the Dewars enclosing the superconducting coils must be leak checked and evacuated before the cooling and energizing of the magnets is attempted.

It is assumed that all vacuum leaks have been located and repaired during the component test phase. The initial pumpdown is accomplished by special forepumps. This operation is continued until the pressure in both the Dewars and the reactor vessel is about 5 torr. At this point the mercury ejector pumps and the refrigerator baffles are actuated. When the pressure has dropped to 0.1 torr, the mercury diffusion pumps and their cold traps are actuated. The evacuation continues until the pressure is in the 10^{-4} torr range. At this time the pressure is sufficiently low to operate the Dewars, but further conditioning of the reactor vessel is required.

Further preparation of the reactor vessel is accomplished by deuterium discharge cleaning. The deuterium gas is ionized by energizing the appropriate group of poloidal field coils. The discharge cleaning process will continue for a period estimated at 12 to 24 hours. Poor confinement of the plasma during the discharge cleaning is necessary so that the ions bombard the wall and, in addition, pass through the divertor throat into the exhaust chamber. This assures proper "scrubbing" of the walls.

During the discharge cleaning process the throughput will approach its normal operating value. At this time the forepumps will be replaced by the multistage compressor in the primary fuel loop that is described in Chapter 14.

Mercury is deposited on the liquid nitrogen cooled traps at the rate of 1mm/year. About once a year, therefore, at an appropriate maintenance shutdown, the traps are warmed to reevaporate the mercury.

Whenever the reactor suffers a loss of vacuum, or a breakdown of the pumping system, subsequent restarts are accomplished as described above.

Cryogenic System

Once the Dewars have been evacuated, the refrigeration system, described in Chapter 13, can be started according to manufacturers' instructions. When the Dewars and the cooling system reach a temperature of 10 K or less, cryopumping will be adequate to maintain the vacuum in the Dewars at 10^{-5} torr or less.

The magnets are cooled down to operating temperatures according to the procedure described in Chapter 13. The cooldown period is about 220 hours. After the magnets attain operating temperatures, and are ready to be energized, the two spare refrigerators are returned to their standby roles (see Chapter 13). The operating units are chosen manually. A reservoir of liquid helium is available for makeup of losses. This reservoir is replenished from the helium ash produced by the D-T reaction, or from outside sources if necessary.

Magnetic System

When the magnets achieve the operating temperature of 10 K, the magnet coils described in Chapters 8 and 13 are ready to be energized. The first coils to be energized will be the 48 toroidal field "D" coils. These are energized separately with individual power supplies as described in Chapter 13. The energizing process lasts about three hours.

The control, divertor, vertical field and ohmic heating coils, though ready to be energized, will remain on standby until initial ignition occurs. Their operation is fixed by the operating fuel cycle described in Chapter 5.

Auxiliary Preheat System

At initial startup, the temperature of three reactor components must be raised. To accomplish this, two oil-fired, auxiliary boilers, each rated at 75,000 lb 800°F and 200 psi, are actuated. Fig. 18.1 shows the auxiliary heating system and its circuits. Heat is added to the flibe tank with the helium circuit in order to melt the solid flibe whose melting point is 680°F. When melted, the flibe circulating equipment can be started. This same circuit is used to heat the blanket and shield tubes.

The second need for the auxiliary heating system is to drive one of the steam driven boiler feed pumps. This is required during cycle startup and until sufficient flow is produced in the main steam generators to maintain its own operation.

Finally, the auxiliary boiler is used to supply steam to an auxiliary steam-helium heat exchanger. The circulating helium attains a temperature of 750°F.

Blanket and Heat Transport Preheat

The flibe with flibe storage tanks, the blanket, and shield system, all of which were previously purged with helium gas, must be brought up to operating temperature prior to ignition. This is done with the auxiliary boiler described above. Flibe melts at 680°F and has a heat capacity of 0.56 Btu/lb°F. While the flibe is being raised to operating temperature, the hot helium is used to preheat the entire heat transport circuit. The tubes, pumps, heat exchangers, etc., in the first wall, blanket and shield system are all heated to a temperature above the melting point of flibe, so that when the flibe melts, it can flow throughout the system without solidifying at cold spots. The system here described assumes that at initial startup, or subsequent startups, the flibe is accumulated in the flibe storage tank.

Fuel Processing

The details of fuel processing are contained in Chapter 14 (Fuel Handling). Their application to reactor operation is the subject of this section.

Prior to ignition of the reactor approximately 0.5 kg of 40% deuterium and 60% tritium liquid mixture will be added to the primary fractionator and vaporized under conditions of total reflux. This recycling readies the fuel processing system for full

PREHEAT STARTUP

744382

Fig. 18.1 Auxiliary boiler startup scheme.

duty at the time of reactor "firing". In normal operation, the deuterium-tritium-argon liquid collecting at the bottom of the primary fractionator overflows to a storage tank where it is analyzed for composition adjustment en route to the pelletizer.

When ignition occurs and throughput plasma is to be processed, two compressors will receive the gas from the mercury diffusion pumps and discharge it at 10 atmospheres through precoolers, using 70 K helium from the cryogenic system as the cooling medium. The gas will be cooled to 80 K before it enters the primary fractionator. Since the gas will be superheated with respect to conditions within the fractionator, the argon will condense and combine with the droplets of deuterium and tritium in the returning counterflow from the condenser. The mixture of argon, deuterium and tritium will then pass to a storage tank, pelletizer and ultimately to the injector. During this process a small quantity of the through gas constituents that do not condense in the primary condenser will be drawn into the cyclic condenser (one of twenty-four used intermittently) where the hydrogen, deuterium (trace) and tritium (trace) will crystallize and be collected in a receiver. The cooling medium for the cyclic condenser will be 10 K helium. The helium (ash) portion passing through the cyclic condenser will either remain as a gas and be returned to the main helium coolant loop along with trace quantities of non-condensed tritium, deuterium and hydrogen or, after cleansing by oxidation and drying, will be added to the cryogenic helium storage tank.

The crystallized solids in the cyclic condenser, consisting of deuterium and hydrogen with trace amounts of tritium, will be collected in a receiver as liquid and processed through a secondary fractionator for removal of the hydrogen. The deuterium and tritium will be returned to the primary fractionator at 33 K as a partial makeup component to the fuel processing cycle; the hydrogen will be catalytically oxidized to water and impounded to prevent tritium escape to the environment.

Steam Cycle Startup

As for any other once-through supercritical cycle, the reference design unit will require a supplementary startup mode to condition it for high-temperature, high-pressure operation. Since our steam generators will use helically wound tubing for the heat transfer surface of the supercritical coil, no distinct separation will be noted between the so-called furnace section, primary superheater and secondary superheater. The absence of division valves between these sections will require a slightly different startup procedure than is currently practiced on boiler-type fossil units. In essence,

our design will not bypass the superheaters during startup, but instead will use the entire supercritical coil to heat the fluid before it enters the flash tank.

Basically, the supercritical cycle startup procedure will fulfill three objectives: first, it will provide for a cycle cleanup operation whereby a circulation flow will be established through the polishing system and all components of the cycle; second, it will provide for a temporary drum, referred to as the flash tank, that will allow for circulation as well as separation of steam and water during the warm-up period; and third, it will provide for a smooth transition from subcritical to supercritical operation.

With reference to Fig. 18.2, Cycle Startup, the following sequence of startup events can be expected:

1. A 30% flow (4.2 million pounds per hour) will be established at 500 psi through the polishing system, feedwater heaters, pumps, coolers, steam generators, flash tank and hotwell. The cleanup operation will continue until inlet water to the steam generators becomes less than one micromho in conductivity.

2. Preheat may be supplied to the steam generators from auxiliary boilers. The ultimate rate of water temperature increase will be set at approximately 100°F per hour.

3. When the temperature of the fluid entering the flash tank reaches 300°F at 500 psi, the high pressure circulation from the boiler feed pump through the steam generators will be increased to 3500 psi. This will be achieved by control of valve 102. Simultaneously the fluid temperature will be increased to 400°F.

4. Since the high pressure cycle fluid conditions will have increased in both temperature and pressure, it will be important to monitor continuously the conductivity. One micromho will be the upper limit.

5. Assuming favorable water conditions, the next step will be designed for heat recovery optimization in the high pressure cycle. This will be achieved by closing the 141 valves and directing flash tank drains to the deaerator via valve 130. In doing so, the heat normally lost to the condenser will be retained in the deaerator, and the overall startup time will be shortened.

6. When the flash tank pressure increases to 200 psi, valve 105 will be opened to supply warmup steam to the turbine piping. Discharge flow will pass through valve 110.

7. At this point, the flash tank pressure will be controlled at 500 psi. Excess pressure will be relieved via valve 142, and the steam will be directed to No. 1 feedwater heater for heat recovery.

8. The turbine will be rolled at 500 psi (saturated temperature) by steam from the flash tank. Valve 105 will be the control element. On a cold start it will be

744383

Fig. 18.2 Cycle startup.

customary to increase the turbine speed to 2400 rpm and then allow for a two-hour stretch-out period. During this time the flash tank pressure will be increased to 1000 psi. Valve 120 will continue to regulate flash tank overpressure into No. 1 feedwater heater; however, its new setpoint will be 1000 psi. The turbine-generator will be phased at 3600 rpm and 1000 psi, resulting in a 5% output. The turbine control valves will be wide open on full arc admission via stop valve bypass.

9. At this juncture it will be necessary to transfer from the flash tank steam supply to the once-through steam generator output. This will be achieved harmoniously when the fluid conditions at valve 102 are 780°F at 3500 psi, resulting in an enthalphy of 1193 Btu per pound. The saturated steam enthalphy from the flash tank will also be 1193 Btu per pound at operating pressure of 1000 psi. Consequently, the main steam line bypass valve 100-2 will be opened slowly to pressurize the turbine steam lines at 3500 psi with no attendant temperature excursion. Valve 100-1 will then be opened.

10. The turbine-generator load will be 25% due to the throttle pressure increase. This represents the upper load limit on turbine bypass control, and all additional loading will result from partial opening of the throttle valves in the main turbine line.

11. From this point on, the once-through cycle will support all load increases independent of the flash tank. Valves 102 and 105 will isolate the flash tank from active service.

12. Steam temperature will be adjusted to 1000°F by utilizing the temperature of the helium entering the steam generators. The throttle pressure will be adjusted by the speed of the boiler feed pump.

Power Production

When all the systems have been brought to operating temperatures and pressures, we must now begin to produce power. The subsequent sections describe this process.

Ignition

Ignition and discharge current interruption procedures must be carried out once in every 100-minute cycle due to the volt-second capacity of the control field coils. This procedure has been discussed in detail in Chapter 5 from the viewpoint of the plasma dynamics problems. Comments here will be limited to discussions of procedures that effect this cycle of operation.

The ignition sequence will begin when the base pressure in the device is measured to be equal to the required ignition pressure. In addition, spectroscopic analysis of the gas in the pump discharge line will be used to determine that the proper atomic composition (i.e., sufficiently low impurity level) has been obtained. In the event that either of these criteria cannot be met, ignition delay will be initiated and pumping continued to attempt to provide adequate ignition conditions. The maximum delay time permissible is about 100 seconds. It is determined by the maximum temperature drop tolerable in the molten salt blanket. If at this time the ignition criteria still have not been met, ignition abort procedures are initiated. This requires load tripout and the input of sufficient energy to the blanket via the helium circulators to balance the heat loss from the blanket to the containment area. In this manner the blanket temperature may be maintained to allow a diagnostic period of approximately 30 minutes in which to locate the source of difficulty. During this time, energy must be drawn from the grid or auxiliary supplies to maintain station requirements. At the end of the 30-minute diagnostic period, failure to achieve the ignition conditions requires plant shutdown, which is described below.

Under normal circumstances, the ignition sequence is neither delayed nor aborted. The first step in the ignition sequence is to fire the breakdown oscillators so that the gas charge is weakly preionized. The preionization state will be verified by spectroscopic means. Failure of the breakdown oscillators will force ignition delay and possibly even ignition abort, previously described. Five thousand megajoules of stored energy are now delivered to the poloidal coil systems, inducing a 14.6 MJ plasma discharge current in about 13 seconds. The reactor is now in the armed state, and ignition failure at this point represents a serious threat of major component damage. The transition to full power is achieved in 10 seconds through the use of 8 MW of supplementary heating power. The burning cycle continues for 97 minutes at which time interruption occurs by using fuel feed control and the vertical field coils to shut down the discharge as described in Chapter 5.

After ignition it is necessary to maintain control of the discharge for the entire 97-minute burning time. Loss of control of the discharge may result in the melting of a cooling tube in the radiation shield. The probability of such destruction upon loss of control is essentially not known at present.

The ignited discharge contains about 1 GJ of thermal energy. About 1 GJ of magnetic energy must be dissipated by the plasma if the discharge current were to be interrupted suddenly in an uncontrolled manner. If the time scale for interruption of the discharge is a second or so, and if the energy is spread uniformly over the radiation

shield, no harm to the radiation shield is anticipated since its normal thermal load is about 1 GW. A sudden deposition of 2 GJ uniformly throughout the mass of the radiation shield would result in a temperature rise of approximately 120°C. A sudden deposition of 2 GJ uniformly over the surface of the radiation shield would surely melt the exposed surface, but the full consequences of such surface melting is not clear. No more than about 10 microns of metal could be evaporated by such an event. However, non-uniform deposition of 2 GJ on time scales appreciably less than 1 second can clearly lead to extensive rupture of one or more tubes in the radiation shield. Such an event constitutes the most serious service hazard, the consequences of which are discussed in the section on rupture of a helium coolant line in Chapter 16.

In view of the above possible consequences, we adopt the position that loss of control of the discharge during the burning time will result in a reactor shutdown to be carried out automatically.

This is the equivalent of what is known as "scram" in the operation of fission power reactors. When this occurs, load dropout will be required, and energy will be drained from the blanket.

Frequently, load demand on the major producing elements of a utility are substantially less than the design load. Though detailed studies have not been made, if operation is at reduced plasma power levels, preliminary considerations indicate that the plant could be reliably operated at outputs as low as 30%. This comes from knowledge that the plasma power output is highly sensitive to plasma density, impurity and tritium content; each of these provides a strong control mechanism. There is an alternate that one may consider, and that is to provide auxiliary energy usage to absorb off-peak power and subsequently deliver it at times of peak demand.

Current Interruption

The normal operating cycle, which is described in Chapter 5, consists of a 100-minute cycle in which there is a three-minute period called the current interruption. During this interruption, the plasma current is extinguished, the vacuum vessel is cleaned, and reignition of the plasma occurs. To maintain the electrical power output of the plant uniform, thermal power is withdrawn from the flibe located in the reactor blanket and storage tanks. This results in a lower bulk temperature of the flibe. It is desired to have a constant helium temperature at the outlet of the reactor. This is accomplished by increasing the flibe flow rate as the flibe temperature drops.

In Chapter 11 it was shown that the heat transfer coefficient for the flibe varies as the 0.6 power of its velocity, a relationship that allows for reasonable compensation for the power shutdown.

During the next burning pulse the flibe temperature is returned to its former nominal value, with a corresponding return to the normal flibe flow rate. This is accomplished by operating the reactor at a thermal power slightly in excess of nominal full power.

At maximum output, the thermal energy required during the current interruption is 95.49×10^{10} joules. The power produced in the reactor during this cycle is shown in Fig. 18.3. The plasma furnishes 4.10×10^{10} joules during reignition. Also, in the interval between shutdown and reignition, the decay heat (see Chapter 10) supplies 2.45×10^{10} joules. The remainder, 79.92×10^{10} joules, must be supplied by the flibe. As a compromise between minimizing the quantity of flibe required as well as the thermal transients in the flibe system, a temperature reduction of 50°C was selected. The heat capacity for flibe is $2.35 \text{ joules/gm}^{\circ}\text{C}$ (see Ref. 7 of Chapter 11). These parameters indicate that the flibe requirements are 6800 metric tons. The blanket, the pipes and flibe processing system contain 2200 metric tons; thus an additional 4600 metric tons of flibe must be used. The additional flibe is stored in eight tanks, 7.5 m in diameter by 7.5 m high, located in the return path from the flibe processing system to the reactor.

During the shutdown and reignition period the temperature difference between the bulk flibe and the cooling tube walls will have dropped from the nominal full value of 120°C (see Table 11.7) to 70°C . In order to keep the heat flux through the cooling tube walls constant, and thus the helium outlet temperature constant, the flibe flow ratio is increased by about 2.5 times the previous rate.

The flow rate variation will be accomplished with a closed loop control system that automatically adjusts the flibe flow to maintain a constant helium outlet temperature. We restore the 79.92×10^{10} joules removed from the flibe during the cycle interruption by operating the reactor at 133 MW above nominal full power during the 97 minutes burning pulse. The thermal output of the reactor is therefore 5438 MW instead of the nominal output of 5305 MW.

Transfer of Power to Grid

When the turbine achieves the speed of 3600 rpm, phase synchronization takes place in a conventional manner. That is, the phase of the generators is synchronized with the phase in the transmission line. At this point, the breakers are closed, and

744517

Fig. 18.3 Thermal power produced in the reactor during the cycle interruption.

proper loading of the plasma and steam flow increase occurs. Load following, as described below, then takes place.

Operation at Reduced Power

When the reactor is delivering power to the grid, it will then follow load demands made by the customers. The following sections describe how a fusion power plant reacts to load variations and the minimum power demand.

Load Following

Under certain conditions, there will be large fluctuations in the load, sometimes at rates in excess of 100 MW per minute. In all eventualities the reactor and the turbine-generator system must react harmoniously.

The turbine-generator system with its peripheral subsystems is conventional. Its load following and lag characteristics are controlled by adjusting the helium and the steam flow rates so that fluctuation of steam pressure and temperature is minimized. The characteristics of the control system are generally the same as other systems of this type.

The response of the nuclear island and subsystems to variations in load is probably less difficult to achieve than in the conventional part of the plant. The plasma can be completely extinguished in about 20 seconds. Therefore a load following rate in excess of 5% per minute can easily be attained.

Though not completely explored, there are several ways, either individually or in combination, by which quick, effective load following controls can be incorporated in the design of the reactor. They include:

- a) The composition of the fuel can be changed, i.e., the deuterium to tritium ratio and the amount of argon.
- b) The rate of injection of the pellets can be changed.
- c) Since the output power varies as the fourth power of the plasma current, the power can be changed slightly by changing the current in the vertical field magnets.

Minimum Load

Ordinarily, minimum load operation occurs either from reduced load demand from the grid or from operating conditions, such as failure of one or more superconducting toroidal field coils.

It has been shown in Chapter 13 that the reactor can be kept on line with as many as eight toroidal field coils being inoperative, provided that the eight coils are approximately evenly spaced around the torus. This kind of reduced operation puts an absolute limit on the power output. Under the above conditions, for example, the reactor would be capable of producing only 1000 MWe. When operating conditions inhibit full power output, management must decide when the reactor is to be taken off the line and repaired.

On the other hand, day-to-day power fluctuations due to load demands are handled by the operators with the load following techniques described above. When the power output is between 30 and 35%, the limit of turbine operation is reached. At this point, one of three general steps can be taken. They are:

- a) The station can be taken off the grid.
- b) Part of the turbine generator plant can be shut down while the other works at higher output.
- c) The station can continue to operate at a daily high output, storing the energy in an energy storage device, to be used when the load demand becomes higher.

Shutdowns

During the operating life of a reactor, the plant will be shut down from time to time for various reasons. In most cases the shutdowns will be normal, due to maintenance schedules, minor equipment failures, or large load variations. In some cases, however, there will be emergency shutdowns by "scram" conditions or major equipment failure. Shutdown procedures are described below.

Normal Shutdown

Normal shutdowns are broken down into three classes:

- a) Short term shutdown; a shutdown of 12 hours duration, or less.
- b) Intermediate term shutdown; a shutdown of more than 12 hours but less than about 3 days duration.
- c) Long term shutdown; a shutdown of more than 3 days.

Short Term Shutdown

In a short term shutdown, we attempt to keep all systems, except the system that requires repair or maintenance, at operating pressures and temperatures. The

refrigerators continue to operate, and all toroidal field magnets remain energized. The plasma ignition and the fuel systems are kept on standby awaiting a restart.

When restarting from a short term shutdown, one begins from the power production point and continues with the startup procedures at an accelerated rate.

Intermediate Term Shutdown

In an intermediate term shutdown, we accomplish repair and maintenance of those components that need the complete shutdown of the steam plant. It takes approximately 3 days for the equipment to cool sufficiently for work. The refrigeration system continues to operate, and the toroidal field coils are not de-energized unless it is deemed necessary to do so. Unaffected components are kept in a warm condition, if they can be properly isolated.

When a restart is required, those systems that have been maintained or repaired will be started according to the initial startup instructions. Those components that have been kept at operating temperatures are restarted according to the power production instructions.

Long Term Shutdown

Any interruption longer than 3 days would be a complete shutdown of all systems. The only exception might be the refrigeration system. If no repair or maintenance of that system were contemplated, and if the shutdown would be less than 30 days, then the refrigeration system should maintain all magnet coils at operating temperature (10 K), even though the magnets are de-energized.

When the reactor is to be restarted, the procedure used is that described in the initial startup sections.

Emergency Shutdown

The power plant will automatically shut down if any one of a number of emergency conditions occurs. The shutdown may be triggered by a failure of either the conventional part of the plant, or of the nuclear island. The consequences will be the same, plant shutdown, though the sequence of events may be different.

If a failure of any of the steam generating plant, the turbine plant or the generating plant causes one of those systems to shut down, then all systems affected will be shut down and the plasma extinguished. The flibe system will either be shut down or maintained at operating temperatures by the auxiliary boilers depending on how long the

shutdown will last. The same is true of the magnet system, the vacuum system, and the ignition system.

If a significant failure of part of the nuclear island takes place, then the conventional part of the plant will be tripped out in a conventional manner, and, if required, the affected parts will be taken out of service.

If the flibe system is affected, the salt is returned to the storage tank and allowed to freeze. If the magnets or cryogenic systems fail, the magnets are de-energized as described in Chapter 13.

The control system provides a proper sequence of plant shutdown automatically when any one of the faulty devices trigger the sequence.

The length of the shutdown will depend on the emergency. Restart procedures will depend on the length of shutdown as described above.

Chapter 19

Maintenance, Repair and Overhaul

T. A. Coultas*

Introduction

This chapter will outline some of the maintenance criteria and the repair and overhaul situations that are envisioned to occur with the tokamak reactor. The design status of the reactor is not such that it is possible to write detailed instructions and maintenance manuals for each, or even any, component of the plant. Instead, some of the maintenance problems will be indicated and how maintainability has been designed into the reactor. Some of the more obvious maintenance associated with conventional systems will be related as well as some of the procedures to be used to assure adequate reliability of the plant and determine possible maintenance difficulties. The maintenance goals of this preliminary design are to develop the maintainability criteria, identify the foreseeable significant maintenance actions, tools and facilities that must be included in the design.

This predominantly base-load plant will have as its design goal a 200,000 hour life and an availability factor of 85%. These goals will be met by quality-control techniques applied through all stages, from preliminary design to construction and operation. These standards, controls, etc., are discussed in Ref. 1. Other standards in preparation and which may be applicable to this reactor are referenced there.

Segmentation

For maintenance purposes the reactor has been divided into 24 toroidal segments. Each segment will have two toroidal magnets. By segmenting the reactor, malfunctions occurring can be located by diagnostic techniques and only that portion removed and replaced with an entirely new segment. Thus, the reactor downtime will be considerably reduced compared to dismantling the complete reactor. In this segmentation scheme, some of the problems that needed to be considered were: the final closure of the fluid systems and vacuum wall, segment removal, moving magnet coil segments sufficiently out of the way to remove a segment, and shield segmenting to prevent radiation streaming through the opening. Each of these features will be discussed later.

Diverter

The diverter manifold is located outside the blanket and shield and is accessible for contact maintenance when no power is being produced. Material enters the

*Argonne National Laboratory

divertor exhaust chamber as neutral gas atoms with energies of less than 1 eV. No radiation or surface bombardment effects are expected. The only dangers envisioned are those radiation hazards due to tritium gas that has permeated the metal walls and remains within the metal volume during maintenance operations. These radiation levels have not yet been determined. The divertor walls and pumping ports are flanged to reduce the time necessary for removal and replacement.

Superconducting Magnets

The major components that are unusual for a power plant are the superconducting magnets. Superconducting magnets operated to date have shown very high reliability. Once they are placed into operation and satisfactorily brought up to their maximum field strength, no further difficulty is usually experienced with the superconducting magnets. The only problem occurring has been vacuum jacket leaks.

The main toroidal field coils are designed to be dynamically stable in operation, i.e., should a small portion of the superconducting material suddenly become a normal conductor, there will be sufficient current flow path through the copper stabilizing material that the temperature of that local area will not rise above the superconductor critical temperature. Specifically, the current density has been held to an average value of 4530 amps/cm² (including the stabilizing material). A temporary heating condition may be caused in large superconducting magnets due to frictional heat generated by coil slippage with respect to adjacent coils. Slippage may be caused by material stretching under stress of the magnetic pressure, and usually occurs only during startup of the magnet. However, even imperceptible creep in the material may be sufficient to cause intermittent, sudden movement of one conductor relative to its neighbors. The frictional heat generated must be cooled by the helium at that location to prevent the magnet from normalizing. The change in stored energy due to a single coil transition to the normal state is about 4×10^9 joules. Fatal damage to a coil could result if the energy were deposited in a small volume. Superconducting magnets now operating at PPL include thick copper rings to dissipate this energy.

There are 48 of these coils, and it can be shown that if three coils are removed from service (unless they happen to be adjacent coils), the reactor can continue operation, albeit at a reduced level, (at roughly 75% power level) until it is required to shut down the reactor for another reason. This redundancy, or ability to operate in spite of a major component malfunction, is an important part of the design. Repairs should be made within a reasonable time, however, as a coil outage may decrease divertor lifetime.

In addition to the main toroidal field coils, there are 56 vertical field and control coils. These coils will also be superconducting, but the peak field strength in these coils is much lower, and the total stored energy is about 5% that in the toroidal coils.

Since the control magnets do encircle the reactor in this way, each time a segment is changed, some coils must be moved. The coils between the main plasma chamber and the divertor must be disconnected to remove a segment. At this time it is anticipated that a cryogenic "quick disconnect" will be used in each of these coils. The disconnect does not need to be superconducting, but could be cryo-resistive such that the heat losses at this point would not seriously increase the refrigeration required to the remainder of the coils. Although such a quick disconnect has not, to our knowledge, been developed, it is certainly possible in principle, particularly if one is willing to allow a considerable joule heating loss and thus refrigeration load at this point.

Conventional Systems

Other components of the reactor system are more or less conventional and will not be discussed at great length. Many of these conventional components, however, are operating in an unfriendly and unusual environment. For instance, nearly all components will be subjected to traces of tritium and some, such as the vacuum pumps and chemical processing plants, will be deluged with tritium. Another unusual circumstance is the high magnetic field in the reactor building.

This may have unusual effects upon the operation of some conventional components. Tools in the reactor compartment must be made of nonmagnetic materials. Beryllium copper hand tools, for example, are now used in the cryogenic laboratory at PPL when working near energized magnet coils. The large superconducting magnet for NAL (National Accelerator Laboratory) has recently been placed into operation.² It is about 20 feet in diameter, has a 30 kG maximum field and stored energy of 0.4 GJ. Thus it is similar to one of the D magnets. Experience there has shown that the field increase outside the magnet is so gradually produced as to give workmen sufficient warning that they are carrying magnetic materials and should not approach the magnet closer with that object.

The steam and helium systems and the power generating systems are state of the art. The flibe and tritium system are designed as conventional and reliable, low velocity, high area systems. The hydrogen isotope system is a small chemical plant and should offer no unusual operational maintenance or repair problems aside from the tritium handling.

The refrigeration plant furnishes liquid helium to the magnets. It is a closed system that has been designed with 29% redundancy for initial cooldown and other extra refrigeration needs. See Chapter 13 for further details. Leakage of 8.4 m^3 per day of helium is restored from the 10.2 m^3 per day of production from the D-T reaction and the blanket. The remainder of the helium is stored for future use.

Vacuum Pumps

Mercury is a good choice for the pump fluid. Attempts to measure hydrogen concentrations due to hydride formation or hydrogen solubility in the mercury have failed because of the immeasurably small quantities. Thus the tritium holdup in the pump fluid will be nil. On the other hand, impurities and amalgams in the mercury coming from either the vacuum pump itself or from the reactor will probably be such that they will quickly saturate with deuterium and tritium. Because of this, it will be necessary to change mercury in the diffusion pumps somewhat more often than current standard practice with pumps pumping normal air. The contaminated mercury from these pumps can be easily purified by a variety of techniques to reclaim the mercury. If this is not done on site, fluid changes may be done even more often to maintain a very low radioactivity level in the spent mercury. It should be kept in mind that mercury vapor is toxic, and concentrations in breathing air must be maintained at low levels and continuously monitored.

Power Supplies

Power supplies for initiating the superconducting magnets for the toroidal fields are standard laboratory power supplies, and no exceptional maintenance requirements are expected. For the control and vertical field magnets, however, the power supplies must be capable of both charging the field of the magnet and discharging the field into either the power grid or into an energy storage device such as rotating machinery or cryogenic energy storage banks. At this time it appears that this power cannot be drained into the power grid during field changes. Draining power for scheduled shutdowns will be done at a considerably lower rate than during the field changes, and thus the same power supplies can be used.

These devices, although quite large, will not require unusual maintenance services. In addition to the power supplies there will be provision for an emergency power dump from the coils in case of loss of coolant or other malfunction of the magnet. This is a simple water bath resistor that dumps the magnetic energy into the water bath,

heating and evaporating some of the water. See Ref. 2 for the resistor design used for the NAL bubble chamber magnet.

Another component used in an unusual fashion is the energy storage device that stores the energy taken from the control and vertical magnetic field at the time of field interruption. This device can be an ordinary dc motor generator with a large flywheel (see Chapter 5). Maintenance problems with this machine will be only slightly different from those of conventional dc machines, because of continually accelerating and decelerating, thus causing some slightly unusual dynamic balancing problems.

Routine Maintenance and Overhaul

An unusual operating procedure of this reactor is the requirement to interrupt the plasma current. At this time it is expected that this current interruption will require approximately three minutes every hundred minutes of reactor operation. It has been shown that such a shutdown will be easily picked up by the power plant. While operating at full load, the heat exchangers and the heat storage in the flibe blanket itself will allow the steam power plant to continue operation during this field change without severely limiting the power requirements (see Chapter 5). This is an unusual requirement and will need to be factored into the maintenance and overhaul schedules. It is possible that minor maintenance operations can be scheduled during some of these shutdowns each hour.

Most routine maintenance requirements are no different from those of other power plants of this size. Many of these operations are discussed in an excellent reference by Morrow.³

In addition to these routine operations of conventional equipment, one needs to consider the more special equipment present in this plant. This includes the vacuum pumps, cryostats, the power supplies, fuel injection system, the isotope separation plant, the tritium removal and storage, and the lithium and deuterium receiving facilities. (See Table 19.1.) Special safety precautions need to be taken for relief valve servicing, crane and other lifting equipment, inspection, radiation instruments and, finally, the emptying, processing and disposal of waste materials. These materials will accumulate in the cold trap of the "flibe doctor" where the flibe composition is controlled.

Access Requirements

Access to equipment outside the reactor vessel is not limited by space requirements on the power plant and servicing equipment. In the reactor room, however,

Table 19.1. Routine Component Maintenance/Inspection

Component/Action	Frequency	Duration
1. Vacuum pump/change oil change bearings and seals	60 days	1 hr.
2. Power supplies/change breakers	2 yrs.	1 day
3. Liquifier/bearings and seals	2 yrs.	1 wk.
4. Cold trap-Flibe doctor/replace	6 mos.	1 wk.
5. Helium Compressor/inspect	2 yrs.	1 wk.
6. Pumps (Flibe, H ₂ O)/inspect	2 yrs.	1 wk.
7. Crane/test	1 yr.	1 day
8. Thermocouples/calibrate 1. wall 2. flibe 3. divertor wall 4. divertor collector		
9. Relief valves/calibrate(replace)	1 yr.	1 wk.
10. Magnetic heat leak/monitor	daily	
11. Radiation Monitoring/calibrate(replace). Instruments	6 mos.	1 day
12. Plasma Instruments/replace 1. current 2. temperature 3. density	6 mos.	2 days
13. Breaker/exercise 1. operational 2. emergency 3. redundant	weekly	minutes

access to certain locations is quite limited. Maintenance requirements in the vessel will generally be limited to those on the superconducting magnet vacuum jackets and the diffusion pumps on the divertor. Other maintenance required in this room will generally require segment removal. The vacuum pumps and external magnet coils are generally accessible from the outside of the torus. The vertical field windings that are within the shielding material can be reached only by removal of a portion of the reactor's first shield. The shield is segmented to expedite this removal but does require a reactor shutdown to perform maintenance work within the shield. Since the first shield reduces neutron and gamma radiation to a dose rate of 3690 mrem/hr, no access to the reactor room is possible with the reactor operating. When no power is produced the dose is reduced to 27 mrem/hr, and contact maintenance can be done. It is possible, however, that limited maintenance on the pumps may be performed with a short term reactor shutdown, not an easy operation.

In the case of the main toroidal D-shaped magnets, access is quite limited on the inner part of the torus. Maintenance required in that area may require removal of a segment and removal of the D-magnet to gain access to that inner part of the D-magnet. The same is generally true of those control magnets on the inside of the torus. Should excessive problems develop, remote maintenance devices could be used, but that is not believed to be necessary.

Instruments and Inspections

The instruments used outside the reactor to monitor plant operation are quite conventional and no further discussion is required. Computer monitoring of these instruments, such as in modern steam plants, should minimize the operational staff of the plant. However, several unusual (in the power plant sense) instruments will be used to monitor reactor operation. Some of those are listed in Table 19.2. This is by no means a comprehensive list of the required instrumentation, but it is indicative of the instruments required to alert plant operators that corrective operations are required should any of these instruments give readings outside their normal operating range. Many instruments will be located in high magnetic fields and must be designed accordingly.

Major Repair

The most probable malfunction (see Table 19.3) of the plant is believed to be a leak of some sort. This may be cause for replacement of a segment of the vacuum torus. Other possible structural and material failures are discussed in Chapter 9.

Table 19.2. Instruments

<u>Item</u>		<u>Number</u>
Reactor Flibe Temperature	IN	24
	OUT	
Blanket Helium Temperature	IN	12
	OUT	12
Radiation Wall Helium Temperature	IN	12
	OUT	12
Vacuum Wall Helium Temperature	IN	12
	OUT	12
Shield Coolant Temperature	IN	24
	OUT	24
Helium Pressure		12
Helium Flow		12
Field Measurements (D-Coils)		48
Field Measurements (Control)		200
Plasma Current		2
Plasma Temperature		4
Plasma Density		4
Fuel Composition, Flow		4
Ash Composition, Flow		12
Divertor Pressure		240
Diffusion Pump Pressure		389
Radiation Measurements		72
Blanket Temperatures		480
Divertor Temperatures		144
LH Temperature from Coils		240

Table 19.3. Lifetime Goals

Item	Goal	Most Probable Failure Mode/Cure
1. Segments	20 yrs.	leaks/replace
2. Vacuum pump, Roughing	5 yrs. 10 yrs.	Dirt/clean; heater/replace bearings seals/overhaul, replace
3. Liquifier	50 yrs.	bearings seals/overhaul
4. Power Supplies	5 yrs.	overheated component/replace
5. Mech. Eng. Storage	20 yrs.	bearings/replace
6. Flibe Doctor	50 yrs.	leak/patch
7. Helium de-humidifier System	20 yrs.	leak/patch
8. Divertor	20 yrs.	wall erosion/replace
9. Superconducting Magnets	30 yrs.	vacuum jacket leak/repair
10. Superconducting Disconnects	10 uses	faulty contact/repair

Segment Change

It is not a minor repair to replace one of the 24 segments. Very careful consideration is required on whether or not to replace the segment. Replacement analysis shall include review of equipment histories, life expectancy, and anticipated future operating and maintenance requirements. Analyses shall consider replacement needs due to (1) deterioration which prohibits meeting accuracy, capacity, safety, or reliability requirements, and (2) excessive maintenance and operating cost.

It is necessary to decide well ahead of time what constitutes need for changing a segment. Such minor malfunctions as a slight excessive heat leak into a magnet cryostat, a flibe drip from the blanket, or helium bubble through the blanket, or even an excessive vacuum leak, should not be cause of immediate shutdown and segment change. Allowable leak rates in each particular circuit must be prespecified, and if the rate exceeds that value, the segment will be replaced. A vacuum leak into the plasma chamber so severe as to prevent reignition of the burn is an obvious cause for repair. A smaller leak, which delays the attainment of full reactor power, or requires that the argon concentration in the fuel be reduced, may be tolerable. Marginal cases of this nature must be predetermined. It must be remembered, of course, that a helium or flibe leak is also a tritium leak.

Leak Detection

If it is determined that the leak rate is excessive, a difficult problem of pinpointing that leak remains. It will not be obvious which of the 24 segments is leaking. This will be particularly difficult to determine if several segments have slight but allowable leaks. Much of the instrumentation is for leak detection purposes. Thermocouples may be the most effective means of finding macro-leaks of helium or flibe. Vacuum leaks will probably need spectroscopic, mass spectroscopic, or radio-isotopic techniques to determine their location.

Some of the operations envisioned during a change of segment of the reactor are discussed here. When the ailing segment is located, auxiliary shielding will be brought into place to isolate the working area from other parts of the reactor. The outer shielding around the segment can now be removed to lighten the segment. The toroidal magnets around the segment, the adjoining segments, and all superconducting magnet coils encircling the reactor will be de-energized. This energy can be placed directly into the power grid. It should not be necessary to remove LHe from the main coils, but the one to be broken will be de-refrigerated. The quick disconnect on the

encircling coils will be brought up to room temperature and the coil disconnected so that it no longer encloses the segment to be replaced. After the magnet coils have been removed from the segment, the fluid system lining will be cut. The vacuum enclosure will be opened by breaking the weld between segments after the reactor has been purged with helium. Helium and flibe manifolds will be severed after the flibe has been drained from the offending segment into a heated reservoir. Helium from three segments must be drained into a reservoir, since three segments are interconnected through two segments on one inlet manifold and a different two segments on the helium outlet manifold (see Chapter 13).

The segment is now ready for removal, and special structural members will be required to replace those taken away with the magnets. The shield, as well as the blanket, will be removed. The shield (with appropriate end plugs) will form a "coffin" for transporting the old radioactive segment to the hot cell space for convenient and safe storage. During normal operation these coffin plugs and other bulky jigs and fixtures will be stored in the reactor room.

Replacing a segment into the toroid will require some sophisticated, remote control equipment. Many of the operations are straightforward and can be done with adequate access and minimum radioactivity exposure. These operations include: setting the segment in place; welding the high pressure helium coolant manifolds, making the mechanical connection to the low pressure flibe manifolds, replacing the shielding and instrumentation, and moving the D-magnets back in place. Since the alignment is not critical, errors of up to 1/4" seem allowable. Two other operations, however, are more difficult. One has been discussed previously, the control magnet disconnect. It will be necessary to re-connect and check continuity prior to cooling.

Replacement of Vacuum Closure

The vacuum closure on each segment is done by welding outside the shield. The 4.5 foot wide flanges extending from the plasma/divertor chamber are made in this fashion for the purpose of replaceability of segments and to place the weld in a lower level radiation environment (see Chapter 12). Most of this weld can be made without remote operating equipment since it should not require long welding times and is made outside the biological shield. Furthermore, the weld is easily accessible and visible for dye penetrant, ultra-sonic, mass-spectrometer leak checking and X-raying. On the inside of the torus, however, the weld is more difficult.

In this area, access is so limited that special remote welding and inspection gear will be required. The weld will be made in a space about 6" x 8" and about 50 feet

long. The apparatus to make this weld and inspection will be trolley mounted and travel along a track. Just ahead of the welder, a fixture will gather the two steel sheets and position them for welding. This fixture will be passed through the space at least once prior to attempting to weld. It must be ascertained that the segment closure sheets are properly sized and located. This remote inspection must precede the welding of the helium, flibe, etc., ducts, since if proper fit is not attained, the segment must be removed and modified to fit. When fit is assured, the trolley can pass again, this time making the closure weld. Inspection tools will subsequently be passed through the space on similar trolleys. As an example, the trolley mounted welding fixture used to close the vacuum vessel of the ATC experimental tokamak at PPL is shown in Fig. 19.1.

Segment Repair Shop

When a new segment is installed, the old segment is left. The disposition of the segment requires careful consideration. The decision to repair or dispose and salvage certain parts, e.g., helium piping, must be made. The radiation decay schedule, how long the segment must be held in the shielded room before it is safe to be handled (perhaps) with remote operating equipment, needs to be evaluated. If repair is opted, the segment must be cleaned, and the debris (radioactive corrosion products) found in the flibe and the helium must be disposed of in a preplanned manner. Finally, the old segment must be rebuilt, reassembled, and quality-tested for use as a replacement.

Unusual Maintenance Problems

Most of the problems in keeping the plant operational are expected to come from relatively standard components such as pumps, compressors, generators, etc. The unusual aspects of the maintenance of this plant compared to fossil fuel power plants involves the ubiquitous tritium, some radiation from radioactive portions of the blanket, the abundant use of cryogenic superconducting magnets and some access problems. Nuclear fission plants currently operate under considerably more strenuous radiation and limited-access conditions than will be present in this reactor. Experience in operation with large superconducting cryogenic magnets exists at ANL, NAL and at BNL.⁴ No maintenance problems have been experienced although special procedures (control of ferrous materials entering the room) are required. Sustained, reliable operation appears quite feasible after a short adjustment period.

Assumptions and Unresolved Problems

As might be expected in a study of this type, assumptions have been made that may turn out to be incorrect. Most involve the operation of the plasma cycle. The

724085

Fig. 19.1 A.T.C. Welding torch and trolley, used for welding the final closure of the vacuum chamber.

currently envisioned current interruption period appears feasible (see Chapter 5), but the requirements on the field changes in the magnets and energy dumping into an energy sink with subsequent retrieval are strenuous. This procedure has been assumed to be workable without causing other problems that could increase maintenance problems in turn. For instance, it is possible that during the current interruption the divertor will become slightly maladjusted, allowing high-energy particles to impinge upon either the vacuum wall or the divertor wall. This could result in excessive erosion, causing wall changes to be required more frequently than the present assumptions.

Perhaps the assumption requiring most innovation and development is that of a cryogenic disconnect for the encircling control magnets. This, or a design innovation (not out of the question), is definitely required to allow practical disassembly of the reactor. Several alternatives have been considered, but the most satisfactory is believed to be the cryogenic disconnect. In the present design it is assumed that there are two separations per turn of the magnet coil, but more may be required, or perhaps one would do. Without this development it does appear that disassembly of the reactor would be a most tedious, perhaps several months long, job.

Practicality, Cost

From the preliminary conceptual design maintenance viewpoint it appears that the maintenance requirements of this reactor will be similar, or more simple than those of a conventional power plant. Many of the components, e.g., the power plant, are identical, and others, e.g., the segment-handling equipment, are similar. If anything, it appears that the access and major overhaul procedures are slightly more simple in the case of the fusion reactor. The extra maintenance problems caused by tritium and cryogenic magnets would appear to about balance the radiation hazard and the tedious fuel-handling procedures in the fission plant or the stack-grate-ash removal problems of a coal plant.

References

¹Quality Assurance Program Requirements for Nuclear Power Plants, The American Society of Mechanical Engineers, New York, ANSI N45.2-1971 (1972).

²J. R. Purcell and H. Desportes, The NAL Bubble Chamber, Proceedings of the 1972 Superconductivity Conference (Institute of Electrical and Electronics Engineers, New York, 1972) pp. 246-249.

³L. C. Morrow, Maintenance Engineering Handbook, 2nd edition, (McGraw Hill Book Co., New York, 1957).

⁴J. R. Purcell, private communication.

Chapter 20
Cost Analysis
J. File

Introduction

This chapter gives the cost estimate of the Princeton Reference Design Fusion Reactor. Costs are given in 1974 dollars. We choose as a format the Atomic Energy Commission Guide for Economic Evaluation of Nuclear Reactor Plant Designs,¹ used by the power industry estimators to present cost analyses of nuclear power plants. Where necessary, adjustments described below are made to fit the particular needs of a fusion reactor. Table 20.1 gives the major accounts taken from Ref. 1.

Capital Costs

The Plant

The capital cost of this 2030 MWe fusion power plant, excluding interest during construction is \$1,004,320,000, resulting in a unit cost of \$494.70/kW. The total capital cost, if one includes interest on the investment during construction, is \$1,215,230,000, resulting in a unit cost of \$598.60/kW. Table 20.2 itemizes the costs. Figure 20.1 is a plot of capital cost/kW (no escalation is imposed) of operating nuclear power plants as a function of the chronological time that it first started operation. The data are obtained from a Federal Power Commission report² and the 18th steam station cost survey given in Electrical World,³ and they represent both publicly and privately owned plants. Our fusion reactor was plotted in January 1974. Though the capital cost of a fusion reactor is somewhat higher than many of the earlier plants, which were built with less inflated dollars, it will be seen below that the cost of operation is quite competitive with both nuclear and fossil fuel plants.

Accounts 20 and 21, Land, Land Rights, Structures and Site Facilities, are estimated from data obtained in Refs. 2 and 3 on modern operating power plants, as well as from data obtained in a detailed analysis of Volume I, A Pressurized Water Reactor Plant of 1000 MWe.⁴ We compare the needs of our reactor and make reasonable allowances for the larger size of the plant to arrive at estimates. Allowances for inflation are made at the rate of 6% per year since 1971.

Accounts 22 and 23, Reactor Plant and Turbine Plant Equipment, contain the nuclear island, the boilers, the cryogenic equipment, the turbine-generators, etc. Most of the items in these two accounts have been estimated from actual costs of materials and

744284

Fig. 20.1. Cost per kilowatt of installed capacity for all commercial nuclear plants operating in the United States prior to 1972.

Table 20.1

Major Account Numbers and Titles from Reference 1

<u>Account No.</u>	<u>Account Title</u>
20	LAND AND LAND RIGHTS
	201 Land and Privilege Acquisition
	202 Relocation of Buildings, Utilities, Highways, and Other Services
21	STRUCTURES AND SITE FACILITIES
	211 Site Improvements and Facilities
	212 Reactor Building
	213 Turbine Building
	214 Intake and Discharge Structures
	215 Reactor Auxiliaries Building
	216 Radioactive Waste Building
	217 Fuel Storage Building
	218 Miscellaneous Buildings
	219 Stacks (when separable from buildings)
22	REACTOR PLANT EQUIPMENT
	221 Nuclear Island
	222 Main Heat Transfer and Transport Systems
	223 Safeguards Cooling Systems
	224 Radioactive Waste Treatment and Disposal
	225 Nuclear Fuel Handling and Storage Systems
	226 Other Reactor Plant Equipment
	227 Instrumentation and Control
	228 Fossil-Fueled Boilers and Superheaters
	229 Irradiation Facilities
23	TURBINE PLANT EQUIPMENT
	231 Turbine-Generators
	232 Heat Rejection Systems
	233 Condensing Systems
	234 Feed-Heating System
	235 Other Turbine Plant Equipment
	236 Instrumentation and Control
24	ELECTRIC PLANT EQUIPMENT
	241 Switchgear
	242 Station Service Equipment
	243 Switchboards
	244 Protective Equipment
	245 Electrical Structures and Wiring Containers
	246 Power and Control Wiring
25	MISCELLANEOUS PLANT EQUIPMENT
	251 Transportation and Lifting Equipment
	252 Air and Water Service Systems
	253 Communications Equipment
	254 Furnishings and Fixtures
91	CONSTRUCTION FACILITIES, EQUIPMENT, AND SERVICES
	910 Engineering Construction and Field Supervision
	911 Temporary Facilities
	912 Construction Equipment
	913 Construction Services

92		ENGINEERING SERVICES
	921	Reactor Engineering
	922	Plant Engineering
93		OTHER COSTS
	931	Taxes and Insurance
	932	Staff Training and Plant Start-Up
	933	Owners G. and A.
94		INTEREST DURING CONSTRUCTION
	941	Physical Plant and Associated Indirect Costs
	942	Land and Land Rights
	943	Special Materials

Table 20.2

Cost Estimate for the Princeton Reference Design

<u>Account No.</u>	<u>Amount (Thousands of Dollars)</u>	<u>Amount (Thousands of Dollars)</u>
20 Land and Land Rights		
201 Land and Privilege Acquisition	1 000	
Total - Land and Land Rights		<u>1 000</u>
21 Structures and Site Facilities		
211 Site Improvements	1 500	
212 Reactor Building	76 000	
213 Turbine Room and Steam Generator	4 310	
214 Intake and Discharge Structures	4 290	
215 Reactor Auxiliary Building	4 710	
217 Fuel Storage Building	1 840	
218 Miscellaneous Buildings	7 610	
Total - Structures and Site Facilities		<u>100 260</u>
22 Reactor Plant Equipment		
221 Nuclear Island	403 050	
222 Main Heat Transfer and Transport Equipment	177 690	
225 Deuterium and Tritium Handling System and Inventory	7 900	
226 Other Plant Equipment	6 600	
227 Instruments and Controls	10 600	
Total - Reactor Plant Equipment		<u>605 840</u>
23 Turbine Plant Equipment		
231 Turbine Generator Equipment	59 590	
232 Heat Rejection System	10 500	
233 Condensing System	15 700	
234 Feed Heating System	17 700	
235 Other Turbine Plant Equipment	16 300	
236 Instruments and Controls	5 000	
Total - Turbine Plant Equipment		<u>124 790</u>
24 Electric Plant Equipment		
241 Switchgear	2 800	
242 Station Service Equipment	10 100	
243 Switchboards	1 220	
244 Protective Equipment	570	
245 Electrical Structures	3 500	
246 Power and Control Wiring	13 700	
Total - Electric Plant Equipment		<u>31 890</u>
25 Miscellaneous Plant Equipment		
251 Transportation and Lifting Equipment	5 800	
252 Air and Water Service System	5 800	
253 Communication Equipment	300	
254 Furnishings and Fixtures	4 300	
Total - Miscellaneous Plant Equipment		<u>16 200</u>

91	Construction Facilities, Equipment and Services	<u>61 300</u>
92	Engineering Services	<u>33 000</u>
93	Other Costs	<u>30 040</u>
	TOTAL CAPITAL COST OF REACTOR AND FACILITIES (Not including Account 94)	<u>1 004 320</u>
94	Interest During Construction	<u>210 910</u>
	TOTAL CAPITAL COST OF REACTOR AND FACILITIES (Including Account 94)	<u><u>1 215 230</u></u>

equipment as well as from actual quotations from manufacturers (see Acknowledgments). Other items were estimated with suitable adjustments from Ref. 4. The cost estimate for the Nuclear Island, Account 221, the part of the fusion reactor that differs from other nuclear reactors, is described in detail below.

Accounts 24 and 25, Electric and Miscellaneous Plant Equipment, are estimated either from actual requirements, by manufacturers or from Ref. 4, with suitable adjustments.

Accounts 91 through 94 inclusive are known as the Indirect Cost Accounts. The first three are estimated using guides in Refs. 1, 2 and 4 suitably adjusted for our reactor and for inflation since 1971. Account 94, Interest During Construction, reflects the interest that must be paid for the construction funds. This cost, therefore, varies with the time of construction and period required to construct the plant.⁵ In this study, we have taken construction time to be an effective three years at a yearly percentage rate of 7%; i.e., 21% for the cost of capital during construction.

Cost Analysis Format

Except for very minor instances, the changes required to Ref. 1 to fit the needs of this reactor are made in Account 221, which we choose to call the Nuclear Island. The sub-accounts, listed in Table 20.3, were deemed necessary to describe the parts of the Nuclear Island. Modifications to the items listed in Table 20.3 can be made as the need for more expansion of detail arises.

Superconductor Cost

In a study made for the Atomic Energy Commission, Powell⁶ predicted the availability as well as the cost of superconductors when they will be required for reactors, some two or three decades from now. We have used this study to estimate the costs of our superconducting magnets.

The Nuclear Island

The total cost of the Nuclear Island is estimated to be \$403,050,000, resulting in a unit cost of \$198.55/kW. The Nuclear Island was designed using present known technology, materials and manufacturing processes. Experience gained in construction of previous plasma physics research devices at Princeton University, as well as experience by others in the fast breeder reactor technology, was relied upon heavily for engineering decisions made in this study. The details of the cost estimate are given in Table 20.4.

Table 20.3

Suggested Itemization of the Nuclear Island AccountAccount Number 221

221 Nuclear Island

221.1 Reactor Supports and Foundation

221.11 Main Supports and Foundations
 221.12 Coil Supports
 221.13 Vacuum Vessel Supports
 221.14 Shipping Equipment

221.2 Magnet Systems

221.21 Toroidal Field Magnet
 221.211 Conductor
 221.212 Coil Forms
 221.213 Other Materials
 221.214 Coil Winding

221.22 Divertor Magnet
 221.221 Conductor
 221.222 Coil Forms
 221.223 Other Materials
 221.224 Coil Winding

221.23 Vertical Field Magnets
 221.231 Conductor
 221.232 Coil Forms
 221.233 Other Materials
 221.234 Coil Winding

221.24 Control Field Magnets
 221.241 Conductor
 221.242 Coil Forms
 221.243 Other Materials
 221.244 Coil Winding

221.25 Other Magnets
 221.251 Conductor
 221.252 Coil Forms
 221.253 Other Materials
 221.254 Coil Winding

221.26 Magnet Protection System
 221.261 Toroidal Field Magnet
 221.2611 Resistors
 221.2612 Power Supplies
 221.2613 Other Materials
 221.262 Divertor Magnets
 221.2621 Resistors
 221.2622 Power Supplies
 221.2623 Other Materials
 221.263 Vertical Field Magnets
 221.2631 Resistors
 221.2632 Power Supplies
 221.2633 Other Material
 221.264 Control Field Magnets
 221.2641 Resistors
 221.2642 Power Supplies
 221.2643 Other Material
 221.265 Other Magnets
 221.2651 Resistors
 221.2652 Power Supplies
 221.2653 Other Material

221.27	Magnet Energizing Power Supplies
221.271	Toroidal Field Magnets
221.272	Divertor Magnets
221.273	Vertical Field Magnets
221.274	Control Field Magnets
221.275	Other Magnets
221.3	Dewars and Refrigerator System
221.31	Toroidal Field Dewars
221.311	Steel
221.312	Insulation
221.313	Supports
221.314	Special Hardware
221.315	Filling Equipment
221.32	Divertor Dewars
221.321	Steel
221.322	Insulation
221.323	Supports
221.324	Special Hardware
221.325	Filling Equipment
221.33	Vertical Field Dewars
221.331	Steel
221.332	Insulation
221.333	Supports
221.334	Special Hardware
221.335	Filling Equipment
221.34	Control Field Dewar
221.341	Steel
221.342	Insulation
221.343	Supports
221.344	Special Hardware
221.345	Filling Equipment
221.35	Other Dewars
221.351	Steel
221.352	Insulation
221.353	Supports
221.354	Special Hardware
221.355	Filling Equipment
221.36	Refrigeration Equipment
221.361	Foundations and Supports
221.362	Refrigerators
221.363	Piping
221.364	Insulation
221.365	Storage and Handling Equipment
221.366	Purifying Equipment
221.367	Other Material and Equipment
221.4	Helium Inventory
221.41	Cryogenic Helium
221.411	Initial Inventory
221.412	Shipping Equipment
221.413	Make-up Equipment
221.414	Storage and Handling Equipment
221.415	Separation Equipment
221.42	Other Helium
221.421	Initial Inventory
221.422	Shipping Equipment
221.423	Make-up Equipment
221.424	Storage and Handling Equipment
221.425	Separation Equipment

221.5 Divertor and Vacuum Vessel

- 221.51 Radiation Shield
 - 221.511 Radiation Shield Wall and Supports
 - 221.512 Cooling Tubes and Manifold
 - 221.513 Tube Insulation
 - 221.514 Other Material
- 221.52 Vacuum Wall and Supports
 - 221.521 Vacuum Wall and Supports
 - 221.522 Vacuum Tube Handling and Testing Equipment
- 221.53 Vacuum Pumps
 - 221.531 Pumps and Piping for Vacuum Vessel
 - 221.532 Pumps and Piping for Divertor
 - 221.533 Miscellaneous Equipment
- 221.54 Divertor
 - 221.541 Divertor Wall and Supports
 - 221.542 Cooling Tubes and Manifold
 - 221.543 Tube Insulation
 - 221.544 Other Material

221.6 Shields, Blankets and Cooling

- 221.61 Breeding Blanket
 - 221.611 Blanket Support
 - 221.612 Coolant (Flibe)
 - 221.613 Coolant Handling Equipment
 - 221.614 Other Material
- 221.62 Shielding Blanket
 - 221.621 Blanket Support
 - 221.622 Blanket Material
 - 221.623 Handling Materials
 - 221.624 Other Material
- 221.63 Other Biological Shields
 - 221.631 Shield Support
 - 221.632 Shield Material
 - 221.633 Handling Materials
 - 221.634 Other Material
- 221.64 Blanket Cooling System
 - 221.641 Coolant Piping, Manifolds and Insulation
 - 221.642 Cooling Tubes, Manifolds and Insulation
 - 221.643 Cooling Tubes, Manifolds and Insulation

221.7 Fuel Injection System

- 221.71 Injection Guns and Supports
- 221.72 Power Supplies and Supports

Table 20.4

Estimated Cost of the Nuclear Island

<u>Item</u>		<u>Estimated Costs (Thousands of Dollars)</u>
221	Nuclear Island	
221.1	Reactor Supports and Foundations	
221.11	Main Supports and Foundations	5 000
221.12	Coil Supports	27 000
221.13	Vacuum Vessel Supports	1 000
221.14	Shipping Equipment	1 000
Total	221.1 Account	<u>34 000</u>
221.2	Magnet Systems	
221.21	Toroidal Field Magnet	61 800
221.22	Divertor Magnets	2 630
221.23	Vertical Field Magnets	6 600
221.24	Control Field Magnets	2 140
221.26	Magnet Protection System	4 500
221.27	Power Supplies for Magnets and Protection Devices	33 600
Total	221.2 Account	<u>111 270</u>
221.3	Dewar and Refrigeration System	
221.31	Toroidal Field Dewars	13 750
221.32	Divertor Field Dewars	1 170
221.33	Vertical Field Dewars	2 990
221.34	Control Field Dewars	1 720
221.36	Refrigeration Equipment and Piping and Insulation	63 000
Total	221.3 Account	<u>82 630</u>
221.4	Helium Inventory	
221.41	Cryogenic Helium	1 900
221.42	Reactor Cooling Helium	430
Total	221.4 Account	<u>2 330</u>
221.5	Divertor and Vacuum Vessel	
221.51	First Wall	4 500
221.52	Vacuum Wall and Supports	8 940
221.53	Pumps	2 100
221.54	Divertor	14 500
Total	221.5 Account	<u>30 040</u>
221.6	Shields, Blankets and Cooling	
221.61	Breeding Blanket	28 100
221.62	Shielding Blanket	18 630
221.63	Other Biological Shields	750
221.64	Blanket Cooling Systems	85 300
Total	221.6 Account	<u>132 780</u>
221.7	Fuel Injection System	<u>10 000</u>
TOTAL NUCLEAR ISLAND, 221 ACCOUNT		<u>403 050</u>

The Cost of Power

The estimated cost of energy at the busbar for the Princeton Fusion Reactor is 15.2 mills/kWh. The components of the total, which are itemized in Table 20.5, are the following:

a) The cost of return of investment (based on 15% return and 85% availability) is 12.1 mills/kWh.

b) The cost of operation including wages of all personnel, maintenance, replacement and repairs are taken from the averages of the privately owned nuclear plants reported in Refs. 2 and 3. In addition, we have included the maintenance, repair and replacement costs of features unique to a fusion reactor such as the first wall, the cryogenic system, etc. The cost of operation is estimated to be 3.1 mills/kWh.

c) The cost for the tritium handling equipment, initial tritium inventory and the initial lithium inventory is covered in items 221 and 225 of Table 20.2. Tritium used in the D-T reactor is essentially free, inasmuch as the tritium breeding ratio is greater than 1. The only fuel cost, therefore, is that of the deuterium being consumed in the D-T reaction. That cost is only \$300,000 per year or less than 0.02 mill/kWh.

Table 20.6 compares the estimated cost of energy at the busbar of the Princeton Fusion Reactor to that of 16 nuclear plants now in operation in the United States, as obtained from Refs. 2 and 3. We have included an average inflation rate of 6% per year since 1971 when comparing the data to the January 1974 estimate of our fusion reactor.

Table 20.7 makes the same cost comparison between the fusion reactor and all plants (coal, oil, gas, combination and nuclear) of 1000 MWe or greater reported in Ref. 3. The same inflation provision as above is made.

We note that the fuel cost for a fusion reactor is vanishingly small when compared to all other fuels. On the other hand, because of the intricacy of the nuclear island, the capital cost is higher than for other kinds of plants. The cost of labor, repair, replacement and maintenance is comparable to the other plants. Accordingly, it is clearly shown by Tables 20.6 and 20.7 that for a fusion reactor the cost of energy at the busbar is favorably comparable to present day plants. Further, as the cost of fossil and nuclear fuels (see Fig. 20.2 for late data on costs of fossil fuels) spiral upwards at rates higher than the average inflation rate, the cost of energy at the busbar will become even more attractive for fusion reactors.

Table 20.5

Cost of Energy at the Busbar

	Cost Mills/kWh	
Cost of Return on Investment (15% Return and 85% duty factor)		<u>12.05</u>
Cost of Operation and Maintenance		
Steam & Electric Operation Expenses	0.50	
Maintenance of Steam, Boiler, Electric & Nuclear Island Plants	1.00	
First Wall Replacement (5 yr. interval)	1.03	
Engineering and Supervision of Operations and Maintenance	.57	
Total Cost of Operation & Maintenance		<u>3.10</u>
Cost of Fuel	.02	<u>.02</u>
Total Cost of Energy at the busbar		<u>15.17</u>

Table 20.6

Comparison of Princeton Fusion Reactor With Operating Nuclear Plants*

	<u>Capital Cost</u> <u>\$/kW</u>	<u>Capital Cost</u> <u>Mills/kWh</u>	<u>Operating Cost</u> <u>Less Fuel</u> <u>Mills/kWh</u>	<u>Fuel Cost</u> <u>Mills/kWh</u>	<u>Total Cost</u> <u>at Busbar</u> <u>Mills/kWh</u>
Indian Point	466	18.51	3.81	4.58	26.90
Peach Bottom	237	9.40	9.94	1.92	21.26
Humboldt Bay	380	12.02	3.27	4.19	19.48
Monticello	185	15.62	1.42	1.96	19.00
Princeton Reference Design	599	12.05	3.10	0.02	15.17
Big Rock Pt.	194	7.02	4.05	3.67	14.74
9 Mile Point	256	9.97	1.11	2.67	13.75
Dresden	121	9.07	0.98	2.02	12.07
R. E. Ginna	161	5.44	1.91	2.53	9.88
H. B. Robinson	101	5.69	0.94	2.41	9.04
Rowe	218	4.94	1.44	2.37	8.75
Millstone	146	4.77	1.07	2.21	8.05
San Onofre	190	4.58	0.86	2.32	7.76
Oyster Creek	167	4.28	0.96	1.98	7.22
Conn. Yankee	156	3.95	0.92	1.76	6.63
Point Beach	141	4.02	0.47	1.86	6.35

* Data from Refs. 2 and 3.

Table 20.7

Comparison of Princeton Fusion Reactor
With All Plants of 1000 MWe or Larger Placed on Line During 1972*

	Capital Cost \$/kW	Capital Cost Mills/kWh	Operating Cost Less Fuel Mills/kWh	Fuel Cost Mills/kWh	Total Cost at Busbar Mills/kWh
Princeton Reference Design	598.6	12.05	3.10	0.02	15.17
659 (Coal)	185.7	8.78	1.12	3.17	13.07
661 (Coal)	129.9	4.17	1.42	4.40	9.99
664 (Coal)	138.6	5.44	0.76	3.63	9.83
670 (Oil)	161.7	4.17	0.73	4.79	9.69
671 (Oil + Coal)	169.3	4.59	0.50	4.49	9.58
666 (Coal)	101.9	3.01	2.65	3.72	9.38
660 (Coal)	152.3	4.84	0.48	3.63	8.95
662 (Nuclear)	122.0	4.42	0.67	3.26	8.35
663 (Nuclear)	134.0	4.28	1.11	2.96	8.35
672 (Coal)	145.9	4.52	0.65	2.78	7.95
665 (Coal)	144.2	3.53	0.41	3.94	7.88

* Data from Ref. 3

DATA OBTAINED FROM REFERENCE 3

744495

Fig. 20.2 The cost of fossil fuels as a function of time. Costs were rising dramatically from 1970 to 1972 and are not available beyond that time. Thermal efficiency of 34% is assured.

Acknowledgments

Many private sources were used to estimate the cost of this proposed reactor. Of these, several industrial firms expended many hours of effort to render services and information (at no charge to Princeton) without which this cost analysis could not be possible. For this valuable aid we thank the following firms and individuals.

Messrs. J. R. Kalalion, O. J. Ogden, R. B. Williamson, and J. R. Bushey of the Large Turbine Division, Westinghouse Power Systems Co.

Messrs. R. O. Barratt, J. Polcer, W. T. Klamm, M. D. Bernstein, R. Costello and R. deFlesco, of the Foster Wheeler Corporation, Nuclear Department, Livingston, New Jersey, for cost evaluation of the helium heated steam generator.

Mr. Gordon Coombs, Cryogenics Technology, Inc., for cost analysis and preliminary design of a 280 kW, 10 K, helium refrigeration system.

Mr. G. White, Babcock and Wilcox Co., New York, N. Y., for help in preliminary design of the steam generator.

Dr. G. R. Hopkins of The General Atomic Co., San Diego, California, for help with the design of the helium heat transfer system and for cost estimates for the helium circulators.

Mr. Joseph L. Nock of the Allegheny Ludlum Steel Corporation, Bala-Cynwyd, Pa., (District Sales Offices) for price information of stainless steel sheet shapes, tubes and other forms.

References

- ¹Guide for Economic Evaluation of Nuclear Reactor Plant Design, (National Technical Information Center, Springfield, Va., 1969) NUS-531.
- ²U. S. Federal Power Commission, Steam Electric Plant Construction Cost and Annual Production Expenses, Supplement 24-1971 (Superintendent of Documents, Washington, D. C., 1973) pp. 160-163.
- ³18th Steam Station Cost Survey, Electrical World 180, 39 (1973).
- ⁴1000 MWe Central Station Power Plant - Investment Cost Study, U. S. Atomic Energy Commission Report WASH-1230, Vol. 1, Tables Nos. IV and V (1972).
- ⁵L. Geller, J. F. Hogerton, Jr., and S. M. Stoller, Analyzing Power, Nucleonics 22, 64 (1964).
- ⁶J. R. Powell, Design and Economics of Large DC Fusion Magnets, Proceedings of 1972 Applied Superconducting Conference (Institute of Electrical and Electronics Engineers, New York, 1972) p. 346.

Chapter 21
Uses for the Reference Design
R. G. Mills

The Reference Design as a Standard

There is a great tendency for people preparing proposals for future devices, or descriptions of future systems, to spend a disproportionately large fraction of their efforts on those aspects of the project that are well understood. As a result the parts that can be accomplished well may receive far more discussion than the new or novel features that require research and development in order to be accomplished at all. In an extreme case one might find a thorough description of adequate provisions for every aspect of a project except for one insoluble problem for which no one has a solution. The fusion reactor, for example, could founder if high temperature plasmas cannot be confined or if a useful lifetime of the first vacuum wall cannot be achieved in the presence of a strong neutron flux. In the preceding chapters we have attempted to describe every problem associated with a first-generation fusion power plant, with a clear identification of those problems for which adequate knowledge for a solution is not yet available. We have viewed this task not as an end in itself but rather as a necessary first step in a larger and more important task of examining how much freedom exists within the multiple parameter range illustrated by our reference design model. A major use of this study will be to provide a point of departure from which to gauge the effect of any proposed change in machine parameters. From the dozens of possibilities, we shall briefly describe just seven as examples of those needing attention.

The most fundamental problem in research programs related to controlled thermonuclear fusion remains today exactly what it has been for twenty years, the lack of knowledge of the transport properties of thermonuclear plasmas. The need for containing them for study to attain this information conflicts with the lack of knowledge of the transport properties needed to design an adequate confinement device. The transport properties predicted theoretically (the diffusion coefficient and the thermal conductivity are the most important) differed for years from those observed in laboratory devices, not only in magnitude, but also in their functional dependence on the plasma parameters (density, temperature, and magnetic field). Recent success in bringing theory and experiment closer together is the source of much optimism, but experiment suggests that as plasma energy density increases, there is a tendency for the confinement to depart from "neoclassical"¹ or "pseudoclassical"² behavior in which

transport rates decrease as the temperature increases by going into a "Bohm-like" state where transport increases with temperature. Currently, a popular theoretical explanation is the "trapped-ion instability." See, for example, Kadomtsev and Pogutse.³ However much work remains before we can say that theory and experiment really agree. Clearly until it is established whether or not this transitional behavior is to be expected, and at what plasma conditions it takes place, assumptions must be made about the transport properties. For this report constant values (i.e., independent of temperature and density) were assumed. An important question is to explore the implications of varying this assumption. This is one of the highest priority problems that can be done using this model as a first approximation.

In a pressure-(beta-) limited magnetic confinement device, the fusion power density will vary as the fourth power of the confining magnetic field. It is rather obvious that this means a reactor using a field that is too small will be of no economic interest. The converse, viz. "the higher the magnetic field the better" is not so obvious. Problems of structural support and cost increase with field level, and there may be an optimum. The sensitivity of the fusion power plant to assumed field levels needs to be studied.

Should radiation damage prove to be a controlling economic factor, advantage can be gained by designing for prompt change of inner parts with design emphasis on low cost parts and especially rapid replacement procedures to avoid serious reduction in the plant's utility factor. It would be of value to know if it is realistic to assume frequent (six months) replacement and whether recycling of the vacuum wall can be done economically.

The present design has attempted elimination of as many problems as possible. For example, to avoid the complication of MHD effects in moving circulating fluids, helium has been chosen as the coolant, and the flibe circulated only rapidly enough for chemical processing. Since circulating helium requires an enormous amount of power, there is a potentially large increase in efficiency obtainable by eliminating the helium loop, increasing the rate of circulation of the flibe, and making use of it as a heat transfer medium.

Although the breeding characteristics of the blanket design presented here seem very adequate (with a tritium doubling time far shorter than really necessary), the calculation is based on cross-section data that are not of sufficiently high accuracy to give full confidence in the result. Especially suspect are the cross sections for fluorine, where the accuracy is probably not better than 20%. Should its neutron

capture be unexpectedly high, it might be reason to load the blanket with more beryllium or possibly some metallic lithium, thereby complicating the tritium recovery procedures; studies of such contingency designs should be carried out, starting with a small sensitivity analysis to variations of the fluorine cross sections. Efforts should be made to reduce the beryllium content from the point of view of resource conservation. The present design calls for an uncomfortably large beryllium inventory.

It is conventional wisdom that the D-T reactor will be the first to be developed. However this assumption may be seriously questioned. There are important advantages to other fuel cycles, the so-called catalyzed deuterium reactor for example,⁴ and they should not be ignored. The increase in size that is implied in such an alternate fuel mixture might be just what is required should unfavorable results develop in the two major fields of uncertainty: confinement and radiation damage.

It is implicit in the model described in this report that the diffusion coefficients will be substantially higher than those predicted by current theory (neoclassical and pseudoclassical). Although twenty years of research have been hampered by devices that did not provide long enough confinement times, it is not out of the question that devices of the large sizes needed for reactors may tend to provide confinement times that are too long for simple continuous operation at the equilibrium condition.⁵ Although it is generally accepted that some sort of spoiling technique should be readily available, it could turn out that cyclic operation provides the most convenient arrangement for a reactor. Such a model should be explored.

The previous seven items represent variations on the theme developed in this report. It is also necessary to probe more deeply into some of the topics developed in the preceding chapters. A few examples follow.

The question of injection needs experimental study in the high energy density plasmas that have only recently become available in the laboratory. Incorporation of magnetic field effects into the present theory of the injection process⁶ is a complicated and controversial topic that needs some data to build on.

Divertor design is very intricate and has been a cause of much delay in preparing this report. Some doubt still remains concerning the accuracy of the model finally selected even granting the transport property hypothesis on which this model is based. An enlarged effort, both theoretical and experimental, is called for.

As indicated in chapter seven, ohmic heating alone can suffice for ignition only with rather favorable results from confinement studies. Neutral injection is probably satisfactory as an auxiliary heating means, but if compression is required, an

enlarged vacuum chamber will probably be necessary to accommodate a significant increase in temperature by this method.^{7,8}

Other areas that require more theoretical and experimental study include questions of the deposition of the kinetic energy of the alpha particles born in the D-T fusion reactions in the plasma and in the wall, optimization of the neutronics, and tritium management system details.

Programmatic Implications

The value of conceptual studies such as this one is a subject of debate. Some people believe them to have little, if any, value. Some believe them to be misleading, causing people to believe the research program is further along than it really is. The "let's build it and see if it works" attitude might be stimulated by taking this work too seriously. Others dismiss conceptual reactor studies as entirely premature technically and describe them as public relations work.

The authors of this report do not believe that it describes accurately a first generation reactor; rather they view this as an exercise to identify the various problems that must be solved to make fusion reactors possible and evaluate the relative difficulty and importance of each problem. In this way the primary value is believed to be for use as a tool to aid in shaping the research and development program that must be completed before thermonuclear power can be a reality.

What sort of machine should be chosen for conceptual study? One encounters a diversity of opinions on this subject. A few desire to show how an ultimate, high-efficiency, third or fourth generation power plant could meet all the conflicting needs of society. Others recommend ignoring the impossible-to-predict remote future and simply considering an experimental fusion fuel burner; it is close enough ahead in time to be visualized with some hope for accuracy. However in order to fulfill the role of identifying the important problems, we have felt a first generation commercial power plant is the best choice since it requires every element of a practical system (whereas experimental reactors do not), yet avoids certain interesting auxiliaries (e.g., direct conversion or other topping devices) that are not really necessary to make fusion a success.

Certainly the machine described in this report, if built, would not function as described. However it is almost unique among conceptual designs in that we do not know today how it would differ in performance from the preceding description. A number of difficult problems have been identified during the writing of this report, and further research required to investigate the solutions of these problems is summarized in the final chapter of this book.

References

- ¹M. N. Rosenbluth, R. D. Hazeltine, and F. L. Hinton, Plasma Transport in Toroidal Confinement Systems, Phys. Fluids 15, 116 (1972).
- ²S. Yoshikawa, Turbulent Diffusion Coefficients of Toroidal Plasmas, Princeton Plasma Physics Report MATT-959 (1973).
- ³B. B. Kadomtsev and O. P. Pogutse, Trapped Particles in Toroidal Magnetic Systems, Nuclear Fusion 11, 67 (1971).
- ⁴R. G. Mills, Catalyzed Deuterium Fusion Reactors, Princeton Plasma Physics Laboratory Report TM-259 (1971).
- ⁵R. G. Mills, Time Dependent Behavior of Fusion Reactors, Nuclear Fusion Reactor Conference Proceedings (British Nuclear Energy Society, London, 1969) pp. 322-336.
- ⁶S. L. Gralnick, Solid Deuterium Evaporation in a Fusion Plasma, Nuclear Fusion 13, 703 (1973).
- ⁷H. P. Furth and S. Yoshikawa, Adiabatic Compression of Tokamak Discharges, Phys. Fluids 13, 2593 (1970).
- ⁸R. G. Mills, Ignition of Toroidal Fusion Reactors, Proceedings of the 5th Intersociety Energy Conversion Engineering Conference (American Chemical Society, Washington, D. C., 1970) Vol. 1, pp. 1-8 - 1-12.

Chapter 22
Needs for Further Research
R. G. Mills

Major Assumptions Adopted in This Work

Plasma Confinement

Modern theory of toroidal plasma confinement^{1,2,3} predicts times more than adequate for purposes of reactors of the size contemplated in this report. Experiment⁴ has shown reasonable agreement with these theories, but there is some evidence^{5,6} that at higher plasma energy densities a transition to Bohm-like behavior occurs, perhaps caused by trapped-ion instabilities.¹ No theoretical or empirical data have been used to justify the diffusion coefficient selected in our model. It is roughly two orders of magnitude larger than banana diffusion theory predicts, and two and one-half orders of magnitude smaller than classical Bohm diffusion. It has been taken as constant over the aperture.

This procedure is equivalent to assuming that a transition to faster-than-theoretical diffusion will occur before the full thermonuclear region is achieved by some as yet unexplained process, perhaps a breakdown into a turbulent mixture of convective cells in such a manner that a net average diffusion coefficient is active over the entire aperture. The assumption is also implied that this diffusion coefficient will fortunately be exactly what is desired or that some undescribed method will be available to adjust the diffusion rate to that desired.

Ignition

It may be possible for ohmic heating alone to bring a toroidally-confined deuterium-tritium plasma to the ignition point, although at present the method seems marginal. The ideal ignition temperature for equal densities of deuterium and tritium is only 4.2 keV, but in practice it is to be expected that considerably higher temperatures will be necessary. The ideal case balances bremsstrahlung losses to alpha energy released within the plasma.⁷ Addition of ohmic heating input depresses the ignition temperature and may allow ohmic heating to carry the plasma to self-sustaining conditions, especially at high current densities and low plasma densities,⁸ but reducing the ion density slows the process and requires a longer time to achieve ignition. Incorporating other loss processes (thermal conductivity, particle losses, and even synchrotron radiation) further increases the actual ignition temperature. The

difficulty of achieving ignition by ohmic heating alone has been appreciated since the very beginning of CTR research,⁹ and a variety of supplementary heating methods has been proposed, originally magnetic pumping,⁹ and more recently adiabatic compression.^{10,11} With the exception of microwave heating and possibly hybrid resonance heating for which the radio frequency power may be introduced through wave guides, the various rf schemes (e.g., magnetic pumping and ion cyclotron resonance) seem to imply the need for coils within the vacuum chamber. This would require insulation for high voltage, a problem for which adequate solutions do not appear to be at hand.

Recently neutral beam heating has been successfully demonstrated in the laboratory.¹² Since ohmic heating can come at least very close to ignition, it has been assumed in this report that the provision of a relatively small amount of neutral beam auxiliary heating will suffice to ignite the plasma.

Injection

Experiment and theory are in a rudimentary state when it comes to predicting the behavior of a proposed system to inject fuel pellets into a high density reactor grade plasma.¹³ Elementary considerations of the energy necessary to vaporize and ionize the incoming fuel compared to the energy available from the plasma indicate that it is not a simple task to transport a significant amount of fuel into the center of a reactor's plasma.

The nominal radius of the plasma visualized for the reactor described in this report is 3.2 meters, at first glance a vast distance through which to project a particle. However the combination of the assumed parabolic density distribution with the toroidal shift of the magnetic surface towards the outside results in an effective distance over which one-half of the pellet may be lost of only about 50 cm. Studies done so far¹³ indicate that without allowing for any beneficial effects of the magnetic field, such penetrations can be accomplished.

We therefore assume that the magnetic axis can be reached by the remains of the pellet, and the net equivalent source function distributes the fuel uniformly over the cross section.

Divertor

There is always one element of a multifaceted task that seems to be the most intractable and therefore controls the schedule. In the case of the preparation of this report it has been the design of the divertor, and we do not claim that the arrangement

described earlier is a final resolution of all the conflicting requirements. In order to render wall sputtering harmless, it has seemed to us essential to reduce the plasma temperature sharply near the boundary and in particular in the divertor region where the plasma finally makes contact with the wall. The divertor scrapeoff region is very awkward for analysis since it contains a plasma that sees the main discharge across the field in a direction where transport properties are severely restricted whereas it sees the divertor chamber region in the direction of the field where transport is fast. It is assumed that the divertor as described in Chapter 6 will adequately perform the three functions of a divertor: pumping out the efflux, protecting the main chamber wall from bombardment, and avoiding plasma contamination while not permitting an excessive erosion rate in the divertor chamber.

Useful Wall Lifetimes

The divertor may make it possible to eliminate or localize wall erosion due to sputtering, but radiation damage, and especially helium embrittlement from (n,α) reactions, will probably be an inherent feature of any fusion reactor system in which a significant amount of D-T reactions is taking place. We have assumed a useful lifetime of 5 years (40,000 operating hours). We feel confident of this from the point of view of swelling, but not from the point of view of creep and loss of ductility. This remains a crucial assumption in this work.

Needed Experiments

Large Toroidal Confinement System

The above assumptions concerning plasma confinement and ignition require experimental testing as soon as possible if the goal of commercial thermonuclear power is to be achieved on any reasonable time scale. To this end a large machine of the type most likely to achieve success (a tokamak if built today) should be fabricated of sufficient size that attainment of the Lawson criterion can almost be guaranteed. In such a device the confinement time will be long enough for relatively low power heating systems to heat the plasma to levels above the ignition conditions needed for a reactor. It has become popular to call such an experiment a "scientific feasibility experiment", but in addition to confinement and feasibility, it should also show achievement of ignition conditions, explore the high pressure dependence of thresholds of rapid loss (and allow studies of methods to control it), and it should show, in the neoclassical regime, the bootstrap current and provide a test bed to define how to inhibit or destroy it. The importance of trapped ion instability should be assessed.

Plasma Prototype Experiment

When a plasma of fusible isotopes is brought to the ignition point, a whole new class of important experiments becomes possible. Such an experiment would also mark an important milestone along the road to fusion power and consequently is of considerable interest for that point alone. Some have referred to such a machine as a "D-T Burner". Although a large confinement experiment using inert or moderately reactive nuclei (e.g., pure deuterium) can develop most of the necessary knowledge of plasma physics, only by achieving ignition plasma conditions in truly reactive systems can actual ignition be demonstrated leading to all the important experiments on reacting plasma dynamics that the liberation of energetic charged particles within the plasma can make possible. This machine would use tritium derived from fission sources and therefore would not need to breed its own fuel. Thus it would be a plasma prototype only and not a power-producing reactor prototype; no effort would be necessary to make use of the output power.

The plasma prototype would nevertheless produce an intense neutron flux, and a few seconds of operation at full level would render it impossible to service the machine by ordinary experimental laboratory methods.¹⁴ The inner wall would become highly activated and require remote handling for service. This severe complication makes this machine rather specialized in its purpose and not suitable for the main line of experimental work that would be carried out on the large toroidal confinement systems described in the preceding section.

Injection Experiments

Until recently laboratory plasmas have been of small physical size or of low energy density and usually both. Such plasmas have not been well suited to investigation of the depth of penetration of a high velocity pellet, one conceivable way of fuelling a reactor. The current tokamaks provide plasmas suitable for these experiments, and they are underway in at least one laboratory.¹⁵ Realistic knowledge of practical fuel source functions will eliminate the need for the injection assumptions used in this report.

Divertor Experiments

The divertor seems to be a very important part of the reactor system, and at least two different types of experiments are required.

A poloidal divertor, or a variety of poloidal divertors, must be operated on a tokamak device to explore its effectiveness in maintaining the purity of the plasma

and in isolating the wall from the discharge. This class of experiments falls into current high temperature plasma physics research programs.

The second class of experiments is to explore the low temperature plasma effects associated with the interaction of a plasma stream with a material wall. These problems could be most effectively pursued in small, supplementary devices designed for this purpose rather than being associated with high temperature plasma devices.

Radiation Damage

The helium embrittlement problem described above is under active study now by a variety of techniques.^{16,17,18} The principal method is by irradiation in fission reactors; some workers preload the specimen with helium, for example, by alpha-particle bombardment in an accelerator. A fission spectrum generates a rather negligible flux of neutrons above 8 MeV, whereas the 14.1 MeV neutrons from D-T reactions are more effective in producing the (n, α) reactions since they generally have thresholds of a few MeV. To simulate the actual flux to be expected in a D-T fusion reaction it has been suggested^{19,20} to accelerate tritons and bombard a high density deuterium gas target produced by a shock wave in a supersonic gas jet. Calculations indicate that neutron currents similar to those expected in a reactor could be generated by such a device. There is no general agreement whether or not fission source experiments can give satisfactory answers, or if such a special purpose accelerator is required. Certainly the macroscopic properties developed by a test specimen subjected to an actual total nvt of $10^{22} - 10^{23}$ neutrons of 14 MeV would provide very convincing design information for the vacuum vessel of a reactor.

Miscellaneous

1. Sputtering

The design philosophy in this study has been to reduce the particle energy at the plasma boundary to a low enough value to avoid the sputtering lifetime issue. In practice it may prove impossible to achieve this in a real machine. At least part of the vacuum structure may be exposed to potentially damaging bombardment. Low temperature plasma-metallic surface interaction experiments should be carried out in the plasma temperature range that appears likely to be encountered in the reactor environment. If neutron sputtering should be important,²¹ it may be necessary to select alloys that are resistant.

2. Fluorine Cross Sections

Adequate breeding ratios for tritium from lithium seem to be readily achievable, but several nuclear cross sections should be known more accurately for purposes of accurate design. For those contemplating the use of fluorine salts, the neutron absorption cross sections of fluorine are examples where further work would be useful in reducing the possible errors in the calculations.

3. Synchrotron Radiation Spectrum Measurements

The theory of synchrotron radiation from plasmas is complicated,^{22,23} and little effort has been made to apply it to realistic reactor configurations.^{24,25} Measurements have been undertaken on laboratory plasmas,^{26,27} but no plasmas have yet approached reactor conditions. As the plasma density and size increase, the medium changes from one that is very transparent to strongly absorbing, especially at the longer wavelengths. Thus a plasma can be almost as transparent for this class of radiation as for bremsstrahlung, behaving as a body source, or it can become almost opaque and approach black-body performance as a surface emitter. This depends on several parameters, the electron temperature being especially important. Greater effort is required in experimental verification of current theory.

4. Gas Diffusion in Metals

The diffusivity and solubility of protium, deuterium, and tritium should be measured as a function of temperature in all materials proposed for use in the reactor structure. The accuracy of tritium inventory estimates depends on knowledge of these quantities.

5. Flibe Heat Transfer Coefficients

Lithium-beryllium-fluoride, although not selected as a primary heat transfer medium in the model described in this report, circulates through the blanket, and heat is liberated and deposited within it and must be removed. A possible design modification could result in the flibe being a primary coolant. To allow proper engineering design, the appropriate heat transfer coefficients should be measured in the laboratory.

6. Others

As the fusion power era approaches, the engineering tasks required will uncover a great many areas in which research and development will be needed. This report will have suggested to every reader a number of questions that are still open. They will include problems of corrosion, materials compatibility, and useful lifetime of many of the reactor components. The new vacuum system represents a new industry by

itself. Finally, and perhaps the most challenging of all, is the development of satisfactory maximum field superconducting magnet systems.

A Look to the Future

Possible Timing

The problems that have just been summarized can be classified into two broad categories: Plasma physics and reactor engineering. There are many more in the latter category, and the traditional attitude has been that no really significant effort on the engineering problems is justifiable until the plasma physics has been sufficiently advanced to assure the possibility of reactors. In accordance with this conservative approach, the programmatic strategy has been to provide a series of experimental machines of gradually increasing size evolving in form in response to the experimental results. Extrapolation of this procedure resulted in the assumption that a significant milestone would be the so-called Scientific Feasibility Experiment, defined slightly differently within the community, but generally accepted to mean the achievement of the Lawson criterion at or above the ideal ignition temperature.²⁸ One such set of parameters is: an ion density 10^{14} cm^{-3} confined for a time exceeding one second while maintaining an ion temperature in excess of 5 keV (preferably 10). This is only one example of meeting the Lawson criterion; it is frequently erroneously identified as the Lawson criterion. Once such a demonstration had been accomplished, one would have a far clearer idea of what a reactor would be, and with the assurance that such a system was at least possible, one could then approach the practical problems of engineering feasibility, and finally face the challenges of economic viability.

This strategy was logical and consistent with a very limited budget. Today there is a considerable impetus for attempting a faster solution than might come from following that program, and the suggestion has been made of beginning the design today of not simply a scientific feasibility experiment, but rather combining it with the next step along the road, namely a machine that would actually produce a net power yield from the D-T fusion reactor. This has been called the D-T burner. One accepts a higher risk of failure by beginning design before all the experimental information one would like to have is at hand in the hope of saving several years of time in achieving the ultimate goal. The present energy supply dilemma and the increasing budgets for the entire CTR program make such an approach palatable to many people.

Whatever the program details turn out to be, and barring the appearance of some extremely serious fundamental problem, the end of this decade should find us nearer

the answer to scientific feasibility. A decade more of development should allow the operation of a prototype or demonstration reactor, and if these stages are successful, the fusion power industry should flower at the turn of the century.

Potential Roadblocks

This optimistic program for the future could founder by failing on technical or programmatic grounds. The significant increases in funding levels implied by the move to larger, more elaborate facilities are essential, and without them fusion power will never arrive regardless of how favorable the results of plasma physics experiments might be.

Technical obstacles may occur in either the plasma physics or the reactor engineering. Many of the potential problems described above are amenable to changes in design or modification in approach, but some are fundamental. In plasma physics the basic problem is still with us, namely adequate confinement at reactor conditions. If we cannot achieve this (and only large experimental devices can tell us), we cannot build a fusion reactor.

In reactor engineering a variety of unfavorable developments could jeopardize the economy of fusion power, but today we see few that have a potential to preclude an operable device. Perhaps the helium embrittlement problem is the only one.

Conclusions

Controlled thermonuclear fusion, harnessed as described in this report, is not a general panacea for the problems of energy supply to mankind. It is easy to exaggerate the advantages of nuclear fusion by claiming that it will provide a great abundance of very cheap power in a perfectly clean and safe manner that offers no environmental problems with minimal demands on natural resources. Although fusion promises none of these, it does offer numerous important benefits.

1. Abundance

Fuel supplies for the D-T cycle described in this report are widely distributed and available at negligible cost. Fusion could supply power by this cycle (using lithium reserves) for thousands of years. The harnessing of a purely deuterium-consuming fuel cycle would provide a truly inexhaustible fuel supply of worldwide distribution.

2. Low Cost

Although the fuel is essentially free, the plant is complex and expensive. Fusion power is and will remain capital intensive. One should not expect the real cost

of energy ever to become negligible. Nevertheless, the success of fusion power would establish a primary energy source not subject to cost escalation due to progressive fuel shortages.

3. Environmental Problems

Because of inherent neutron production, a fusion reactor will not be free of radiation problems; however storage of its radioactive by-products is a relatively simple task requiring no off-site transportation. Tritium is the only volatile dangerous material, and it should not be hard to make its hazard potential acceptably small. The principal environmental effect will be due to waste heat, intrinsic to any heat engine.

4. Resource Conservation

Capital intensive equipment implies heavy use of materials and raises questions of availability. The first generation plant described in this report, with its high requirement for beryllium, is clearly not suitable as an ultimate design. There are also questions about the potential supplies of other resources such as nickel, helium, and a few others. Because of the wide variety of options available for the development of fusion power, however, design variations can avoid critical materials. Ultimately the only resource really consumed should be deuterium.

Although fusion power is not a perfect answer to all energy problems, it appears to be the closest approach to the ideal of any system so far seriously proposed. The contemporary scene has served to demonstrate that the goal of universal affluence can easily be missed. The twenty-first century could spell disaster for mankind. A solution of the problems of fusion power in this century can assure the primary energy necessary for a solution to mankind's dilemma. We can rejuvenate the dream of abundance for all.

References

¹B. B. Kadomtsev and O. P. Pogutse, Trapped Particles in Toroidal Magnetic Systems, Nuclear Fusion 11, 67 (1971).

²M. N. Rosenbluth, R. D. Hazeltine, and F. L. Hinton, Plasma Transport in Toroidal Confinement Systems, Phys. Fluids 15, 116 (1972).

³H. P. Furth and P. H. Rutherford, Experimental Tests of Tokamak Reactor Scaling, unpublished.

⁴D. Dimock, D. Eckhardt, H. P. Eubank, E. Hinnov, L. C. Johnson, E. B. Meserve, E. Tolnas, D. J. Grove, Particle and Energy Balance in the ST Tokamak, Plasma Physics and Controlled Nuclear Fusion Research (International Atomic Energy Agency, Vienna, 1971) Vol. 1, pp. 451-463.

⁵Princeton Plasma Physics Laboratory Report MATT-Q-29 (1972).

⁶Princeton Plasma Physics Laboratory Report MATT-Q-30 (1973).

⁷L. Spitzer, D. J. Grove, W. E. Johnson, L. Tonks, W. F. Westendorp, Problems of the Stellarator as a Useful Power Source, U. S. Atomic Energy Commission Report NYO-6047 (1954).

⁸R. G. Mills, Lecture Notes, Proceedings of the Symposium on Thermonuclear Fusion Reactor Design (Texas Tech. University, Lubbock, 1970) pp. 38-77.

⁹L. Spitzer, D. J. Grove, W. E. Johnson, L. Tonks, W. F. Westendorp, op. cit.

¹⁰H. P. Furth and S. Yoshikawa, Adiabatic Compression of Tokamak Discharges, Phys. Fluids 13, 2593 (1970).

¹¹R. G. Mills, Ignition of Toroidal Fusion Reactors, Proceedings of the 5th Intersociety Energy Conversion Engineering Conference (American Chemical Society, Washington, D. C., 1970) Vol. 1, pp. 1-8 - 1-12.

¹²K. Bol, J. L. Cecchi, C. C. Daughney, R. A. Ellis, Jr., H. P. Eubank, H. P. Furth, R. J. Goldston, H. Hsuan, R. A. Jacobsen, E. Mazzucato, R. R. Smith, T. H. Stix, Neutral-Beam Heating in the Adiabatic Toroidal Compressor, Princeton Plasma Physics Laboratory Report MATT-1029 (1974).

¹³S. L. Gralnick, Solid Deuterium Evaporation in a Fusion Plasma, Nuclear Fusion 13, 703 (1973).

¹⁴W. G. Price, Jr., Nucleonic Considerations for PLT-II, Princeton Plasma Physics Laboratory Report TM-266 (1973).

¹⁵R. A. Ellis, S. L. Gralnick, C. D. Hendricks, private communication.

¹⁶Fusion Technology Studies Progress Report for Period Ending December 31, 1972, Oak Ridge National Laboratory Report ORNL-TM-4156 (1973).

¹⁷K. R. Garr, D. Kramer, and C. G. Rhodes, The Effect of Helium on the Stress-Rupture Behavior of Type 316 Stainless Steel, Metallurgical Transactions 2, 269 (1971).

¹⁸F. W. Wiffen and E. E. Bloom, Effect of High Helium Content on Stainless-Steel Swelling, Transactions of the American Nuclear Society 1973 Winter Meeting (American Nuclear Society, Inc., Hinsdale, Illinois, 1973) Vol. 17, pp. 143-144.

¹⁹C. R. Emigh, J. E. Brolley, Jr., and M. C. Cline, A Proposal for a 14 MeV Intense Neutron Source at Los Alamos, New Mexico, Los Alamos Scientific Laboratory Report LA-5191-P (1973).

²⁰Fusion Neutron Source Facility (Westinghouse Astronuclear Laboratory, Pittsburgh, Pennsylvania, 1973) WANL BP 3006.

²¹M. Kaminsky and S. K. Das, Particle Release from Niobium under 14-MeV Neutron Impact, Proceedings of the Fifth Symposium on Engineering Problems of Fusion Research, (Institute of Electrical and Electronics Engineers, New York, 1974) pp. 37-40.

²²B. A. Trubnikov and V. S. Kudryavtsev, Plasma Radiation in a Magnetic Field, Proceedings Second International Conference on Peaceful Uses of Atomic Energy (United Nations, Geneva, 1958) pp. 93-98.

²³M. N. Rosenbluth, Synchrotron Radiation in Tokamaks, Nuclear Fusion 10, 340 (1970).

²⁴R. G. Mills, Synchrotron Radiation from Fusion Reactors, Princeton Plasma Physics Laboratory Report MATT-658 (1969).

²⁵R. A. Krajcik, The Effect of a Metallic Reflector upon Cyclotron Radiation, Nuclear Fusion 13, 7 (1973).

²⁶A. J. Lichtenberg, S. Sesnic, and A. W. Trivelpiece, Measurement of the Synchrotron Radiation Spectrum from a Hot Plasma, Phys. Rev. Letters 13, 387 (1964).

²⁷S. Sesnic, A. J. Lichtenberg, A. W. Trivelpiece, D. Tuma, Synchrotron Radiation Experiments with a Hot Electron Plasma, Phys. Fluids 11, 2025 (1968).

²⁸R. G. Mills, Lawson Criteria, Institute of Electrical and Electronics Engineers Transactions on Nuclear Science NS-18 (1971) pp. 205-207.

Appendix A
Principal Characteristics of the
Princeton Reference Design
Fusion Power Plant

I. Plasma and Magnetics

A. Plasma Configuration

1. Tokamak with single null poloidal divertor
2. nominal major radius 10.5 m
3. nominal minor radius 3.25 m
4. nominal volume 2190 m³
5. confining field at centerline 6 T

B. Plasma Parameters

1. mean hydrogen ion density $5 \times 10^{19}/\text{m}^3$
2. mean ion temperature 30 keV
3. mean ion confinement time 3.83 seconds
4. average β , maximum β 0.04, 0.13
5. average β_0 1.83
6. plasma current 14.6 MA
7. ion ratios (D:T: Argon) (1:1: 0.096)
8. nominal safety factor q 2.067

C. Burn Parameters

1. DT reaction rate $1.22 \times 10^{21}/\text{second}$
2. tritium burnup per pass 8.7%
3. deuterium feed rate $1.40 \times 10^{22}/\text{second}$
4. tritium feed rate $1.40 \times 10^{22}/\text{second}$
5. argon feed rate $0.134 \times 10^{22}/\text{second}$
6. tritium consumption 0.527 kg/day
7. burn cycle length 100 min.
8. shutdown time per cycle 3 min.

D. Coil Descriptions

1. toroidal field coils (48)
Nb₃Sn ribbon, 10 kA/conductor
1600 km x 4.5 cm x 0.5 to 1.54 cm

- 315.4 M ampere-turns; 250 GJ stored energy
 16.0 T peak field
2. divertor field coils (2 pairs)
 Nb₃Sn ribbon, 10 kA/conductor
 100 km x 5.0 cm x 0.5 cm
 14.2 M ampere-turns
 3. vertical field coils (5 pairs)
 Nb₃Sn ribbon, 10 kA/conductor
 220 km x 5.0 cm x 0.5 cm
 33.9 M ampere-turns
 4. control field coils (21 pairs)
 Nb₃Sn ribbon, 10 kA/conductor
 540 km x 5.0 cm x 0.5 cm
 64.0 M ampere-turns

E. Cryogenic System

1. coil heating 280 kW
 from pulsed fields 35 kW
 from neutrons & gammas 95 kW
 thermal conduction 150 kW
2. refrigeration power required 100 MW
3. helium inventory (STP) $4.55 \times 10^5 \text{ m}^3$
4. helium loss rate (STP) $8.4 \text{ m}^3/\text{day}$

734445

Fig. A.1 Magnetic flux surfaces, divertor and coils.

II. Energy and Power Systems

A. Reactor Power

5305 MW

1. Origin

fusion alphas	688 MW
fusion neutrons	2752 MW
neutron capture	1865 MW

2. Deposition

radiation in divertor	134 MW
radiation on wall	554 MW
nuclear heating	4617 MW

B. Wall Load

1. nominal wall area (11.0 m x 3.6 m torus)	1563 m ²
2. DT neutron production rate	$1.22 \times 10^{21}/s$
3. DT neutron current through wall	$0.78 \times 10^{18}/m^2 \cdot s$
4. total neutron flux at wall	$8.65 \times 10^{18}/m^2 \cdot s$
5. wall load due to DT neutrons	1.76 MW/m ²
6. wall load due to plasma radiation	0.35 MW/m ²

C. Helium Cooling

1. mean pressure	50 atm (5.2 MPa)
2. mean pressure drop	2.8 atm (.29 MPa)
3. outlet conditions	3.24 Mg/s @ 638°C
4. inlet conditions	2.83 Mg/s @ 360°C 0.41 Mg/s @ 66°C
5. pumping power	241 MW
6. helium inventory (STP)	$2 \times 10^5 m^3$

D. Steam System

1. energy inputs

5546 MW

from main steam generator	3664 MW
from first reheater	780 MW
from second reheater	474 MW
from helium feedwater heaters	628 MW

2. energy to turbo-alternator shafts		2405 MW
to VHP turbine	498 MW	
to HP turbine	434 MW	
to IP turbines (2)	483 MW	
to LP turbines (4)	990 MW	
3. energy to condenser	3141 MW	
@ 2.5 inches Hg (abs) back pressure		

E. Electrical System

1. gross electric generation		2105 MW
2. station service		375 MW
helium circulator motors	251	
cryogenic refrigerators	100	
cooling tower fans	15	
miscellaneous	9	
3. net electrical output		2030 MW
4. net efficiency		38.3%

F. Cost Evaluation

1. capital cost	1215 M\$
2. capital cost per installed capacity	598 \$/kWe
3. cost of busbar energy	15.2 mills/kWh

Fig. A.2 Energy flow schematic of the reference design power plant.

III. Materials and Design

A. Fuel Reactions

1. tritium burnup	0.527 kg/day
2. deuterium burnup	0.352 kg/day
3. lithium burnup	1.104 kg/day
4. beryllium burnup	0.362 kg/day
5. helium production	10.2 m ³ (STP)/day

B. Blanket Materials

1. flibe: eutectic mixture
 53.1% BeF₂, 46.9% LiF
 m.p. 355°C, 1.898 Mg/m³ @ 800°C
2. PE-16: austenitic nickel alloy
 43% Ni, 39% Fe, 18% Cr

C. Tritium Handling

1. tritons per fusion	1.04 (net)	
2. breeding rate	0.548 kg/day	
3. inventory		2.57 kg
a) main fuel loop	0.505 kg	
(two hour cycle at		
feed rate of 4.21 g/min)		
b) fuel reserve	2.020 kg	
(8 hours of fuel feed,		
or 4 days of burnup)		
c) dissolved in structure	0.030 kg	
d) dissolved in flibe	0.010 kg	
e) entrained in helium	0.007 kg	
4. doubling time (without compounding)	121 days	

D. Vacuum Chamber

1. nominal wall area	1563 m ²
(11.0 m x 3.6 m torus)	
2. actual wall area, main chamber	1635 m ²
3. divertor wall area	1080 m ²
4. mass of radiation shield wall,	204 Mg
vacuum wall, & divertor	

E. Breeding Blanket

1. dimensions: 74 cm thick, 1378 m³
2. average composition

PE16	9.4%	129 m ³	1.03 Gg
Flibe	69.8%	962 m ³	1.86 Gg
Helium	20.8%	287 m ³	905. kg

F. Primary Shield

1. dimensions: 80 cm thick, 1563 m³
2. average composition

Heavy concrete	90%
Steel	5%
Helium	5%
3. total mass 9.03 Gg
4. dose rates

while operating	3690 rem/hr.
while shut down	27 mrem/hr.
5. biological shield: 2 m additional concrete

G. Thermal Flywheel

1. molten Flibe; 4.94 Gg

H. Main Support Structure

1. total mass of about 4 Gg

744378

Fig. A.3 Cross section of the nuclear island.

Appendix B

The Profiles Code

Introduction

The Profiles Code is used to solve numerically the energy transport equation,

$$nk \frac{dT}{dt} = (P_{\alpha} + P_J) - (P_b + P_s + P_t) \quad (B.1)$$

and to evaluate the solution of the mass transport equation under the assumptions of constant diffusion coefficient and uniform density source. Cylindrical geometry is used, and the boundary conditions that are imposed at the separatrix surface are the solutions of the transport equations (temperature and density). The various terms are defined below.

A steady state solution is sought as the asymptotic limit solution of Eq. B.1. When this solution is found, the equilibrium condition¹ is satisfied at all values of r , the plasma radius. It is important to note however, that this condition has been generalized to include the non-local effects of energy transport by conduction and particle diffusion, (contained in P_t).

The power terms in Eq. B.1 represent the effect of transport phenomena, P_t , radiation losses due to bremsstrahlung, P_b , and synchrotron radiation, P_s , joule heating, P_j , and the absorption of alpha particle energy, P_{α} . The transport losses, based on the assumption of constant transport coefficients, are given by

$$P_t = \sum_k \left[\left(\frac{3}{2} k T_k \right) \nabla \cdot \vec{J}_k + \nabla \cdot \vec{q}_k \right] \quad (B.2)$$

where the summation is over all species present. The first term is due to the convective transport of energy by the particle diffusion currents

$$\vec{J}_k = - D \nabla n_k, \quad (B.3)$$

and the second term is the conduction energy flux

$$\vec{q} = - \kappa \nabla T. \quad (B.4)$$

The radiation losses are

$$P_b = 4.85 \times 10^{-22} n_e \sum_k z_k^2 n_k \sqrt{T_e} \quad (B.5)$$

and

$$P_s = 0.8 \times 10^{-5} P_b T_e^3 \quad (B.6)$$

The latter equation is based on an approximate relationship derived by Mills²

$$\frac{P_s}{P_b} \approx (0.707 \times 10^6) \frac{\rho^{0.3} B^3 T^3}{r^{0.6} n^{1.6}} \quad (B.7)$$

In this expression ρ is the resistivity of the metal wall, and r is the plasma minor radius. It is a weak approximation at the operating density employed here ($n \leq 2 \times 10^{14}$) but is sufficiently valid for the purpose of this study. The joule heating term, based on the classical resistivity, is

$$P_J = \eta j^2 \quad (B.8)$$

while the alpha particle energy source is given by

$$P_\alpha = p(1 - p) n^2 \langle \sigma v \rangle U_\alpha ; \quad (B.9)$$

in which p is the tritium fraction and n refers to the fuel ion density; $U_\alpha = 3.52$ MeV.

Boundary conditions have been specified on the separatrix, which we take to be circular. This surface is not a physical boundary. Rather, the conditions set here express the continuity of the density and temperature (energy per particle) and the fluxes of these quantities between the reactor plasma (interior to the separatrix), and the plasma in the scrapeoff region (exterior to the separatrix) that flows into the divertor. Our choice of boundary values,

$$T_e = T_i = 0.5 \text{ keV} \quad (B.10)$$

$$n_i = 7 \times 10^{12}, \quad (B.11)$$

was based on the considerations of the divertor plasma given in Chapter 6. Since an asymptotic steady state is being sought, an initial temperature profile may be chosen

arbitrarily; the dissipative nature of the diffusion process renders the steady state independent of the initial data.

In cylindrical geometry, the steady state fuel density profile is given by

$$n_i = \frac{a^2 S}{4 \alpha D} \left[1 - \alpha \left(\frac{r}{a} \right)^2 \right]; \quad (\text{B.12})$$

where α is a parameter used to provide a finite $n_i(a)$, a is the plasma minor radius, and S is the fuel ion source, ions/cm³-sec. We assume that all of the species diffuse at the same rate and determine the electron density by the condition of charge neutrality

$$n_e = \sum_k z_k n_k. \quad (\text{B.13})$$

Other than the deuterium and tritium fuel ions, there is a specified fraction of impurity ions in the fuel mixture, and the alpha particles produced by fusion reactions must also be included.

The source function for the alpha particles is not spatially uniform, and consequently the steady state alpha particle density is found by solving

$$D \nabla^2 n_\alpha + S_\alpha = 0 \quad (\text{B.14})$$

subject to the boundary condition

$$n_\alpha(a) = 0. \quad (\text{B.15})$$

A Green's function solution is found,³

$$n_\alpha(r) = \frac{1}{D} \left[\ln\left(\frac{a}{r}\right) \int_0^r S(r_o) r_o dr_o + \int_r^a \ln\left(\frac{a}{r_o}\right) S(r_o) r_o dr_o \right]. \quad (\text{B.16})$$

The interpretation of the boundary condition (B.15) is

$$\frac{n_\alpha(a)}{n_\alpha(0)} \ll 1. \quad (\text{B.17})$$

The electron temperature is determined by an empirical relationship,

$$T_e = -45 + 807 \frac{n_A}{n_H} + \ln(T_i) \left(24 - 353 \frac{n_A}{n_H} \right), \quad (B.18)$$

which accounts for the equilibrium distribution of the thermalized alpha energy between the electrons and ions. n_A is the density of argon impurity ions present. This is a rough fit to the results of a program, TETIPAIR⁴ that calculates the equilibrium temperature difference between the electrons and ions in an infinite homogeneous reactor plasma with a given amount of impurity.

Energy Transport Difference Equation

The energy transport equation may be written in the form

$$\frac{dT}{dt} = \frac{1}{nk} F(T, r, t) + \kappa' \nabla^2 T, \quad (B.19)$$

where $\kappa' = \kappa/nk$, and F is given by

$$F(T, r, t) = \left(p(1-p)n^2 \langle \sigma v \rangle U_\alpha + \eta j^2 \right) + \left(\sum_k \frac{3}{2} kDT_e \nabla^2 n_k \right) \\ - \left[\left(1 + .8 \times 10^{-5} T_e^3 \right) \left(4.85 \times 10^{-22} n_e \sum_k n_k z_k^2 \sqrt{T_e} \right) \right] \quad (B.20)$$

This equation, B.20, will be cast in forward time, centered space, difference form on a cylindrical grid consisting of 56 shells spaced at equal distances. The radial spacing is

$$\Delta r = \frac{a}{56}, \quad (B.21)$$

and the indexing of the shells is as shown in Fig. B.1. The resulting explicit difference equation will be stable provided⁵

$$\kappa' \Delta t / (\Delta r)^2 = \text{const.} \leq 1/2. \quad (B.22)$$

744381

Fig. B.1 Spatial grid used for
 profiles code calculations.

The finite difference form of equation B.20 is

$$\begin{aligned}
 k N_k^n \left(\frac{T_k^{n+1} - T_k^n}{\Delta T} \right) = & (N_k)^2 \langle \sigma v \rangle_k p(1-p) U_\alpha + 1.0 \times 10^7 \eta_k (j_k)^2 \quad (B.23) \\
 & - \left(1 + 0.8 \times 10^{-5} (TE_k)^3 \right) \left(4.85 \times 10^{-22} NE_k \sum_l (n_k)_l z_l^2 \sqrt{TE_k} \right) \\
 & + \left\{ \frac{3}{2} kD(1. + f_A) \left[\frac{2}{(\Delta r)^2} (N_{k+1/2} T_{k+1/2} - N_{k-1/2} T_{k-1/2}) \right. \right. \\
 & \left. \left. + \frac{1}{r_k \Delta r} (N_{k+1/2} T_{k+1/2} + N_{k-1/2} T_{k-1/2}) \right] \right\} \\
 & + \left\{ \frac{3}{2} kD \left[\frac{2}{(\Delta r)^2} (NE_{k+1/2} TE_{k+1/2} - NE_{k-1/2} TE_{k-1/2}) + \right. \right. \\
 & \left. \left. \frac{1}{r_k \Delta r} (NE_{k+1/2} TE_{k+1/2} + NE_{k-1/2} TE_{k-1/2}) \right] \right\} \\
 & + \left\{ D \left[\frac{2}{(\Delta r)^2} (NA_{k+1/2} UA_{k+1/2} - NA_{k-1/2} UA_{k-1/2}) \right. \right. \\
 & \left. \left. \frac{1}{r_k \Delta r} (NA_{k+1/2} UA_{k+1/2} + NA_{k-1/2} UA_{k-1/2}) \right] \right\} \\
 & + \left\{ \kappa \left[\frac{2}{(\Delta r)^2} (T_{k+1/2} - T_{k-1/2}) + \frac{1}{r_k \Delta r} (T_{k+1/2} + T_{k-1/2}) \right] \right\}
 \end{aligned}$$

in which the meanings of the new notation should be readily grasped.

Values taken at $(k + 1/2)$ imply

$$N_{k+1/2} = \frac{N_{k+1} + N_k}{2} \quad (B.24)$$

similarly

$$N_{k-1/2} = \frac{N_k + N_{k-1}}{2}, \quad (B.25)$$

and the superscript n has been suppressed on the right-hand side of the equation.

The third diffusion term comes from the treatment of the alpha energy source function. The net source in the k^{th} shell is the alpha energy generated in that shell plus the alpha energy convected into the shell less all of the alpha energy that is convected out of the shell. It is assumed that characteristically the alpha particles in any shell all possess the same mean energy given by

$$U_{\alpha} = \frac{3}{2} T_i + \frac{\tau^*}{\tau} \left[(3520 - 32T_e) + \frac{3}{2}(T_e - T_i) \ln \left(\frac{3520 - 1.5 T_e}{30.5 T_e} \right) \right] \quad (\text{B.26})$$

where τ^* is given by

$$\tau^* = \frac{(0.9922 \times 10^{13} T_e^{3/2})}{\ln \Lambda n_e} \quad (\text{B.27})$$

$\ln \Lambda$ being the Coulomb logarithm for scattering between alpha particles and electrons.

Treatment of Boundaries

At the center shell, $k = 1$, and at the outermost shell special attention must be given to the proper imposition of boundary conditions.

By symmetry all fluxes vanish at $r = 0$, and consequently all terms of the form

$$\frac{F_{k-1/2}^n}{\Delta r} \quad (\text{B.28})$$

where F may be N , NE , ... , vanish for $k = 1$. The remainder of Eq. B.23 remains unchanged.

At the outer boundary of the system we set

$$T_{k+1/2} = T_{\text{bound}} \quad (\text{B.29})$$

in the conduction term and apply the global conservation law

$$\oint_{\text{outer bound}} \mathbf{J} \cdot d\mathbf{A} = \iiint_{\text{volume}} S \, dv \quad (\text{B.30})$$

to determine the flux of particles out of the system at $T = T_{\text{bound}}$. The flux of alpha particles leaving the system is found in the same manner, and the alpha particle energy characteristic of the boundary is determined by linear extrapolation.

Calculation Sequence

The Profiles calculation is initiated by the acquisition of an initial arbitrary temperature profile. In addition, information about the characteristics of the machine (geometry, magnetic field and total discharge current) as well as the values of the transport coefficients are set at this time.

The ion density profile, alpha source and density profile, and electron density profile are evaluated by the expressions given earlier, and the mean alpha energy is found. In evaluating the alpha source function use is made of a service routine, REACTO,⁶ which calculates the reactivity, $\langle\sigma v\rangle$, of the plasma as a function of T .

At this point the calculation of T is carried forward in time and iterated until sufficient convergence is obtained. In practice a steady solution was found to be maintained to six significant figures for 300 iterations of the code. This solution could be run, in fact, until the propagation of the roundoff errors obliterated the calculation. The convergence of the system was valid locally, shell by shell, as well as globally for the system as a whole.

References

- ¹R. G. Mills, Time Dependent Behavior of Fusion Reactors, Nuclear Fusion Reactor Conference Proceedings (British Nuclear Energy Society, London, 1969) pp. 322-336.
- ²R. G. Mills, Synchrotron Radiation from Fusion Reactors, Princeton Plasma Physics Laboratory Report MATT-658 (1969).
- ³P. M. Morse and H. Feshbach, Methods of Theoretical Physics, Part II (McGraw-Hill Book Co., New York, 1953) p. 1191.
- ⁴R. G. Mills, unpublished.
- ⁵R. D. Richtmyer and K. W. Morton, Difference Methods for Initial-Value Problems, 2nd edition (Interscience Publishers, New York, 1967) p. 189.
- ⁶F. H. Tenney, unpublished.

Appendix C

FOSTER WHEELER CORPORATION
HELIUM HEATED STEAM GENERATOR

FOR

FUSION REACTOR

December, 1972

Document Number

ND/72/145

This report was prepared by the Nuclear Department of Foster Wheeler Corporation in response to a request for assistance from Dr. M. B. Gottlieb, Director of the Plasma Physics Laboratory, Princeton University.

1. INTRODUCTION

Note: The introduction, Figures 1 and 2 and Table I of the original document have been deleted because they presented a brief description of the fusion process and the reactor. These subjects are covered in more detail in the main text of this report.

2. FUNCTIONAL DESIGN

Figure 3 shows the basic energy balance for the fusion reactor steam cycle. The cycle utilizes a supercritical steam generator following parallel pass high pressure and low pressure reheaters.

An item unique to this cycle is the use of helium heated feed water heaters. It is expected that two stages of helium heated feed water heaters will be included in the cycle. These heaters are included because of the necessity to cool some of the helium to 150°F in order to extract a heavy isotope of hydrogen (tritium) from the helium coolant inventory. The tritium diffuses from the flibe through the heat transfer tubes into the helium whilst the helium is passing through the fusion device's coolant circuits.

Figure 4 shows the temperature profile existing in the steam generator. Examination of this figure will reveal some significant differences from the high-temperature, gas-cooled reactor cycle.

744322

Fig. 4. Surface area required for steam generator. (sic)

Courtesy of
Foster Wheeler Corp.

744323

Fig. 3. Energy balance for fusion system.

First, the helium temperatures are lower than the HTGR because in the case of the fusion reactor the metallic coolant circuits passing through the flibe will be constructed from stainless steel.* This is in order to benefit from the extensive irradiation damage information existing for this material as a result of testing in the LMFBP Program where it is being used as a fuel cladding material.

Second, the steam pressures and temperatures are higher than the current HTGR conditions in order to obtain the highest cycle efficiency. These high steam temperatures, when associated with the lower helium temperatures, result in significantly reduced temperature differences, which in turn increase the heat transfer surface requirements for a given duty.

Third, the cycle uses a parallel pass two stage reheat arrangement. This introduces functional and mechanical complexity which, however, can be incorporated into the steam generator design without significant problems.

Fourth, the fusion reactor uses a metal pressure circuit to contain the helium rather than a pre-stressed concrete pressure vessel, which reduces the number of constraints placed upon the designer in the arrangement of the steam generator.

Figure 5 shows the general arrangement of the steam generator. Helium enters the unit through the upper horizontal gas duct and divides, one portion flows downward over the high pressure reheater, whilst the remainder flows upwards over the low pressure reheater. The ratio of gas mass flow over each reheater is controlled mechanically by the dampers situated in the top of the steam generator. The low pressure reheater gas flow recombines with the high pressure reheater gas flow in the region above the once-through supercritical fluid coil and continues to flow downward over the once-through supercritical fluid coil to the bottom of the unit where the gas passes through the screen and into the axial-flow electrically driven circulator. The helium is then returned to the fusion reactor through the vertical and horizontal ducting from the circulator outlet.

In order to maintain capital costs at a minimum, the most efficient heat transfer surface is used in the form of helical tubing for the once-through supercritical fluid coil and the high pressure reheater. In the low pressure reheater essentially straight tubes are to be used in order to minimize steam side pressure drop to maintain the economics of the steam cycle.

* Editor's Note: Subsequent to the preparation of this report the construction material was changed from stainless steel to PE-16. See Chapter 9.

744321

Fig. 5. General arrangement of the steam generator.

Feedwater enters the steam generator at the bottom of the unit through a pressure vessel penetration. Tubes from the heat transfer coil are connected to the penetration through tubesheets. These tubes include capillaries to form inlet orificing to avoid dynamic and static stability problems. The supercritical fluid is heated to 1000°F whilst flowing up the counter flow helical coils. Upon leaving the helical coils, the fluid is directed to an outlet tubesheet and penetration from which it is routed to the turbo-alternator.

The high and low pressure counter flow reheaters operate in a similar manner to the once-through supercritical fluid coil with the exception that orificing is not necessary to maintain stability in these circuits.

Statistical analysis of the various parameters affecting the performance of the heat transfer surfaces shows that this type of unit can be designed to operate over the load range and under conditions where fouling and tube plugging have occurred. Non-linearity between the gas flow requirements of the parallel pass reheaters is accounted for by the inclusion of a flow damper in the top of the unit.

For reasons of simplicity and efficiency of operation, the plant uses a constant speed electrically driven circulator in this study. Helium flow variation over the load range for essentially constant helium temperatures requires the use of helium gas circuit isolation valves in each circulator circuit. These are very important during periods when one module is shut down and isolation is necessary and when the modules are operating at different loads.

Startup of the circulators without disturbing the gas flow distribution in the reactor is accomplished by use of a recirculation loop. This is accomplished by use of the horizontal duct, which enters the steam generators above the lower once-through supercritical fluid coil. From here the recirculation gas is passed down over the lower supercritical coil to the inlet to the circulator. Control of the recirculation gas is accomplished by operation of the butterfly valve located in the horizontal duct.

Figure 6, showing the plant layout, has been deleted because of later revisions in this part of the design.

Tables 2-4 give general details of the heat transfer surface in the once-through supercritical fluid generator and the HP and LP reheaters.

TABLE 2ONCE-THROUGH SUPER CRITICAL FLUID GENERATOR
Data for One 2300 MWe Plant

Installed Surface = 698304 FT²
Number of tubes = 2814
Length = 845 FT

Steam Δ P Coil = 335
 Lead ins = 330
 Lead out = 100
 Total 735

Helium Δ P 6.4 psi

Diameter 149" ID outer shroud

Length of Coil 59' coil

Tube Specs: 1-1/8" OD x 0.185" Nom. Thk. x Incoloy 800

TABLE 3

HP REHEAT
Data for One 2300 MWe Plant

Installed Surface = 113,000 FT²
 Number of tubes = 2440
 Length = 101

Stm Side Δ P = 58 psi (Allowed 70)

Helium Δ P = $\frac{101}{37} \times 1.33 = 3.7$ psi

Coil Height = $\frac{101}{37} \times 4.6 = 12.5$ FT

Diameter (ID of outer shroud) = 139"

Tubes Specs: 1-3/4" OD x 0.150" Nom. Wall Thk. x Incoloy 800 - Coil
 1-3/4" OD - Leads

TABLE 4

LP REHEAT
Data for One 2300 MWe Plant

Installed Surface = 91,000 FT²
Number of tubes = 2840
Length = 70 FT

Stm Side Δ P = 44 psi (Allowed 50)

Helium Δ P = $1.33 \times \frac{70}{37} = 2.6$ psi HP Side Governs, Use 3.7 psi

Coil Ht = $\frac{70}{37} \times 4.6$ FT = 8.7 FT

Diameter (ID Outer Shroud) = 139"

Tube Specs: 1-3/4" OD x 0.150" Nom. Wall Thk. x Incoloy 800 - Coil
2-1/4" OD - Leads

3. MECHANICAL DESIGN

The steam generator module is contained within a ferritic-steel pressure vessel. The pressure vessel is insulated on the inside using silica insulation and on the outside using conventional insulation. The purpose of the insulation is to maintain the pressure vessel at temperatures commensurate with economic material selection.

The high temperature helium gas flows over the exposed heat transfer surface and its supports. In these high temperature regions the support structure is fabricated from high nickel alloy materials.

The central core pipes, which exist in both the reheater section and the supercritical core section, act as the main support members. The helical coil heat transfer surfaces are supported from the core sections by radial supports.

The reheaters and the upper portion of the supercritical coil experience very significant structural loadings during operation as a result of the combination of temperature, pressure, and differential expansion existing within these structural members. These structural members are analyzed using finite element, creep fatigue, time dependent analyses utilizing modern high-speed computers.

These techniques are directed towards assuring compliance with the ASME Nuclear Codes.

A novel feature incorporated into this design is the reheat damper located on the top of the unit. The lower portion of the vertically actuated operating shaft and the conical seat assembly are all subject to the high temperature helium environment and are fabricated from suitable high nickel alloy material. The actuating shaft passes out of the pressure vessel through an externally cooled bellows seal assembly to the remote operated electrically driven actuator mechanism.

The helium circulator located at the bottom of the unit is of single-stage axial flow conventional design. The design temperature for this unit falls below the creep range and utilizes present day technology.

The steam/water connections to the unit utilize cylindrical penetrations connected to the pressure shell through thermal sleeves. Heat transfer tubing is connected to the penetrations via circular tubesheets. The tube to tubesheet joints are accomplished using high integrity welding techniques currently available.

4. MAINTENANCE

Each steam generator will be shop assembled in halves and shipped to the job site and assembled with a circumferential field weld. The two halves will permit

shipment by railroad to any site with good railroad access. Further, the two halves will reduce the lifting requirements to the job site. Should it become necessary to completely replace the steam generator, the sequence can be reversed.

All the heat transfer surface is of helical coil configuration. All the circuits are individually lead out to tubesheets, which are thermal sleeved to the main pressure shell. Tube ends are accessible from outside the helium pressure vessel by breaking a joint just outside the water or steam thermal sleeve. These joints can either be welded or use a variety of flanges according to the preference of the user and economics.

Tube ends are connected to the tubesheets with internal bore welds. Tubesheet faces are then accessible without need for machining away the tube from inside the tube hole. Fundamentally, two types of plugs are available. In the first type, the plug can be hollow or solid and driven into the tube hole and sealed by a fillet-weld. This is a common technique, which is used with much success in most heat exchangers and steam generators to date. In the second type, a radiographable internal bore weld and orbital weld combination can secure a cap to a lip trepanned from the tubesheet face. The latter is more involved but restores the unit to the original integrity of the manufactured product.

There is no maintenance necessary for the heat transfer surface.

The unit is internally insulated, there may be a program of insulation inspection or thermal couple checking, which would be recommended on a periodic basis at the convenience of the plant operator. The exact details and nature of this can be developed in future activities.

The unit is equipped with a reheat damper, which is to be set during the initial operation. Examination of the expansion bellows seals would be recommended on an annual basis. Likewise, the recirculation damper should be so examined. The central core of the entire unit is hollow allowing man access from the operating floor, which is above the unit, down through the unit to any elevation. The helium circulator and drive motor or turbine can be arranged to be detached and withdrawn from outside the unit.

Seismic support guides or snubbers or pipe hangers would be subject to normal examination requirements in practice by the utility.

5. ACKNOWLEDGEMENT

Foster Wheeler Corporation wishes to take this opportunity to acknowledge the assistance rendered by Dr. P. W. Davison and Mr. K. C. Sokolosky, Jr. Their contributions proved to be invaluable in preparing this report.

6. LIST OF CONTRIBUTORS

R. O. Barratt

J. Polcer

R. de Flesco, Jr.

W. T. Klamm

R. Costello

INDEX

A			
Acceleration of fuel pellets to hypervelocities	135	Boundary temperature	103
Accident, design basis	449,454	Breeder reactor	20
Accidental release	467	Breeder salt loop	393
Adiabatic stabilization	337	Breeding blanket	550
Adsorbers	390	Breeding sensitivity	194
Afterheat	207	Bremsstrahlung	27,45
Air and water pollution	473	Brines	20
Allegheny Ludlum Steel Corporation	524	Burn parameters	543
Alpha particle	553	C	
Alpha particle density	554	Calculation sequence	559
Alpha particle source function	61	Capital cost	508,511
ANISN	171	Charge exchange	85
Argon impurity	27	Christensen, U. R.	1
Argonne National Laboratory	1	Circulating power requirement	286
Assumptions	531	Classical diffusion	25
Attenuation factor	129	Code of Federal Regulations	432
Auxiliary power systems	287	Coil description	543
Auxiliary preheat system	479	Coil energizing	478
B		Coil heating	200
Babcock and Wilcox Co.	288,524	Coils	479
Ballooning modes	28	Coils, control field	56,544
Banana orbits	25	Coils, D	324
Banana regime	27	Coils, design for fault forces	346
Bishop, A. S.	2	Coils, divertor	80,544
Black body spectrum	27	Coils, divertor field	143
Blanket	16,165,171	Coils, toroidal field	324,543
Blanket cooling tube arrangement	246	Coils, vertical field	80,139,544
Blanket design goals and constraints	257	Cold fuel injection	43
Blanket heat transfer	230	Compensating EMF	56
Blanket heating	195	Compensating flux	56
Blanket material	549	Concrete	168
Blanket mechanical design	243	Conductance time	120
Blanket power	198	Confinement	61
Blanket volume and weight	270	Consequences of accidents	474
Blistering	312	Consumable materials	471
Bombardment	293	Control field coil system	56,544
Bonanos, P.	1	Coolant	149
		Coolant helium loop	381
		Cooling system	219
		Cooling, effect of magnetic field	255

Equilibrium condition	30	Fujisawa, T.	1
Equilibrium configuration	40		
Equilibrium force	43		
Equilibrium points	43	G	
Equilibrium theory	28,127		
Equilibrium vertical field	66	Gamma fluxes	182
Evaporation rate equation	128	Gamma rays	169
Exhaust chamber	75	Gas diffusion in metals	536
Extended pulse tokamak discharge	56	General Atomic Co.	288,524
		Gralnick, S. L.	1,127
		Greenspan, E.	1
		Guiding centers	24,25
F			
Feed rate	45		
Field line breaking	28	H	
File, J.	1		
Filling pressures	46	Hancox, R.	1
First wall	291	Hazards	432
First wall fluence	201	Hazards, biological	432
Fission	2	Hazards, public	432,436
Flibe	149,153,168	Hazards, service	432,442
Flibe blanket heat transfer	230	Heat balance	284
Flibe cooling	251	Heat conduction in scrapeoff plasma	99
Flibe flow in blanket	260	Heat exchanger breakdown	429
Flibe heat transfer coefficient	256,536	Heat rejection	22
Flibe heat transfer parameters	266	Heat transfer	217
Flibe phase diagram	160	Heat transfer calculations	230
Fluorine cross sections	186,198,536	Heat transfer from the flibe	251
Flute modes	28	Heat transfer systems	217
Foster Wheeler Corporation	288	Heating method for plasma	41
Foster Wheeler Corporation, Nuclear		Heavy water	21
Department	524,561	Helium	149,154
Fuel cost	519	Helium circulation system	271
Fuel deposition profile of a pellet	134	Helium circulator	280,570
Fuel fabrication	135	Helium cleanup loop	276
Fuel handling	362	Helium coolant loop	381,420
Fuel injection	126	Helium cooling	546
Fuel mixture	30	Helium, cryogenic	352
Fuel pellet density	128	Helium ducts	276
Fuel processing	479	Helium headers	273
Fuel processing equipment costs	380	Helium inventory	352,461,544,546
Fuel requirement profile	135	Helium pressure drops	276
Fuel supply	470	Helium production	201,352,549

Helium pumping power	235	K	
High pressure reheater	568		
High velocity ballistic injection	126	Kerma factors	195
		Kink	28

I

Ideal fuel requirement profile	127
Ignition	41,531
Impink, A. J.	1
Impurities	122
Impurity lines	45
Inductance matrix	143
Inductively stored energy of the plasma	43
Initial cooldown	357
Initial startup	357,477
Injected energy per particle	45
Injection	532
Injection energy	51
Injection experiments	534
Injection power requirement	43
Instabilities, plasma	28
Instabilities, radial field	334
Interaction of fuel pellet with reactor plasma	127
Interchange modes	28
Interest during construction	514
Intermagnetics General Corporation	334
Internal energy	60
Ion and electron conduction energy confinement times	45
Ion confinement time	45
Irradiation of superconducting magnet system	355

J

Johnson, E. F.	1
Johnson, J. L.	1
Joule heating	553

L

Lamp effect	46
Lawrence Livermore Laboratory	4
Leakage of tritium	426,434
Leakage, radiation	435
Lewin, G.	1
Line radiation	45,46
Lithium	20
Lithium consumption	10,194,470
Lithium, liquid, coolant	149
Load following	489
Los Alamos Scientific Laboratory	2,4
Low pressure reheater	569

M

Magnet protection	339
Magnet shield	168,204
Magnetic axis	28
Magnetic characteristics	75
Magnetic configuration of the reactor	138
Magnetic energy	138
Magnetic flux surfaces	28
Magnetic forces on flibe	252
Magnetic limiter	68
Magnetic surfaces	25
Magnetic system	478
Maintenance, repair and overhaul	493
Major repair	499
Malfunction, control field system	441
Malfunction, divertor field system	440
Malfunction, most serious plant	446
Malfunction, toroidal field	441

Malfunction, vertical field system	441	Niobium-titanium superconductor	151
Maroni, V. A.	1	Niobium-3-tin superconductor	150
Massachusetts Institute of Technology	2	Nominal blanket section	259
Materials	148	Nuclear waste	460
Materials and design	549	Nucleonics	164
Maximum allowable flux in the reactor core	57	Null point	75
Maximum allowable levels of radiation	433		
Maximum allowable levels of radioactive materials	433		
Maximum burning pulse length	40		
Maximum cycle time	59		
Maximum design temperature	148		
Maxwellian average fusion reactivity integral	45		
Mills, R. G.	1,4		
Minimum load	489		
Mining	460		
Molecular speed ratio	129		
Multifilament superconductor	328		
Multistage permeation	367		

N

N-squared ignition	41
Narragansett Electric Company	1
Needed experiments	533
Needs for further research	531
Neutral beam injection	51,53
Neutral flux	97
Neutral injection power	43
Neutral-plasma interaction	91
Neutron balance	186
Neutron flux	178
Neutron source	174
Neutron spectra	178
Neutron sputtering	315
Neutronics	164
Neutrons	169
Nimonic PE-16	150,152,156,165
Niobium	148

O

Oak Ridge National Laboratory	4
Ohmic heating phase	41
Ohmic heating power supply	41
Once-through supercritical fluid generator	567
Operating cycle	40
Operating density	51
Operation at reduced power	489
Operational procedures	477
Oxygen fractions	46

P

Parameters, plasma	543
Parameters, reactor	543
PE-16	150,152,156,165
Pellet injection	43
Perkins, H. K.	1
Personnel hazards	432
Plant characteristics	10
Plant efficiency	287
Plant layout	278,459
Plasma and magnetics	543
Plasma characteristics and control	24
Plasma configuration	543
Plasma confinement	531
Plasma control	24
Plasma density	43,543
Plasma density profile	133
Plasma design profile	28
Plasma parameters	28,40,543
Plasma properties	24

Special shield	169,204	Temperature stability of the chosen	
Spitzer, L., Jr.	2	operating point	70
Sputtering	300,535	Tenney, F. H.	1
Sputtering coefficients	293	Thermal efficiency	287
Stainless steel	148	Thermal pollution	17,473
Startup	357,477	Thermal power	259
Steady state power reactor	40	Thermal stabilization	334
Steam cycle	562	Thorium	20
Steam cycle startup	481	Time history of plasma parameters	
Steam generators	280,281,564	and discharge current	40
Steam system	283,546	Tokamaks	4
Steiner, D.	195	Tonks, L.	127
Stellarator	2	Toroidal field	138,149
Stored magnetic energy	60	Toroidal field coils	324,543
Stress, superconductor	338	Toroidal magnetic field mirrors	25
Structural material	148	Toroidal shift	28
Structural problems	291	Transport coefficients	30,552
Structure, main support	550	Transport losses	552
Subsonic flow	88	Trapped particles	25
Superconducting magnets	323	Treatment of boundaries	558
Superconducting material	328	Tritiated water	465
Superconductivity	2	Tritium breeding	148
Superconductor	150	Tritium breeding ratio	169,190
Superconductor stress	338	Tritium, dissolved	426
Superconductor thermal stabilization	334	Tritium distribution and leakage	411
Supersonic flow	84,119	Tritium doubling time	16,169
SWAN code	194	Tritium escape	17,465
Swelling	150,295	Tritium fraction	45
Synchrotron radiation	27,45,99	Tritium handling	549
Synchrotron radiation spectrum		Tritium inventory	428,549
measurements	536	Tritium leakage	17,424,429,434,465
		Tritium loss	429
		Tritium mapping	424
		Tritium migration	412
		Tritium permeation	416,429
		Tritium processing system	395
		Tritium release guidelines	411
		Tube midwall temperature	270
		Tube temperatures	257
		Tube wall corrosion	257
		Turbine-generators	283
		Turbulence	28
T			
TCAP code	205		
Tearing modes	28		
Temperature of plasma	32		
Temperature of plasma boundary	103		
Temperature of plasma in divertor	15		
Temperature profile in steam			
generator	562		

U

University of Texas	4
University of Wisconsin	4
Unsolved problems	526
Uranium	20

V

Vacuum chamber	549
Vacuum closure	503
Vacuum pumps	121
Vacuum system	75,119
Vacuum wall	229,291
Vapor pressure and melting point data	372
Vapor pressures for argon	368
Vertical field	28,139
Vertical field coils	80,139
Volt-second rating	40

W

Wall absorption	27
Wall absorptivity	45
Wall erosion	91
Wall life	291,533
Wall load	178,186,546
Wall load variation	256
Waste disposal	460
Waste heat	17
Westinghouse Electric Corporation	1
Westinghouse Power Systems Co.	288,524

X

X-rays	220
--------	-----

LEGAL NOTICE

This report was prepared as an account of Government sponsored work. Neither the United States, nor the Commission, nor any person acting on behalf of the Commission:

A. Makes any warranty or representation, express or implied, with respect to the accuracy, completeness, or usefulness of the information contained in this report, or that the use of any information, apparatus, method, or process disclosed in this report may not infringe privately owned rights; or

B. Assumes any liabilities with respect to the use of, or for damages resulting from the use of any information, apparatus, method, or process disclosed in this report.

As used in the above, "person acting on behalf of the Commission" includes any employee or contractor of the Commission to the extent that such employee or contractor prepares, handles or distributes, or provides access to, any information pursuant to his employment or contract with the Commission.