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TOKAMAK EXPERIMENTAL POWER REACTOR STUDIES

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ABSTRACT

This report documents-the principal results of a scoping and project definition study for the Tokamak Experimental Power Reactor. Objectives are discussed; a preliminary conceptual design is described; detailed parametric, survey and sensitivity studies are presented; and research and development requirements are outlined.

FOREWORD

The ERDA/DCTR Program Plan for the development of tokamak power reactors calls for a tokamak fusion test reactor (TFTR) to begin operation in 1980, a first tokamak experimental power reactor (TEPR) to begin operation in 1985-86, possibly a second TEPR to begin operation in approximately 1990, and a tokamak demonstration power reactor (TDPR) to begin operation in 1995-2000.

In August, 1974, an interdisciplinary project was organized at Argonne National Laboratory to perform a scoping and project definition study for the first TEPR. This project has directly involved about a dozen staff members, with backgrounds in fission reactor technology, high energy physics technology, basic research and plasma physics, whose names appear on the title page of this report. In addition, the specialized expertise of several groups at Argonne and of a number of consultants have been drawn upon in the development of the study (see acknowledgements).

The principal goal of this study was to develop a technical basis for the specification of a TEPR conceptual design study in FY 1976. Thus, the development of requirements and examination of realistic options for the major systems were emphasized. Attention was given to the extrapolation of TEPR technology to a commercial reactor and to the constraints imposed by the schedule on technology development for the TEPR. A preliminary conceptual design was developed to provide a focus for the study. However, the emphasis of the study was upon a broad and critical assessment of the technological options for the TEPR.

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INTRODUCTION

This report documents the principal results of a scoping and project-definition study for the Tokamak Experimental Power Reactor (TEPR). This study has simultaneously addressed four interrelated questions: 1) definition of objectives of the TEPR; 2) identification and evaluation of technological options for the major systems; 3) assessment of the technical basis for the TEPR and identification of required research and development; and 4) preliminary definition of an integrated conceptual design.

Possible objectives for the TEPR are discussed in Chapter I, and a plausible set of objectives is defined. A review of the factors that affect the size of the TEPR and how the size is related to the objectives is also included in this chapter.

Chapter II summarizes the study. A preliminary conceptual-design summary is presented in the first section. Subsequent sections summarize reactor performance and the studies of the major systems.

The performance characteristics of the TEPR are discussed in Chapter III. The conditions that must be obtained in order for the TEPR to achieve its objectives are established, and the plasma physics basis for the performance analysis is discussed. The plasma computational model is described in Appendix A, and a parametric survey, which was useful in sizing the TEPR, is summarized in Appendix B.

A preliminary conceptual design of the magnet system is presented in Chapter IV. A superconducting toroidal-field coil system design is described. Superconducting and copper coil designs are described for the ohmic heating and equilibrium field systems. Supporting design analysis is discussed in some detail in Chapter IV and in Appendix E.

The plasma support systems are discussed in Chapter V. Design requirements for the neutral-beam heating system are developed, and the status of ion-source development is reviewed. Preliminary specifications for the toroidal and neutral-beam vacuum systems are developed. Power-supply requirements are discussed.

Chapter VI describes the preliminary conceptual design of the primary-energy conversion system. Several options for first-wall and blanket/shield design and for the primary coolant are evaluated. A broad review

of materials is discussed in Appendix C, and extensive blanket/shield sensitivity and optimization studies are presented in Appendix D.

The tritium handling system is discussed in Chapter VII. A preliminary conceptual design is described.

Supporting research and development programs -- in materials, superconducting magnets, blanket and shield technology and tritium handling -- which are required for the TEPR, are outlined in Appendices F, G, H and I. These research and development requirements were identified in this study, and the suggested programs were developed on the basis of previous experience with similar research and development programs. Additional research and development needs (e.g. neutral beams, vacuum systems, power supplies) are discussed in the report.

CHAPTER I

TEPR OBJECTIVES AND SIZE CONSIDERATIONS

A. Objectives

A major purpose of this study is to focus and refine the objectives for a TEPR by defining a scientifically and technologically feasible device that satisfies the programmatic requirements of the U.S. tokamak fusion power reactor development program. Such a device should represent the logical next step beyond the TFTR in the achievement of D-T plasma confinement adequate for power reactors. The TEPR should be a major step in the demonstration of engineering feasibility of tokamak power reactors. It should provide experience with and demonstrate the feasibility of the various technologies involved and their synthesis in a power reactor environment, thereby serving as a focal point for the technology development program. It should also demonstrate the feasibility of electrical power production from fusion on a significant scale (in the range 5 to 50 MWe), and, if possible, demonstrate the feasibility of electrical power breakeven. The TEPR should serve as a major test facility for the technology development program. These general objectives must be reconciled among themselves, with the state-of-the-art and required developmental programs for the various technologies, with the DCTR programmatic requirement for initial operation in 1985-87, and with a yet unspecified fiscal constraint (which was taken to be approximately \$500 million as a guideline in this study) to arrive at a realistic set of specific objectives for a TEPR.

It is useful to divide the potential TEPR objectives into four categories:

1. Plasma Physics

- Heat, confine and refuel a reactor-grade D-T plasma under such conditions that the production of 5 to 50 MWe of electrical power is feasible.
- Study plasma confinement, stability, heating, fueling, and control in the reactor regime.

2. Power Reactor Engineering Feasibility

- Remove sensible heat from an energy conversion blanket operating under high temperature conditions such that generation of 5 to 50 MWe of electrical power is feasible.

- Demonstrate the feasibility of electrical power breakeven.
- Breed, extract, process, and recycle tritium under conditions such that a tritium breeding ratio greater than unity is demonstrable.

3. Technology Demonstration

- Superconducting magnets.
- Plasma heating and fueling.
- Vacuum systems.
- Power supply and energy storage.
- Primary coolant.
- Tritium extraction, processing, and containment.
- Materials.
- Assembly, fabrication, and remote maintenance.

4. Test Facility Utilization

- Surface and bulk materials irradiation.
- Blanket modules with different blanket design, material, coolant, and tritium extraction concepts.
- Other - components testing, plasma and reactor instrumentation and control, etc.

The TEPR should be the logical next step beyond the TFTR (initial operation 1980) along the path to achievement of plasma confinement that is adequate for a power reactor. Achievement of sufficient pulse length and pulse repetition rate so that the cycle-averaged thermonuclear power, if converted to electrical power, would be in the range 5 to 50 MWe is a major goal for the TEPR. Conversion of this energy into sensible heat in an energy-conversion blanket is a second major goal. If these two goals could be achieved, conversion of the thermal energy in the primary coolant into electrical energy in a secondary system could be accomplished straightforwardly. (Whether or not electrical power should actually be produced in the TEPR is more an economic and psychological question than a technical one.) These two objectives are sufficiently important in themselves that there is a strong motivation to minimize any other factors which might complicate or compromise their attainment. This consideration argues for a "minimum-risk" approach that utilizes, to the maximum extent possible, technology that is either "state-of-the-art" or is extrapolated therefrom with reasonable confidence.

The minimum-risk approach is contrary to many of the general objectives envisioned for the TEPR. The specific technological choices (e.g. materials) might not be extrapolatable to a commercial power reactor, or at best might not represent optimal choices for the latter. Certain functions of a commercial reactor (e.g. tritium breeding) are not essential to the plasma and sensible heat production objectives for the TEPR; such "extraneous" complications would certainly be eliminated in a minimum-risk design. A minimum-risk design does not provide a focus for a broad-base reactor technology development program, nor does it lead to a demonstration that the technology can be sufficiently developed so that a full-scale fusion power reactor is technologically feasible. Thus, there are strong arguments against the minimum-risk, or minimum technology development, approach and in favor of utilizing the TEPR as a vehicle for advancing the technologies which will be needed for fusion power reactors. The pros and cons of the minimum-risk approach must be carefully weighed in the initial design iterations.

Staged operation is proposed as one means of reconciling these seemingly conflicting requirements. A minimum-risk approach would be followed in designing a device to achieve the plasma physics objectives, with an energy conversion blanket (no tritium breeding) to achieve the sensible heat-power objectives. The device would be operated during Stage I (approximately 1986-89) to obtain these objectives and, in addition, to serve as a materials irradiation facility. Provision would be made in the design to replace one or more of the original blanket modules with alternate blanket test modules. During Stage II (approximately 1990-95), the device would function primarily as a blanket test facility in which various blanket, tritium breeding and extraction, materials, and coolant concepts were studied under reactor conditions. Staged operation has a number of advantages: (1) the fundamental plasma-power objectives can be focused upon first, with a minimum amount of complication; (2) decisions to allocate funds for the blanket test modules can, to some extent, be made after the plasma physics objectives have been demonstrated; and (3) additional time is allowed for the development of the technologies required for a tritium breeding blanket, without delaying the overall TEPR schedule. The disadvantages of staged operation are associated with the added complications to the basic design and with the need for remote breakdown and reassembly. These apparent advantages and disadvantages must be examined in detail during the coming year.

The TEPR will represent a unique test facility. Its utilization as such should be factored into, but should not compromise, the basic design objectives. The extent to which TEPR can be used for materials studies and to test power plant components must be subjected to careful study.

Plasma confinement scaling and the effect of impurities are two major uncertainties that must be allowed for in the design. (Results from PLT [initial operation 1975] on confinement scaling and from Doublet-III [initial operation 1978] on plasma shape optimization, as well as results from PDX and ISX [initial operation 1977] on impurity control, will be factored into the design as they become available.) Allowance will be made in the design for the attainment of many of the objectives even if the confinement scaling and impurity effects are less favorable than anticipated.

Demonstration of the feasibility of electrical power breakeven would be a dramatic achievement for the TEPR. Allowance for attaining this objective, in the event of relatively unfavorable transport scaling and impurity effects, would require substantial and expensive additional margin in the design relative to what would suffice to assure many of the other objectives. (This problem would be mitigated somewhat if Doublet-III demonstrates the anticipated advantages of noncircular cross-section plasmas.) However, a convincing demonstration of electrical power breakeven might well require the advance of certain technologies beyond the point that is compatible with the schedule, financial constraints, and general objectives of the TEPR. Thus, electrical power breakeven feasibility should be a tentative objective, but the decision to make this a firm objective should be deferred until the consequences are examined in detail and additional information is available from the plasma confinement experiments that will be conducted in the next few years.

The size of the TEPR, hence its cost and degree of extrapolation beyond preceding devices, scales with the power level, as will be discussed in the next section. This consideration argues for choosing a modest power level objective which will suffice for the demonstration of power reactor technology and will satisfy the criterion of "significant" power production. An electrical power output on the order of tens of megawatts is adequate for this purpose. On the other hand, electrical power breakeven becomes more plausible at larger power outputs, perhaps on the order of a hundred megawatts electrical. Achievement of 5 to 50 MWe power output, with provision for operation at higher levels if present plasma physics uncertainties turn out favorably, seems to be a reasonable objective.

TABLE I-1
TEPR OBJECTIVES

General Features

- Logical next step beyond TFTR in plasma confinement devices.
- Two distinct stages — an initial stage based largely on current technology and a second stage that demonstrates new breeder-blanket technology.
- Plasma performance can be demonstrated early in Stage I, prior to funding decisions on breeder-blanket technology demonstration experiments for Stage II.
- Blanket-tritium technology demonstration experiments are scheduled to allow time for technology development programs.
- Electrical power breakeven is demonstrated before 1990 (tentative).
- Major components designed to accommodate objectives of both stages, as appropriate.

Stage I (~1986-89)

Objectives

- Study plasma confinement, stability, fueling, heating, and control in the reactor regime.
- Heat, fuel, and confine a reactor-grade D-T plasma under such conditions that the generation of 5 to 50 MWe of electrical power is feasible.
- Utilize as a materials irradiation facility.
- Demonstrate certain technologies (e.g. superconducting magnets) in a power reactor environment.
- Operate an energy conversion blanket with sensible heat removal under such conditions that the generation of 5 to 50 MWe of electrical power is feasible.
- Demonstrate electrical power breakeven (tentative).

Features

- The plasma physics objective (first one) can be achieved with a low-temperature blanket.
 - The blanket functions only as an energy converter, without lithium and the extra complication of tritium extraction and containment.
 - The design and the achievement of objectives should be predicated upon technology which is either presently available or which can be developed with realistic technology development programs.
-

TABLE I-1

TEPR OBJECTIVES (cont'd)

-
- Adequate supplemental neutral beam heating will be provided so that the first five objectives can be obtained in the beam-driven mode if plasma confinement is less than anticipated.

Stage II (~1990-95)

Objectives

- Demonstrate the breeding, extraction, processing, and containment of tritium with a blanket test module operating at low temperature.
- Demonstrate the breeding, extraction, processing, and containment of tritium with a blanket test module operating at temperatures such that sensible heat is removed and 5 to 50 MWe of electrical power production is feasible.
- Utilize as a test facility to achieve above objectives for alternate blanket modules embodying different primary energy conversion, sensible heat removal, and tritium extraction concepts.
- Utilize as a materials irradiation facility.

Features

- Primarily an engineering test facility to demonstrate the required blanket-tritium technology in a power reactor environment.
 - Simultaneous utilization as a component and radiation test facility (to the extent that this is possible).
-

A tentative set of TEPR objectives are summarized in Table I-1. These objectives should be periodically reviewed during the course of the conceptual design. As more comprehensive analyses are performed and the resulting requirements are compared with technology as it exists and as it can reasonably be expected to advance, the objectives and schedules of the TEPR and of the supportive technology development programs can be refined to best satisfy the overall programmatic objectives of the DCTR tokamak power reactor development program.

B. Factors Determining the Size of a TEPR

Three types of considerations guide the determination of size. The TEPR must be a justifiable and logical next step beyond the TFTR in tokamak plasma confinement devices. On the other hand, it must be large enough to accomplish the power objectives of the TEPR. Finally, the cost constraint will impose a size limitation of a different type.

Table I-2 provides some perspective on the evolution of tokamak plasma confinement devices. Also shown in this table are the parameters for two conceptual designs of "commercial" tokamak power reactors. The parameters for the commercial reactors are in a certain sense a moving target, which will change as information is developed in the tokamak confinement program and the supporting technology development program. Nevertheless, they are useful in giving some indication of what must be accomplished in the experimental power reactor and demonstration power reactor stages. While one cannot merely interpolate in this table to arrive at parameters for the TEPR, such an interpolation is useful in providing guidance for analysis and in providing a measure of assurance that the objectives and the analysis are plausible.

The objective of demonstrating the feasibility of producing 5 to 50 MWe of electrical power, averaged over the operating cycle, imposes requirements on the thermonuclear power output during the burn pulse and on the length of the burn pulse. Refueling needs depend upon the confinement time. Requirements on power output and on confinement time translate directly into requirements on size. These requirements are developed for an ignition device^{*} with reference to the idealized geometrical configuration shown in Fig. I-1.

* An ignition device is defined as one in which alpha heating is sufficient to maintain the energy balance at the operating condition without resort to supplemental heating.

Table I-2 Tokamak Parameters

	R (m)	a (m)	A = R/a	I (MA)	B _t (kG)	n _e (10 ¹⁹ m ⁻³)	T _i (keV)	T _e (keV)	β ₀	q	τ _E (msec)	P _T (MW)	Operation Date
Existing Experiments(1)													
ST	1.09	0.13	8.4	0.07	40	4.0	0.60	2.50	0.8	5.1	10		
ORMAK	0.80	0.23	3.5	0.12	18	3.0	0.30	0.70	0.5	5.0	11		
ATC (uncompressed)	0.90	0.17	5.3	0.15	15	1.5	0.25	1.00	0.4	4.0	5		
ATC (compressed)	0.36	0.11	3.3	0.13	35	8.0	0.75	2.50	0.2	4.0	3		
Doublet II	0.59	0.12/0.15	4.5	0.14	8	1.3	0.25	0.55	0.6	5.0	2		
TFR(2)	0.92	0.20	4.9	0.10-0.30	50	4.0	1.00	1.00	0.8	2.8	16		
TM-3	0.40	0.08	5.0	0.07	40	7.0	0.35	0.50	0.8	4.6	3-4		
T-4	1.00	0.17	5.9	0.12	40	4.0	0.70	1.50	0.8	4.8	16		
T-6	0.70	0.25	2.8	0.06	10	1.0	0.20	0.30	0.3	7.4	1		
Next Generation Experiments (1)													
T-10	1.50	0.40	3.8	1.1	50		2.0			2.5			1975
PLT	1.30	0.45	2.9	1.6	50	10.0	3.1	3.1	0.3	2.4			1975
Doublet III	1.40	0.45/1.5	3.1	5.0	25	20.0	5.0		1.0	2.6			1976
"Feasibility" Experiment (3)													
TFTR (nominal)	2.50	0.56	4.5	1.0	49	8.0	6.0	6.0	1.0	3.0	200	7.4	1980
TFTR (high current)	2.70	0.95	2.8	2.5	45	13.0	8.5	8.1	~2.0	3.0			
Experimental Power Reactor													
?													
Demonstration Power Reactor													
?													
"Commercial" Reactor													
PPPL ⁽⁴⁾	10.5	3.25	3.2	4.6	60	2.0	56	24	1.9	2.1		5000	
UNMAK-1 ⁽⁵⁾	13.0	5.0	2.6	21.0	38	8.0	11	11	1.1	1.75	1.4 × 10 ⁻⁶	5000

Figure I-1 Idealized Tokamak Configuration

The thermonuclear power during the burn pulse is

$$P = \text{const } n^2 \overline{\sigma v} V_p \quad (I-1)$$

where V_p is the plasma volume, n is the D-T ion density and $\overline{\sigma v}$ is the Maxwellian-averaged fusion cross section. The maximum ion density is limited by the constraint on the maximum value of β_θ , the ratio of plasma thermal pressure to the magnetic pressure of the poloidal confining field,

$$n \propto \frac{\beta_\theta I^2}{a^2 T}, \quad (I-2)$$

where I is the plasma current which produces the confining poloidal field, a is the plasma radius, and T is the plasma temperature. Theoretically, $\beta_\theta \leq A \equiv R/a$ to suppress "balloon-mode" MHD plasma instability.⁽¹⁾

Stability against "kink-mode" MHD plasma oscillations limits the maximum plasma current

$$I \propto \frac{a B_t}{q A}, \quad (I-3)$$

where q is the safety factor for kink-mode MHD stability. $q(a) \geq 2.5$ is thought to be adequate for stability.⁽¹⁾

The maximum field at the toroidal field coil, $B_{\text{max}}^{\text{TFC}}$, is limited by technological constraints on the superconductor. For niobium-titanium, $B_{\text{max}}^{\text{TFC}} \lesssim 80$ kG, and this must be further reduced to allow for the presence of other fields. The field in the plasma is reduced by a geometrical factor (see Fig. I-1)

$$B_t = \frac{r_w + \Delta_m}{R} B_{\text{max}}^{\text{TFC}} = \left(1 - \frac{r_w + \Delta_B + \Delta_S}{R}\right) B_{\text{max}}^{\text{TFC}}. \quad (I-4)$$

Combining these results leads to

$$P \propto \frac{\beta_\theta^2 (B_{\text{max}}^{\text{TFC}})^4}{q^4} \frac{a^2 R}{A^4} \left(1 - \frac{r_w + \Delta_B + \Delta_S}{R}\right)^4 \frac{\overline{\sigma v}}{T^2}. \quad (I-5)$$

The quantity $\overline{\sigma v}/T^2$ depends only on the plasma temperature and has a broad maximum at $T \approx 13$ keV, as shown in Fig. I-2. For a fixed aspect ratio, A , an increase in size increases the power in two ways: (1) the plasma volume increases, $P \propto a^2 R \propto R^3/A^2$; and (2) the field in the plasma increases, $P \propto (C_1 - C_2/R)^4$

Figure I-2. Fusion Reaction Parameter $\overline{\sigma v} / T^2$

In order to attain a power balance in the plasma during the burn pulse, the power losses (radiation and transport) must equal the power sources (fusion alpha, ohmic and, possibly, supplemental heating). The transport loss is inversely proportional to the confinement time, which is characterized by the parameter $n\tau$. The value of $n\tau$ that must be achieved in order to have a balance can be determined by solving the plasma power and particle balance equations. The required ($n\tau$) can be reduced by using supplemental heating (e.g. neutral beams of energetic deuterons). The required $n\tau$ is shown in Fig. I-3 for several values of the parameter ξ , which is the ratio of supplemental to fusion alpha heating of the plasma.

The relationship among the required $n\tau$, size, and other reactor parameters depends upon the transport mechanism that will govern the energy confinement in the TEPR. What mechanism this will be is not known at this time, although there are theoretical arguments^(1,6) that trapped-particle instabilities will govern energy transport in a reactor-type plasma. In particular, the dissipative trapped-ion mode (TIM) instability is thought to be the major energy transport mechanism that will be present in the TEPR. The TIM theory, when combined with Eqs. (I-2)-(I-4) leads to a requirement upon the size which is necessary to attain a given $n\tau$.

TIM

$$n\tau \propto \frac{\beta_{\theta}^2 (B_{\max}^{TFC})^6}{q T^{11/2}} \frac{a^4 [1 - (r_w + \Delta_B + \Delta_S)/R]^6}{A^{3/2}} . \quad (I-6)$$

Although the trapped particle modes are expected, on theoretical grounds, to be the most likely energy transport mechanism in the TEPR, current experiments are consistent with ion-energy transport predicted by the neoclassical theory, while electron-energy transport and particle-transport seem to follow a pseudoclassical (PC) theory in which the magnitudes of the transport coefficients are somewhat larger than is predicted by the neoclassical theory.⁽¹⁾ Thus, extrapolation of current experimental results to the TEPR regime indicates a classical type of energy loss which, when combined with Eqs. (I-3) and

Figure I-3 Confinement

(I-4), leads to a requirement upon the size that is necessary to attain a given $n\tau$.

PC

$$n\tau \propto \frac{(B_{\max}^{\text{TFC}})^2 T^{\frac{1}{2}}}{q^2} \frac{a^2 [1 - (r_w + \Delta_B + \Delta_S)/R]^2}{A^2} \quad (\text{I-7})$$

For a fixed aspect ratio, A , confinement improves with size in two ways: (1) the plasma gets bigger, $n\tau \propto a^m \propto R^m/A^m$; and (2) the field in the plasma increases, $n\tau \propto (C_1 - C_2/R)^p$, where $m = 4$ and $p = 6$ for TIM, and $m = p = 2$ for PC.

Thus, both power level and confinement increase with size, maximum field at the toroidal field coil and β_θ (for TIM scaling), and both decrease with the safety factor, q , and the aspect ratio, A . The power level increases with ion temperature up to $T \approx 13$ keV, then decreases. The confinement decreases rapidly ($T^{-11/2}$) with temperature for TIM scaling and increases slowly ($T^{\frac{1}{2}}$) with temperature for PC scaling.

The blanket (Δ_B) and shield (Δ_S) thicknesses are determined by the requirements that the radiation damage to the components of the superconducting toroidal field coils and the radiation heat load to the refrigeration system for these coils be sufficiently low. A detailed analysis of this question is presented in Chapter VI and Appendix D. The conclusion is that $\Delta_B + \Delta_S \approx 1$ m. There is a strong incentive to minimize this thickness because the magnetic field in the plasma varies directly with $(\Delta_B + \Delta_S)/R$, for a given $B_{\max}^{\text{TFC}}$, and the power varies as B_t^4 .

The thicknesses of the toroidal field coil, of the ohmic heating (transformer) coil and of the central support cylinder are determined from magnet design requirements. The analysis in Chapter IV indicates that the combined thickness, Δ_m , should be ≈ 1 m.

The change in magnetic field produced by the ohmic heating (transformer) coils, ΔB_{OH} , varies with plasma current, I , major radius, R , and central core radius, r_v , approximately as

$$(\Delta B_{\text{OH}})_{\text{ind}} \propto \frac{RI}{r_v^2} \propto \frac{a^2 B_{\max}^{\text{TFC}} [1 - (r_w + \Delta_B + \Delta_S)/R]}{r_v^2 q} \quad (\text{I-8})$$

Relation (I-8) pertains to the field change required to induce the plasma current. Allowing a factor of ~ 2 for plasma resistive losses and a factor of $\sim 1/2$ for a superconducting coil going from $-B_{OH}$ to $+B_{OH}$, relation (I-8) determines the minimum r_v for which $|B_{OH}|$ is less than the limit for niobium-titanium superconductors ($|B_{OH}| \lesssim 80$ kG). For a conventional copper conductor, operation would be from $-B_{OH}$ to 0, so the minimum r_v would be that for which ΔB_{OH} was less than one-half the limit for copper conductors ($\Delta B_{OH} < \sim 60$ kG).

Noncircular plasma cross sections promise some advantage relative to circular plasma cross sections in terms of the power level and confinement for a given reactor size. For a noncircular plasma with minor dimension a (dimension in the horizontal midplane in Fig. I-1), a factor ℓ is defined as the ratio of the circumference of the noncircular plasma to the circumference of a circular plasma with radius a , and a factor κ is defined as the ratio of cross-section area of the noncircular plasma to a circular plasma with radius a (e.g., for an elliptical cross section, with major radius b , $\kappa \approx b/a$ and $\ell \approx \sqrt{1/2(1 + \kappa^2)}$). Relations (I-2) and (I-3) obtain for the noncircular plasma, but with a replaced by $a\ell$ (including in $\bar{A} = R/a\ell$), and Eq. (I-4) is unchanged. Thus, for a noncircular plasma, the thermonuclear power scales as

$$P \propto \frac{\beta_{\theta}^2 (B_{\max}^{TFC})^4}{q^4} a^2 \kappa R \left(\frac{a\ell}{R} \right)^4 \left(1 - \frac{r_w + \Delta_B + \Delta_S}{R} \right)^4 \frac{\overline{ov}}{T^2}, \quad (I-9)$$

which reduces to relation (I-5) for $\ell = \kappa = 1$. If noncircular cross-section plasmas can operate at the same β_{θ} and q as circular cross section plasmas, a point which is uncertain but which should be resolved by the Doublet-III experiments, the former should realize a significant advantage over the latter in power performance. This advantage arises primarily because a given volume of plasma can be located closer to the inside of the toroidal field coil if it is noncircular, hence it is in a higher magnetic field, than if it is circular.

Relations (I-6) and (I-7) also are assumed to apply to noncircular plasma cross sections, when a is replaced by $a\ell$, although there may be other differences in confinement scaling. As was the case with power output, confinement is improved by the stronger magnetic field in the plasma, which allows a larger plasma current.

Impurities have an important effect on the power balance in tokamaks, particularly upon ignition devices. High-Z ions sputtered or outgassed from

the first wall enhance the radiation loss from the plasma, and the significance of this plasma contamination upon the power balance depends strongly upon the atomic number of the impurity ion. Using the computational model that is described in Appendix A, the minimum temperature for which ignition occurs (the temperature above which the radiation loss exceeds the alpha heating) was determined for several impurity species. These results are plotted in Fig. I-4 as a function of the impurity concentration in the plasma. Oxygen is a typical background gas, carbon and silicon would be found in a device with a graphite or silicon-carbide surface, iron would be found in a device with a stainless steel first wall, and molybdenum is representative of the higher-Z materials that might be used for first walls or limiters. D^+ and T^+ sputtering coefficients are $\sim 5 \times 10^{-3}$ atoms/ion, so that an impurity concentration on this order could be anticipated in a device which operated for several particle confinement times without special impurity control measures. Considering that the confinement predicted by TIM theory decreases with temperature as $T^{-11/2}$, it is unlikely that an ignition device could be operated with a stainless steel first wall without special impurity control measures. On the other hand, ignition devices with silicon carbide, graphite, sintered aluminum or beryllium first-wall surfaces should be possible for $T = 10$ keV.

The power that would be obtained during the peak portion of the burn pulse is plotted as a function of major radius, R , in Fig. I-5, for $T = 10$ keV. Results are shown as a function of aspect ratio for circular ($\kappa = 1$) and non-circular ($\kappa = 3$) plasmas. These results indicate the size reactor that is required to obtain a given power, without regard for the question of confinement, for the parameters ($q = 2.5$, $\beta_\theta = 2.2$, $\Delta_B + \Delta_S = 1$ m, $B_{\max}^{TFC} = 75$ kG). Some of the smaller devices indicated in Fig. I-5 would not be allowed because the central core radius, r_v , is so small that $|\Delta B_{OH}|$ is excessive. Allowing a factor of approximately one-half for pulsed operation, the TEPR power objective (5 to 50 MWe) should be comfortably satisfied by a reactor with $P \approx 100$ MWt during the burn pulse. For an aspect ratio of 3, this requires $R \approx 6$ m for a circular plasma and $R \approx 5$ m for a $\kappa = 3$ noncircular plasma. To achieve the net electrical power objective would require larger power levels during the burn pulse, hence larger sizes.

Figure I-4 Effect of Impurities Upon Ignition

Figure I-5 Power Versus Size ($B_{\max}^{\text{TFC}} = 75$ kG, $\beta_0 = 2.2$, $q = 2.5$, $T = 10$ keV)

The discussion up to this point has focused on ignition devices, although driven devices, in which the alpha heating is supplemented by neutral beam (D_0) injection heating, are also considered in this study. Similar analyses for devices driven to $Q_{\text{plasma}} \approx 10$, which is about the lowest value for which net electrical power is conceivable, indicate that the values of R cited in the preceding paragraph could be reduced slightly.

An extensive parametric study was performed to evaluate the tradeoff among reactor size, confining plasma current, and supplemental injected power. The sensitivity of these parameters to the maximum field in the toroidal field coils, the blanket plus shield thickness, the plasma impurity content, the safety factor (q) and the confinement parameter ($n\tau$) was examined. These results are summarized in Appendix B.

The experimental basis for confidence in the noncircular plasma cross section is not nearly as extensive as for the circular plasma cross section. However, the predicted size advantage associated with the noncircular cross section is significant. The Doublet-III program is intended to provide an experimental confirmation that this advantage can be realized. In this study, the performance of circular and noncircular cross section plasmas, which would fit into the same toroidal field coil system, are evaluated in parallel, but the technological systems are sized to be compatible with the circular cross section plasma.

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CHAPTER II

SUMMARY

The primary purpose of this scoping study is to define objectives, options, major problems and research and development needs. This purpose is best accomplished within the framework of a preliminary conceptual design. In this manner, the interactions and trade-offs among the various plasma-physics, technological and cost constraints can be established; the requirements upon the major systems can be defined and options for these systems can be examined on a consistent basis; and the background information upon which a conceptual design should be based can be developed in an appropriate context. The first section of this chapter summarizes the ANL-TEPR preliminary conceptual design. Subsequent sections summarize various aspects of the design and scoping study.

A. Reference Design

On the basis of trade-off studies among the plasma-power objectives and uncertainties, the toroidal-field coil design, the plasma heating and driving systems requirements, the radiation and energy attenuation requirements, and access considerations, a reference point was selected for the development of the engineering design. This reference point was sized on the basis of a circular plasma cross section. A $\kappa = 3$ (3:1) noncircular plasma design utilizing the same toroidal-field coil system and having the same plasma volume was developed in parallel, but in less detail.

A perspective view of the TEPR reference design is shown in Figure II-1, for the circular plasma, and the geometrical parameters are tabulated in Table II-1. Vertical section views are presented in Figures II-2 and II-3 for the circular and $\kappa = 3$ noncircular designs, respectively. A horizontal section view is shown in Figure II-4, and an elevation view is shown in Figure II-5.

The plasma properties during the peak, quasi-steady state portion of the burn cycle are given in Table II-2. The maximum field, $B_{\text{max}}^{\text{TFC}}$, at the toroidal field coils is 75 kG. These properties are for operation

Figure II-1 ANL-TEPR

Figure II-2 ANL-TEPR Vertical Section -- Circular Plasma

Figure 7-3 ANL-TEPR Vertical Section -- Noncircular Plasma

Figure II-4 ANL-TEPR Horizontal Section View

Figure II-5 ANL-TEPR Elevation View

Table II-1 ANL-TEPR Preliminary Conceptual Design Geometrical Parameters

Central core radius, r_v	1.8 m
Thickness of TFC + OHC + Support cylinder, Δ_m	1.05 m
Blanket + shield thickness, inside, $\Delta_B^{in} + \Delta_S^{in}$	1.0 m
Toroidal-field coil minor bore, R_{bore}	7.7 m
Toroidal-field coil major bore, $2Z_{TFC}$	11.9 m

<u>Circular Plasma</u>	
Plasma radius, a	2.1 m
First-wall radius, $r_w = a + \Delta_v$	2.4 m
Major radius, R	6.25 m
Aspect ratio, $A = R/a$	2.98
Plasma volume	544 m ³
Toroidal vacuum-chamber volume	711 m ³
First-wall area	592 m ²
Blanket + shield thickness, outside, $\Delta_B^{out} + \Delta_S^{out}$	1.3 m ^(a)

<u>Noncircular Plasma ($\kappa = b/a = 3$)</u>	
Plasma minor dimension, a	1.3 m
First-wall minor dimension, $r_w = a + \Delta_v$	1.6 m
Major radius, R	5.45 m
Aspect ratio, $\bar{A} = R/a\ell$	1.9
Plasma volume	545 m ³
Toroidal vacuum chamber volume	826 m ³
First-wall area	770 m ²
Blanket + shield thickness, outside, $\Delta_B^{out} + \Delta_S^{out}$	1.3 m ^(a)

a. Blanket and shield thickness = 1.3 m. The space available within the toroidal-field coil is 1.9 m for the circular plasma and 3.5 m for the noncircular plasma.

Table II-2 TEPR Steady-State Plasma Properties at Equilibrium

Parameter	Circular Plasma $\kappa = 1$	Noncircular Plasma $\kappa = 3$
Pressure ratio, β_0	2.2	2.0
Safety factor, $q(a)$	2.5	4.0
Density, n_1 (m^{-3})	0.56×10^{20}	0.65×10^{20}
Temperature, T_1 (keV)	9.6	9.6
T_e (keV)	10.0	10.0
Confinement, $n\tau$ (sec/ m^3)	4.2×10^{20} (10 x TIM)	4.2×10^{20} (5 x TIM)
Plasma current, I_p (MA)	4.8	7.6
Toroidal field, B_t (kG)	34	39
Power, P_T (MWt)	129	174
Neutron wall load, P_w (MW/ m^2)	0.16	0.17

at ignition, which requires energy confinement ten times that predicted by TIM for the circular plasma, and five times TIM for the noncircular plasma. If the achievable energy confinement turns out to be less than this, the reactor will be operated in a beam-driven mode to achieve essentially the same properties. The noncircular plasma design has a more conservative value of $q(a)$ than does the circular design — the power output of the noncircular design would be substantially greater if operated at $q(a) = 2.5$.

The power performance of the TEPR and the requirements upon the plasma driving and heating systems are summarized in Table II-3, for the circular plasma design. The plasma is heated ohmically during the current rise, but 40 MW supplemental neutral-beam (D) heating is required to reach the operating temperature. If energy confinement is adequate for ignition, the beam heating is terminated once the operating temperature is achieved;

Table II-3 TEPR Burn Cycle Performance -- Circular Plasma

Burn Cycle

Current-rise phase (sec)	1
Beam-heating phase (sec)	~ 3
Burn phase (sec)	20-50
Shutdown phase (sec)	5
Exhaust & Replenishment phase (sec)	~ 15

Neutral-Beam Requirements

Startup - 40 MW @ 180 keV for ~ 3 sec
 Burn* - 23 MW @ 180 keV for 20-50 sec

Plasma Driving-System Requirements

$\Delta\phi$ (volt-sec)	~ 100
Peak power (MWe)	~ 1000
Energy (MJ)	~ 300

Reactor Performance ($B_{\max}^{\text{TFC}} = 70 \text{ kG}$)

	<u>$n\tau = 15 \times \text{TIM}$</u>	<u>$n\tau = \text{TIM}$</u>
Electrical power, $\bar{P}_E$ (MWe)	~ 25	~ 25
Net electrical power, $\bar{P}_{\text{NET}}$ (MWe)	15-20	~ -17

* Required if $n\tau = \text{TIM}$.

otherwise a supplemental heating beam is used to maintain the power balance during the burn phase. A constant plasma density is maintained during the startup and burn phases by plasma refueling and recycling. The burn-cycle length will be determined by the length of time that a stable plasma can be confined or by the buildup of impurities. Burn cycles in the range of 10 - 60 seconds are analyzed, but the plasma driving system is adequate for much longer burn cycles. The cycle-averaged electrical power (assuming a thermal-to-electrical conversion efficiency of 0.3) is relatively insensitive to the confinement, but the cycle-averaged, net electrical power (assuming an efficiency of 0.5 for beam production and an efficiency of 0.95 for the plasma driving-system power supply) is positive only if the confinement is adequate for, or very close to, ignition. Sputtering from a stainless steel first-wall would extinguish the plasma and preclude a reasonable power performance. The power performance is only slightly degraded from that indicated in Table II-3 if the first-wall is coated with graphite, but a silicon-carbide or aluminum first-wall surface results in shorter burn cycles and degraded power performance. The power performance shown in Table II-3 is based on a conservative value of $B_{\max}^{\text{TFC}} = 70 \text{ kG}$ -- using the design value of $B_{\max}^{\text{TFC}} = 75 \text{ kG}$ increases $\bar{P}_E$ by $\approx 30\%$ and has a corresponding beneficial effect upon $\bar{P}_{\text{NET}}$.

A secondary function of the TEPR is to serve as a radiation facility. The TEPR was sized, using a plausible set of design parameters, to obtain the objective of a rather modest amount of power, with the result that the 14 MeV neutron power load on the first wall is also rather modest, which is an advantage from the viewpoint of radiation damage to the reactor, but is a disadvantage in terms of using the TEPR as a radiation facility. In order to obtain some indication of the maximum possible neutron-flux achievable in the TEPR, an optimistic set of parameters [$q(a) = 2.5$, $\beta_\theta = 3.5$, $B_{\max}^{\text{TFC}} = 80 \text{ kG}$] was considered. If the TEPR could operate with these parameters, it would serve quite well as a radiation facility, as indicated in Table II-4.

Cryogenically stable superconducting toroidal-field, ohmic-heating and equilibrium-field coils are proposed for the TEPR, although conventional copper coils are still under consideration for the latter two sets of coils. The magnet-system characteristics are given in Table II-5. The superconducting material is niobium-titanium, with a copper stabilizer and a

Table II-4 ANL-TEPR 14 MeV Neutron Radiation Characteristics

	Circular Plasma $\kappa = 1$	Noncircular Plasma $\kappa = 3$
<u>Design Parameters</u>		
$B_{\max}^{\text{TFC}}$ (kG)	75.0	75.0
β_{θ}	2.2	2.0
$q(a)$	2.5	4.0
Neutron Wall Load, P_w (MW/m ²)	0.16	0.17
Wall Area, A_w (m ²)	592.0	770.0
Neutron Current, J_w (n/m ² ·sec)	7.2×10^{16}	7.4×10^{16}
<u>Optimistic Parameters</u>		
$B_{\max}^{\text{TFC}}$ (kG)	80.0	80.0
β_{θ}	3.5	3.5
$q(a)$	2.5	2.5
Neutron Wall Load, P_w (MW/m ²)	~ 0.5	~ 4.0
Wall Area, A_w (m ²)	592.0	770.0
Neutron Current, J_w (n/m ² ·sec)	$\sim 2.3 \times 10^{17}$	$\sim 1.8 \times 10^{18}$

Table II-5 TEPR Magnet System Characteristics

Toroidal Field Coils

Superconductor/Stabilizer/Support	NbTi/Cu/SS
Number of coils	16
Shape	pure-tension "D"
Major bore (vertical)	11.9 m
Minor bore (horizontal)	7.7 m
Field in plasma B_t	34 kG
Peak field, B_{TFC}^{max}	75 kG
Current density, J_{avg}	1280 A/cm ²
Operating temperature	4.2°K
Ampere-turns	6.54 x 10 ⁶ per coil 104.6 x 10 ⁶ total
Stored energy	15,600 MJ
Maximum hoop stress in SS support	~ 24,000 psi
Maximum field ripple	~ 2%
Access between coils	3.4 m

Equilibrium Coils

Superconductor/Stabilizer	NbTi/Cu & Cupro-nickel
Current density, J_{avg}	2,300 A/cm ²
Equilibrium field in plasma	3.0 kG
Ampere-turns	10 x 10 ⁶
Stored energy	90 MJ
Volt-seconds to plasma	37 V-sec
Peak field	37 kG
Field rise in conductor	37 kG/sec

Ohmic-Heating Coils

Superconductor/Stabilizer	NbTi/Cu & Cupro-nickel
Current density, J_{avg}	1,280 A/cm ²
Peak field, B_{OH}	32 kG
Ampere-turns	45 x 10 ⁶
Stored energy	883 MJ
Volt-seconds to plasma	73 V-sec
Field rise in conductor	64 kG/sec

stainless steel support structure. There are 16 pure-tension "D"-shape toroidal-field coils, which provides an access between coils of ~ 3 m with a maximum field ripple of $\sim 2\%$. The average current density is 1280 A/cm^2 , which produces a peak field of 75 kG at the inside of the "D". This value of 75 kG was chosen to allow ~ 5 kG stray fields from other magnet systems and from fields due to the plasma current without exceeding the ~ 80 kG limit for niobium-titanium at 4.2°K . The maximum hoop-stress level in the support system for the toroidal-field coils is $\lesssim 24,000$ psi, and the stored energy is $< 1,000$ MJ per coil.

Both the ohmic-heating and the equilibrium-field coils are located external to the toroidal-field coils. This arrangement has certain advantages with respect to reduced radiation and thermal loads, ease of assembly, reduced pulsed fields on the steady-state, toroidal-field coils, and ease of producing the required vertical field. The disadvantage of this location is poorer coupling to the plasma. These two sets of coils are capable of producing 110 volt-sec in the plasma, with $1/3$ of this being produced by the equilibrium-field coils.

The performance requirements for the neutral-beam injection system are summarized in Table II-6. Ion-source currents (total, not per source) are given for injection of all three components (D^+ , D_2^+ , D_3^+) and for injection of only the D^+ component. An injection system consisting of 16 or 32 injectors, with counter- and co-injection in the horizontal mid-plane and each capable of delivering 2.5 or 1.25 MW, would satisfy these requirements. Details of the injection system can not be specified at this time, but it is assumed that this system will be based upon positive-ion sources of the type presently under active development.

Toroidal-chamber and neutral-beam system vacuum requirements are indicated in Table II-7. The toroidal vacuum system was sized to pump down from 10^{-3} to 10^{-5} Torr during the ~ 15 sec exhaust phase between burn pulses and to achieve $\sim 10^{-9}$ Torr base pressure following a ~ 24 hour bake at 400°C at the beginning of an experimental period. The neutral-beam vacuum system was sized to handle the gas load that would be required in each beam for 40 MW of injected power.

The energy storage and transfer portion of the ohmic-heating system power supply is a major design concern, since the energy of the coil is

Table II-6

Neutral-beam Injection System Requirements (Nominal)

Heating

Injected power (total)	40 MW
Ion-beam composition	75% D^+ , 18% D_2^+ , 7% D_3^+
Ion-source current	890/1600 A*
Ion-source power	160/290 MW*
Ion-beam energy	180 keV
Pulse duration	3 sec
Number of injectors	16-32

Supplemental Heating (Beam-driven Mode)

Injected power	23 MW
Ion-beam composition	75% D^+ , 18% D_2^+ , 7% D_3^+
Ion-source current	510/920 A*
Ion-source power	92/170 MW*
Ion-beam energy	180 keV
Pulse duration	20-50 sec
Number of injectors	16-32

* D^+ , D_2^+ , D_3^+ injection/ D^+ injection

Table II-7 TEPR Vacuum Systems

Toroidal Vacuum System

Volume	711 m ³ (897 with duct)
Surface area	592 m ² (1,528 with duct)
Gas load after burn	704 Torr/l (888 with duct)
Number of pumping ports	32
Diameter of port	0.914 m
Number of cryosorption pumps	32 (64 for on-line regeneration)
Pump speed	25,000 l/sec (rated)
Pumpdown time 10 ⁻³ to 10 ⁻⁵ Torr	14 sec (with duct)
Secondary pumps:	
Number of 1,300 CFM blower stations	16
Number of 1,400 l/sec turbo pumps	16

Neutral-Beam Vacuum System

Number of beams	16 minimum
Gas load/beam	21 Torr-l/sec*
Pumping speed/beam	200,000 l/sec*
Number of cryosorption panels	4 per beam*
Pump size	1 m ² cryosurface per 50,000 l/sec

* 16 injectors and 3-component (D⁺, D₂⁺, D₃⁺) injection

about 10^9 J. The maximum voltage and current that must be handled during the burn cycle are $\sim 140,000$ volt and $\sim 40,000$ A, respectively. Conventional energy-storage systems will be too costly or inefficient. Homopolar generators and superconducting magnetic-energy transfer systems are being examined.

The characteristics of the primary-energy conversion system (PECS) are outlined in Table II-8, a vertical-section view of the blanket and shield arrangement is shown in Figure II-6, and the performance of the PECS is summarized in Table II-9. A stainless steel first wall with a graphite coating or liner interfaces with the plasma. Alternating zones of stainless steel and boron-carbide constitute the blanket and shield in the primary design. An alternate design, in which the inner blanket/shield consists of alternating zones of a tungsten- or tantalum-base alloy and boron-carbide to enhance the radiation attenuation, is also considered. The dimensions and compositions of the alternating zones are optimized to maximize radiation attenuation for one meter of the primary (SS-B₄C) inner blanket-shield design.

Ninety-nine percent of the nuclear heating occurs in the first 0.4 m of the blanket/shield. The nuclear heating in the superconducting toroidal-field coils is comparable to the non-nuclear heat leaks and causes a maximum temperature rise in the superconductor that is less than 0.05°K.

The toroidal-field coil is designed to accommodate an increase in resistivity of the copper stabilizer, due to irradiation of, 2.5×10^{-8} Ω -cm. Allowing a factor of 10 for safety, the primary-shield design would allow the TEPR to operate at 0.2 MW/m^2 wall load for ten years with a duty factor of 50% before this criterion was violated, at which time annealing would be necessary.

Sensible heat removal from a blanket operating at $T_{\text{max}} = 600^\circ\text{C}$ with either water or helium as a coolant is feasible. With helium, average void fractions of 5% in the blanket region and 2% in the shield are required, and ~ 7 MWe is required to pump the coolant through the blanket. Neither coolant volume fraction nor pumping power are significant with water.

First-wall and toroidal-field coil radiation damage parameters are given in Table II-10. A stainless steel first wall is expected to maintain its structural integrity for integrated wall loadings in excess of 1 MW-yr/m^2 ,

Table II-8
TEPR Primary Energy Conversion System

Blanket and Shield Thickness	
Inside	1.0 m
Outside	1.3 m
Blanket and Shield Composition	
Inside	SS-B ₄ C or W-alloy-B ₄ C or Ta-alloy-B ₄ C
Outside	SS-B ₄ C
First Wall	1 cm SS (50% density), 1-4 mm carbon or silicon-carbide coating
Coolant	
H ₂ O (136 atm)	
T _{in}	38°C
T _{out}	302°C
He (50 atm)	
T _{in}	357°C
T _{out}	527°C
Biological Shield	0.97 m concrete

Figure II-6 ANL-TEPR Blanket/Shield Vertical Section View

Table II-9

TEPR Primary Energy Conversion System Performance

First-wall Neutron Loading (PECS design value ^a)	0.2 MW/m ²
Nuclear Heating, Maximum	
First wall	2.1 W/cm ³
Blanket	2.0 W/cm ³
Shield	2×10^{-2} W/cm ³
Magnet (TFC)	5×10^{-6} W/cm ³
Nuclear Energy Deposition Rate in TFC	
Per coil	16 W
Total (16 coils)	256 W
Maximum Radiation-induced Resistivity in Cu Stabilizer of TFC (10 MW-yr/m ²)	2.5×10^{-8} Ω -cm
Maximum decrease in Critical Current Density in TFC Superconductor (10 MW-yr/m ²)	< 5%
Maximum Blanket Temperature	600°C
First-wall Temperature	$\leq 600^\circ\text{C}$
Coolant Pumping Power (Blanket only)	
H ₂ O	$\ll 1$ MW
He	~ 7 MW
Biological Dose Outside Biological Shield	1 mrem/hour

^a The PECS design neutron wall loading used in the PECS performance evaluation is 25% larger than the nominal design value, for conservatism.

Table II-10 Radiation Damage in TEPR

<u>First-wall (maximum)</u>	
Displacements	11 dpa/(MW-yr/m ²) 1.1 dpa/yr*
Helium Production	216 appm/(MW-yr/m ²) 22 appm/yr*
Hydrogen Production	531 appm/(MW-yr/m ²) 53 appm/yr*
Erosion	~ 0.1 mm/yr*
<u>Toroidal Field Coil (maximum)</u>	
Displacements in Copper	1.5 x 10 ⁻⁵ dpa/(MW-yr/m ²) 1.5 x 10 ⁻⁶ dpa/yr*
Neutron Fluence	4.7 x 10 ¹⁵ n/cm ² -yr*
Absorbed Dose in Mylar	4.0 x 10 ⁷ rad/(MW-yr/m ²)

* 0.2 MW/m² neutron wall load and 50% duty factor.

which would permit over ten years of operation at 0.2 MW/m^2 with a duty factor of 50%; less than 5% swelling and a uniform ductility of over 1% are expected for annealed material. Wall erosion by sputtering and blistering will not seriously affect the mechanical integrity of a stainless steel first wall under the anticipated particle currents; however, the plasma contamination will be excessive. Further work is necessary to develop a low-Z coating on stainless steel that will withstand the severe radiation, thermal and chemical environment of the TEPR. The major radiation-damage effect on the toroidal-field coils is to increase the electrical resistivity in the copper stabilizer. Adequate shielding has been provided in the design to limit this effect to an acceptable level.

A preliminary study has been carried out to establish criteria for the design and operation of an integrated facility that meets the tritium-handling requirements of a TEPR. For purposes of assessment, the tritium-handling facility for Stage I operation has been broken down into six major systems, which provide for fuel delivery, fuel circulation, fuel processing, fuel storage, in-plant containment and purge processing. Stage II operation requires all six of these systems plus an additional system to provide blanket processing. Table II-11 contains a summary of preliminary fuel-cycle operating parameters for the TEPR design approach summarized in this chapter.

Table II-11

Fuel Cycle Parameters for the Preliminary TEPR Design

Assumed cycle-averaged power	100 MWt
Tritium burn-up rate	16 gram/100 MWt•day
Deuterium burn-up rate	11 gram/100 MWt•day
Maximum through-put to burn-up ratio	100/1
Fuel insertion rate	2700 gram/100 MWt•day
Maximum fueling costs for a 30% plant factor:	
For tritium	\$15 M/year
For deuterium	\$5 K/year

B. Reactor Performance

The performance of the TEPR will depend upon a number of plasma-physics factors that are uncertain at this time. Foremost among these are the energy confinement and the impurity concentration that will exist in a reactor-grade plasma and the MHD-stability characteristics, particularly of noncircular cross-section plasmas. An envelope of performance characteristics was established in order to bracket these uncertainties, then a reference design point was selected for more detailed performance analysis and for the engineering design of the TEPR (see Chapter III).

To establish the performance envelope, a series of steady-state operating conditions (corresponding to the steady-state portion of the burn pulse) were determined as a function of achievable energy confinement and impurity concentration. In these calculations, $B_{\max}^{\text{TFC}} = 70 \text{ kG}$, $\beta_\theta = 2.2$ and $q = 2.5$. Supplemental neutral-beam (D) injection heating was utilized when the energy confinement was inadequate for ignition. The results are summarized, for a relatively clean plasma, in Figure II-7. The total thermal power, P_T , the supplemental beam power, P_B , the neutron wall load, P_W and J_W , and the plasma power amplification factor, Q_p , are plotted as a function of $n\tau$ for the D-T mixture which optimizes each quantity. Ignition requires $n\tau \gtrsim 4 \times 10^{20} \text{ sec/m}^3$ and net electrical power production probably requires $n\tau \gtrsim 3 \times 10^{20} \text{ sec/m}^3$, for a relatively clean plasma. The presence of impurities increases the values of $n\tau$ that are required for these two conditions and may preclude their being obtained, if the impurity is of high enough Z and/or is present in sufficient concentration. However, production of thermal-power output during the burn pulse in the range $\sim 100\text{--}300 \text{ MWt}$, with a corresponding neutron wall load of $\sim 0.1\text{--}0.3 \text{ MW/m}^2$, can be achieved over a range of possible values of $n\tau \gtrsim 10^{19} \text{ sec/m}^3$ with supplemental heating. Allowing a factor of 1/3 reduction for thermal-to-electrical conversion and a factor of 1/2 reduction for burn-cycle duty factor, the TEPR power objective (5-50 MWe) should be achieved. This power production capability is greater for values of $B_{\max}^{\text{TFC}} > 70 \text{ kG}$ and for noncircular plasma cross sections. It is relatively unaffected by the presence of impurities, if adequate supplemental heating is provided.

Steady-state performance parameters for a circular and a noncircular reference design are shown in Table II-12. The amount by which the

Figure II-7 Several Performance Parameters of the TEPR as a Function of $n\tau$ for 0.5% ¹⁶⁰ Impurity ($q = 2.5$, $\beta_\theta = 2.2$)

Table II-12 Equilibrium Performance Characteristics

Q_p	$n\tau$ (10^{20} sec/ m^3)	α_{TIM}	I (MA)	$\Delta\phi_{ind}$ (V·sec)	n_i (10^{20} m^{-3})	T_i (keV)	P_T (MW)	P_B (MW)	P_W (MW/ m^2)
(Circular plasma - $\beta_p = 2.2$, $q = 2.5$, $Z_{eff} = 1.3$, $T_e = 10$ keV, $B_{max}^{TFC} = 75$ kG)									
Ignition	4.2	10	4.8	54	0.56	9.6	129	0	0.16
6.5	1.7	5	4.8	54	0.54	10.2	171	23	0.18
1.0	0.057	3	4.8	54	0.16	19.6	294	146	0.18
($\kappa = 3$ Noncircular plasma - $\beta_p = 2.0$, $q = 4.0$, $Z_{eff} = 1.3$, $T_e = 10$ keV, $B_{max}^{TFC} = 75$ kG)									
Ignition	4.2	5	7.6	63	0.65	9.6	174	0	0.17
6.5	1.7	2	7.6	63	0.62	10.2	230	31	0.19
1.0	0.057	2	7.6	63	0.18	19.6	395	196	0.19

reactor confinement must exceed the TIM prediction, α_{TIM} , and the plasma power amplification factor, Q_p , are given for a range of confinements. The total thermal power, P_T , the supplemental beam power, P_B , and the neutron wall load, P_W , are shown. At ignition the reference, circular plasma cross section design produces a thermal-power output of 129 MWt and a neutron wall load of 0.16 MW/m^2 during the peak of the burn pulse. Larger power outputs would be obtained in a beam-driven mode (if the confinement turns out to be insufficient for ignition) and for the $\kappa = 3$ noncircular plasma cross section design. A plasma-driving system that produced ≈ 100 volt-sec would provide an ample margin for plasma resistive losses in addition to the $\Delta\phi_{ind}$ needed to induce the plasma current.

The burn-cycle dynamics of the TEPR were examined in some detail. The circular-plasma design, but with $B_{max}^{TFC} = 70 \text{ kG}$ and $P_T = 98 \text{ MW}$ to be pessimistic, was analyzed to obtain a lower bound on the power performance. The basic burn cycle consisted of: 1) a 1 second current-rise phase; 2) a ~ 3 second heating phase with 40 MW of neutral deuteron beams; 3) a burn phase of variable length; and 4) a 5 second shutdown phase. The cycle-average, electrical power output, $\bar{P}_E$, computed on the basis of a thermal-to-electrical conversion efficiency of 0.3, and the cycle-average net-electrical power, $\bar{P}_{NET}$, accounting for the neutral beam being produced with an efficiency of 0.5 and for the plasma driving system power supply functioning with an efficiency of 0.95, are shown in Figure II-8, as a function of the length of the burn cycle. If the confinement is sufficient for ignition at $T_e = 10 \text{ keV}$ ($\alpha_{TIM} = 15.2$), $\bar{P}_{NET} \gtrsim 20 \text{ MWe}$ is possible for burn cycles of $\gtrsim 45$ seconds or more. (Down time between burn pulses and power needed for coolant pumping, refrigeration, etc., would reduce $\bar{P}_{NET}$.) If the confinement turns out to be TIM ($\alpha_{TIM} = 1$), then operation at $T \gtrsim 6 \text{ keV}$ with supplemental beam heating of $\gtrsim 23 \text{ MW}$ during the burn phase will yield the best power performance. The $\bar{P}_E$ that can be obtained with $\alpha_{TIM} = 1$ in the beam-driven mode is comparable to the value which can be obtained with $\alpha_{TIM} = 15.2$ in the ignition mode, but $\bar{P}_{NET} < 0$ for the beam-driven mode. Larger values of $\bar{P}_E$ and $\bar{P}_{NET}$ would be obtained for $B_{max}^{TFC} > 70 \text{ kG}$ and for the noncircular design.

When wall-sputtered impurities are included in the analysis, the power performance may be substantially altered, depending upon the wall material. For a graphite wall coating or liner, the power performance is only slightly

Figure II-8 Cycle-average Electric Power as a Function of Confinement

degraded by wall-sputtered impurities. On the other hand, impurities sputtered from a stainless steel wall would entirely preclude the possibility of $\bar{P}_{NET} > 0$ and would, for reasonable levels of supplemental beam heating during the burn phase, limit the burn-cycle duration to less than ≈ 20 seconds. With a silicon-carbide or aluminum wall, the power performance is degraded and the burn cycle is limited to less than ≈ 40 seconds, but $\bar{P}_{NET} > 0$ is possible.

The burn-cycle simulations were carried out under a variety of conditions in order to determine the requirements upon the plasma heating and driving systems. For the circular plasma design: a) the neutral-beam injection system must provide 40 MW of 180 keV beam power for 3 seconds; b) the plasma-driving system must provide ≈ 250 -300 MJ of energy, with a peak power requirement of ≈ 1000 MW.

C. Magnet System

The TEPR magnets analyzed in this design study consist of the toroidal-field coils (TF), the equilibrium-field coils (EF) and the ohmic-heating coils (OH). The complete magnet design is described in Chapter IV. The objectives of this design study are two-fold: 1) to develop a completely integrated design; and 2) to establish the requirements for the research and development program necessary to support the design and construction of the TEPR magnets. The design approach taken here is a systems approach to design magnets that are an integral part of the system, rather than to develop an independent magnet design that does not fully take into account all the problems of a TEPR. Although this is a design-scoping study, the design of each magnet system is sufficiently developed so as to provide enough information about the main problems of each magnet system to indicate the necessary research and development program. The required research and development program is described in Appendix G.

The proposed toroidal coils are fully-stabilized superconducting magnets with NbTi as the superconductor and copper as the stabilizer. The toroidal-coil shape has a pure-tension profile. The proposed equilibrium coil and the ohmic-heating coil are fully-stabilized, ac fast-pulsed magnets with NbTi as the superconductor and cupro-nickel and copper as the stabilizer. Both EF coils and OH coils are located outside the TF coil system. Equilibrium copper coils and ohmic-heating copper coils are also studied. The I^2R loss for copper coils is much larger than for the superconducting coils.

We conclude that a TEPR will have a better chance for breakeven, or net-power output, if both the EF coils and the OH coils are superconducting. The main characteristics of the TF coils, the EF coils and the OH coils are given in Figure II-9.

1. Toroidal-Field (TF) Coil System

Because the power density is proportional to B_t^4 , it is clear that the toroidal field level must be as high as possible to minimize the TEPR size for a given power. The toroidal-field coils must be superconducting in order to achieve net power out. NbTi is the best superconductor to use because of its ductility. The specification of a peak field of 75 kG (at 4.2°K) at the magnet winding is based on a practical judgment concerning some degree of margin required in a real system subjected to the superimposed rapidly-pulsed field of the order of 5 kG from the equilibrium coil, the ohmic-heating coil and the plasma current. Furthermore, the local temperature fluctuation during operation due to the pulsed field dissipation and the nuclear heating must be allowed for in the design.

One of the major problems with large toroidal coils is that of support. If the coil profile is circular, the nonuniform Lorentz forces will subject the TF coils to large bending stresses. Consequently, for a given coil structure, the coil reaches its stress limit before it reaches the peak field limit of NbTi. The minimum stress condition exists when the coil is in pure tension with no bending moments. We have developed a new analytical approach to determine a pure-tension coil profile, taking into account the number of coils, the coil cross section and the gap size between two adjacent coils. The pure-tension computational model is described in Appendix E. The three-dimensional stress analysis verified that our coils do indeed approach the pure-tension criterion with a peak stress of 20,000 psi and an average stress of 16,000 psi. For comparison, if the coils are circular, the peak stress will be 70,000 psi for the same inner and outer toroid dimensions (R_{10} and R_{20} in Figure II-9). If the coils have the so-called Princeton "simple D" shape, the peak stress will be 60,000 psi.

The determination of the TF coil size was the result of an interactive process involving the plasma physics and power performance, the blanket and

Figure II-9 Schematic of TEPR Magnets Design

shield design, requirements for the OH coils and the access requirements. For radiation protection, the thickness of the inner blanket and shield is 1 m. The inner radius (R_{10} in Figure II-9) was determined by the requirement that the central-core radius be sufficiently large so that the OH coil field need not be too high, and by the requirement that the plasma volume be sufficiently large to meet the power objective of the TEPR. For the design, the power objective can be satisfied by a circular plasma with a major radius $R_0 = 6.25$ m and minor radius $a = 2.1$ m. The next dimensional requirement is the 3 m spacing between coils for neutral-beam injectors and for access for installation and maintenance of the first wall, blanket and shield. These requirements can be satisfied with 16 TF coils having an R_{bore} of 7.7 m and a maximum field ripple of 2%. The characteristics of the TF coils are given in Table II-13.

For a peak field of 75 kG, the average coil stress is 16,000 psi. A reasonable stress limit for the copper is about 12,000 psi. To maintain the stress level for copper with an average coil stress of 16,000 psi, the coil volume is roughly divided into one part copper, 0.5 part stainless steel and 0.1 part coolant passage. The resulting stress in stainless steel is 24,000 psi.

Since the toroidal field decreases rapidly across the coil winding, the conductor will be made in three grades. Conductor design is based upon the current-carrying capacity of NbTi at 5.2°K and peak fields of 76 kG, 60 kG or 45 kG, depending on the grade of the conductor. The maximum radiation-induced resistivity in the copper is $2.5 \times 10^{-8} \Omega\text{-cm}$ for an integrated wall loading of 10 MW-yr/m^2 . This induced resistivity decreases to $5 \times 10^{-9} \Omega\text{-cm}$ at the outer zone of the coil. The electrical insulation is mylar and the supporting material is stainless steel. The conductor specifications are developed in detail in Chapter IV.

The basic coil unit will be a pancake module. The bobbin, having a pure-tension profile, is stainless steel casting, 2.5 cm thick all the way around. Each pancake will be wound directly on the bobbin. Between two pancake coils, a spacer of 0.4 cm in thickness is provided for force transmission and liquid-helium flow between the pancakes. Each pancake has 70% of both edges cooled. In addition, 50% cooling is provided on one face to fulfill the full-stabilization criterion. The stainless steel support material will be distributed uniformly throughout the coil. This method of reinforcing provides the best stress distribution throughout the winding.

Table II-13 Superconducting Toroidal-field Coils

Superconductor/Stabilizer/Insulator/Support	NbTi/Cu/Mylar/SS
Number of coils	16
Coil shape	Pure tension
Field ripple	2%
Maximum access	3.4 m
Operation temperature	4.2°K
Peak field due to TF coils	75 kG
Superimposed AC field	3 to 5 kG
Vertical bore	11.88 m
Horizontal bore	7.7 m
Field in plasma, B_t	34 kG
Operational current	10,000 A
Stored energy	975 MJ/coil, 15,600 MJ total
Inductance	19.5 H/coil, 312 H total
Ampere - turns	6.54×10^6 /coil, 104.64×10^6 total
Turns per coil	654
Pancakes per coil	21
Mean turn length	34.4 m
Total conductor length	22,500 m/coil
Approximate weight	175 tons/coil, 2,800 tons total
Coil & bobbin cross section,	56.8 cm (radial) x 90 cm (axial)
Winding cross section,	51.8 cm (radial) x 85 cm (axial)
Conductor width	3.6 cm
Conductor thickness, inner zone	1.35 cm
central zone	1.217 cm
outer zone	1.0 cm
Stainless steel thickness, inner zone	0.6 cm
central zone	0.5 cm
outer zone	0.5 cm
Conductor current density, inner zone (5 turns)	2459 A/cm^2
central zone (6 turns)	2572 A/cm^2
outer zone (20 turns)	3223 A/cm^2
Average current density	1260 A/cm^2 average over bobbin & coil
	1486 A/cm^2 average over coil winding
	2378 A/cm^2 overall average conductor

An ac field of approximately 3 kG will be superimposed on the TF coils by the OH coil, the EF coil and the plasma. Assuming a 10 μ filament diameter for the conductor and a typical fusion cycle of 35 sec during which the ac field makes a swing of 0 $\rightarrow$ 3 kG $\rightarrow$ 0, the hysteresis loss per coil is 2700 J and the average power dissipation is 77 W/coil at 4.2°K. Assuming a reasonable short-twist pitch of 5 cm, no field shielding and $\dot{B} = 3$ kG/sec, the matrix loss per coil over a fusion cycle is 119,400 J, or 3.411 kW/coil. However, if a field shield is used to slow down the flux-diffusion rate by a factor of 10, the matrix eddy-current loss will be 11,940 J/coil, or 341 W/coil. To reduce the matrix loss further, the conductor could be made of a cable consisting of transposed multiple wires. Then the twist pitch could be reduced by a factor of 5, thus reducing the matrix loss by a factor of 25. The nuclear heating contributes 16 W/coil and the cryostat heat leak is about 13 W/coil. The total refrigeration requirement, with a field shield but without using transposed cable, is about 10 kW of refrigeration at 4°K.

The straight sections of the 16 TF coils exert a total centering force of 642×10^6 pounds onto a central support cylinder having an inner radius of 2 m and an outer radius of 2.282 m. The cylinder is made of laminated stainless steel so that the eddy-current losses are reduced. The radial compression pressure is about 4,000 psi and the maximum circumferential stress of the cylinder is 34,000 psi. Since the cylinder is at 4.2°K, this stress level poses no problem.

A series-connected coil protection scheme allows the stored energy to be rapidly dumped to a discharge resistor outside each TF coil to prevent damage under malfunction conditions. The series connection also ensures that each coil carries equal current at all times so that no additional large forces are generated during a magnet quench.

Finally, the field interaction between the TF coil and the equilibrium field coil is discussed in Appendix E.

2. Equilibrium-Field (EF) Coil System

The required equilibrium field for the Argonne TEPR is 3 kG. For stability against radial displacement, $|\partial B_{vz}/\partial R|$ must be less than 3.6 G/cm, where B_{vz} is the vertical-field component and R is the toroidal radius. Note that $\partial B_{vz}/\partial R$ must always be negative. For stability against vertical displacement, the vertical-flux lines must be concave toward the toroidal axis.

The equilibrium coils were carefully arranged so that the vertical field satisfies these plasma-stability requirements. Furthermore, they are arranged so that they are decoupled magnetically from the ohmic-heating coil and yet produce a bonus of 37 volt-sec for the plasma, thus reducing the volt-sec requirement for the ohmic-heating coils. After trying various equilibrium coil locations, we found that if they are located external to the TF coil, the coil arrangement is very flexible, the assembly and disassembly is easier, and the pulsed field on the TF coil can be reduced. Furthermore, since the EF coils are superconducting, their current-carrying ability is affected by the magnetic flux. Thus, if they were located inside the TF coils, they would require a high percentage of NbTi superconductor. Based on these requirements and considerations, the equilibrium-coil configuration shown in Figure II-9 was chosen. The magnet characteristics are tabulated in Table II-14, and the vertical-field pattern is indicated in Figure II-9.

The equilibrium-coil operational current is 15,000 A, with a charging time of 1 sec and a field excursion of $0 \rightarrow 37$ kG. The fast pulsing suggests that the filaments must be small, twist-pitch must be short and a three-component composite with a highly-resistive barrier of cupro-nickel between filaments must be used. The high current and fine filaments suggest that the 15,000 A conductor cannot be made of one strand. Multiple-strand cable must be transposed to ensure good current sharing. Because of the large stored energy, the equilibrium coil must be fully stabilized. This means that the strand diameter cannot be too large in order to ensure an adequate surface-to-volume ratio for cooling. With these considerations and based on careful design calculations, the required composite diameter is found to be 0.6 mm, filament size is 5 μ m, twist-pitch is 6 mm and the number of composites in the cable is 243. Two cable schemes were examined and full stability was verified. The temperature rise and the amount of cooling during the field-swing process were estimated. For a complete fusion cycle of 35 sec, the total hysteresis loss is 22,637 J, the total matrix loss is 4,353 J, the total self-field loss is 224 J and the ac pulsing loss of the equilibrium coil is 27,214 J. The average power dissipation is 777 W at 4.2°K or 233 kW at 300°K. In comparison, the water-cooled-copper equilibrium coil will have an average power dissipation of 92 MW.

Table II-14 Superconducting Equilibrium Coils

Superconductor/Stabilizer	NbTi/Cu & cupro-nickel
Average current density	2300 A/cm ²
Equilibrium field	3.0 kG
Peak field	~ 37 kG
Ampere turns	10 x 10 ⁶
Conductor length	727 x 10 ⁶ amp-m
Field rise in conductor	37 kG/sec
Stored energy	90 MJ
Operation of current	15,000 A
Inductance	0.8 H
Power supply voltage	37.7 kV
Volt-sec to plasma	37 volt-sec
<u>15,000 A Cable:</u>	
Composite diameter	0.6 mm
Composite operational current	61.7 A at 4.2°K
Composite critical current	123.4 A at 4.2°K
Composite composition	NbTi: Cu: Cupro-nickel = 0.23: 0.57: 0.2
Twist pitch	6 mm
Matrix resistivity	10 ⁻⁶ Ω-cm
Filament diameter	5 μm
Number of filaments in composite	3307
Number of composites in cable	243
Total composite volume	3.33 m ³
Total cable length	48,480 m

3. Ohmic-Heating (OH) Coil System

Based on the burn-cycle dynamics study (see Chapter III), it appears that the required resistive volt-sec is less than the inductive volt-sec. Thus, the assumption that the required volt-sec is $2 L_p I_p$ is conservative and one which should provide some margin. L_p was computed to be 12.273 μH . With $I_p = 4.5 \times 10^6$ A, the total volt-sec is 110. (I_p depends upon the peak toroidal field and the safety factor, but 110 volt-sec is a representative requirement.) Since the equilibrium coils supply 37 volt-sec, the required volt-sec for the ohmic-heating coil is 73. The flux-core radius is 1.9 m, so that the required central field is 32 kG, provided the coil is superconducting. The air-core transformer has the advantage of not requiring a massive iron core and, above all, leads to a smaller core-size requirement. The air-core superconducting transformer is chosen as the preferred design over the air-core, water-cooled copper coil because it has a smaller power loss. The stored energy of the superconducting OH coil is around 890 MJ. Because of the large stored energy, the OH coil is designed to be fully stabilized.

The OH coils are located outside the TF coil system. The OH-coil windings are so arranged that they impose a minimum field and torque on the TF coils. It is also necessary that the OH-coil fields in the plasma-column region be nearly zero so that the plasma can be initially ionized. The winding configuration is shown in Figure II-9. The achieved minimum field in the plasma region is on the order of 10 G. The OH coils superimpose a field of 1 kG on the TF coil. The magnet characteristics are listed in Table II-15.

The OH-coil current is 40,000 A, with a charging time of 1 sec and a field excursion of $-32 \rightarrow +32$ kG; therefore, $\dot{B} = 64$ kG/sec. The considerations in the design of the conductor composite and cable that applied to the EF coil apply as well to the OH coil. The required diameter for the composite is 0.5 mm. The conductor is a three-component composite with copper and cupro-nickel as the matrix.

The 40,000 A cable will have 729 strands, each carrying 54.9 A, with a 5 μ filament diameter and a 6 mm twist-pitch. The ac pulsing losses over a complete fusion cycle of 35 sec are: (1) filament hysteresis loss -- 32,068 J, (2) matrix loss -- 8,800 J, and (3) self-field loss -- 500 J.

Table II-15 Superconducting Ohmic-heating Coils

Superconductor/Stabilizer	NbTi/Cu & cupro-nickel
Average current density	1280 A/cm ²
Central field	32 kG
Peak field	32 kG
Ampere turns	45 x 10 ⁶
Conductor length	628 x 10 ⁶ amp-m
Stored energy	883 MJ
Field rise in conductor	64 kG/sec
Operational current	40,000 A
Self inductance	1.1 H
Mutual inductance	958 μ H
Power supply voltage	140 kV
Volt-sec. to plasma	73 volt-sec
<u>40,000 A Cable:</u>	
Composite diameter	0.5 mm
Composite operational current	54.9 A at 4.2°K, 32 kG
Composite critical current	109.8 A
Composite composition	NbTi: Cu: Cupro-nickel = 0.26: 0.54: 0.20
Twist pitch	6 mm
Matrix resistivity	10 ⁻⁶ Ω -cm
Filament diameter	5 μ m
Number of filaments in composite	2613
Number of composites in cable	729
Total composite volume	2.25 m ³
Total cable length	15,702 m

Thus, the total ac loss per cycle is 42,369 J and the average power dissipation is 1182 W at 4.2°K or 354 kW at 300°K. The cable is designed to be fully stable and the temperature rise for 64 kG/sec is small, even with poor cooling. In comparison, the water-cooled copper OH coil will have an average power dissipation of 4.3 MW.

D. Plasma Support Systems

1. Neutral Injection

Neutral deuteron-beam heating requirements for TEPR have been determined on the basis of burn-cycle dynamics calculations. Nominal neutral beam requirements for the TEPR are given in Table II-6.

The corresponding ion-source, beam-current and power requirements have been determined for four different methods of producing the neutral beam. The most efficient method requires that D^- ions be extracted directly from a deuterium plasma and accelerated to the desired final energy before neutralization. This method, which requires the smallest ion-source currents, has been ruled out for the time being because a source suitable for even the present-day neutral-injection experiments does not presently exist. The second most efficient method, but the one which demands the largest ion-source currents, requires that positive ions (D^+ , D_2^+ , D_3^+) extracted from a deuterium plasma be converted, at low energy (several keV), into D^- ions, which are then accelerated to the required neutral-beam energy before injection into a neutralizer cell. This method has been ruled out for the TEPR not only because a suitable charge-exchange source does not presently exist but, also, because of the very high positive-ion currents that are needed. The least efficient methods require that positive ions be extracted from a deuterium plasma and accelerated to the required energy before injection into the neutralizer. In one scheme, the ions are accelerated to full energy before optional removal of the molecular components of the ion beam. If the option is exercised, the D^+ beam is then neutralized; if not, all three components of the ion beam are neutralized. This method, with all of the ion-beam components neutralized, is the one presently used for neutral injection into CTR devices. We have chosen this method, with optional removal of unwanted molecular ion-beam components and energy recovery, for the reference design.

An ion-beam energy of 180 keV was chosen on the basis of plasma penetration considerations. An energy capture efficiency of 95% was estimated for the TEPR plasma density and dimensions, taking into account the intersection of fast deuteron-ion orbits with the vacuum wall.

The neutral-injection efficiency, ϵ_{NI} , is plotted as a function of the ion beam voltage, V_+ , for one- and three-component neutral beams, with and without energy recovery, in Figure II-10.

To obtain tens of amperes of ion beam from a single source, it is necessary to have large-area extraction systems, and this has led to the use in CTR sources of large, multiaperture or multislot extraction grids. The total amount of extraction-electrode grid area required to produce a given amount of neutral-beam power is shown in Figure II-11 for an extracted-current density (total extracted current/total extraction grid area) of 125 mA/cm²; curves are drawn for several beam voltages for one- and three-component neutral beams, with and without energy recovery.

Source requirements for a 40 MW neutral beam for the four beam-production methods which have been considered are shown in Table II-16. These requirements must be satisfied by 16 or 32 sources.

At the present time, the Berkeley and Oak Ridge ion sources, discussed in Chapter V, are in the forefront of the advance toward the type of source required for TEPR. Thus, they, along with relatively well established vacuum, power and beam-handling (deflecting, focusing) technologies, form the technological basis for the neutral-injector designs. The TEPR design requires significant extrapolations beyond the present state of the art.

2. Vacuum Systems

There will be two vacuum systems associated with the operation of the TEPR: the Toroidal Vacuum System and the Neutral Beam Vacuum System (see Table II-17). These two systems are interdependent to some extent, since beam injection is through ports in the toroidal vacuum vessel. Both systems will be served by a common secondary pumping system for pumping down from atmosphere to operating pressure of the primary pumps. This same secondary system can be used for bake-out cycles, for holding pumps and as a backup system for regeneration of the cryosorption pumps.

The primary pumps being considered for reaching and maintaining operating pressure in the toroidal and neutral beam systems are cryosorption pumps.

Figure II-10 Neutral-Beam Production Efficiency versus Ion-Beam Voltage
 $(D_J^+ \rightarrow D^0)$

Figure II-11 Extraction Grid Area versus Neutral-Beam Power for an Extracted Current Density of 125 mA/cm^2 ($D_j^+ \rightarrow D^0$)

Table II-16 Ion Source Requirements for a 40-MW D^0 Beam

	$D^- \rightarrow D^0$		$D_j^+ \rightarrow D^- \rightarrow D^0$			$D_j^+ \rightarrow D^0$ (3 Components)		$D_j^+ \rightarrow D^0$ (1 Component)	
V_+ (kV)	I_- (A)	P_- (MW)	I_- (A)	$\tau_1 I_- V_-$ (MW)	I_+ (A)	I_+ (A)	P_+ (MW)	I_+ (A)	$\tau_2 \Gamma_1 I_+ V_+$ (MW)
120	708	84.9	884	84.9	5900	830	100	1700	122
160	531	84.9	663	84.9	4420	880	140	1890	182
180	472	84.9	590	84.9	3930	890	160	2030	219
200	425	84.9	531	84.9	3540	900	180	2190	263

^a See Table V-4, p. V-24 for a precise definition of quantities

These pumps are being considered because they reduce the space problem that diffusion pumps present and the more difficult tritium recovery and maintenance necessary with ion pumps and sublimation pumps. They have the ability to generate an inherently "clean" vacuum, which is not contaminated by oil vapors or other impurities, and operation is not immediately affected by power failures. Moreover, cryosorption pumps can vary in configuration so that they may be attached to vacuum systems at almost any point. Special pumps can quite easily be constructed by simple scale-up to almost any imaginable size to achieve desired pumping speeds and mass throughput rates. Another point of consideration is the fact that the necessary cryogen for the cryosorption pumps (i.e., liquid helium and liquid nitrogen) will be readily available, since it must be supplied for the superconducting magnets.

Although cryosorption pumps are a conventional form of pumping, they can be classified here as developmental because of the unavailability, at present, of pumps with the speeds required and the added feature of a closed vacuum system due to the presence of tritium. Even the secondary pumping system must be classified as developmental because of the limited selection of so-called "canned" pumps, which eliminate exposure of seals and thus the possibility of tritium leakage into the atmosphere.

3. Power Supplies

The current in the ohmic-heating coil reverses twice each reactor cycle, causing the total stored energy of the magnet to be transferred into and out of the ohmic-heating coil power supply twice a cycle. Since the energy of the coil is about 10^9 J, the energy storage and transfer portion of the ohmic-heating coil power supply is the dominant feature of the design. The maximum voltage and current which must be handled during the cycle are 140,000 V and 40,000 A. Conventional energy storage systems, such as capacitors and motor-generator-flywheel sets, will be too costly and/or inefficient. Homopolar generators or superconducting magnetic-energy transfer systems might be applicable, but research and development are required.

Relatively detailed designs will be undertaken in collaboration with major equipment manufacturers on those systems that appear most applicable.

Table II-17 Vacuum System Parameters

Toroidal Vacuum System Parameters

Volume	711 m ³ (≈ 897 with exposed duct)
Surface area	592 m ² (≈ 1528 with exposed duct)
Gas load after each burn	704 Torr-liters (888 with exposed duct)
Number of pumping ports	32
Diameter of port	36 in. (91.4 cm)
Number of pumps (cryosorption)	32 (64, if operation is to continue while half the pumps regenerate)
Rated speed/pump	25,000 liters/sec
Effective speed/pump	≈ 12,300 liters/sec
Pumpdown time 10 ⁻³ to 10 ⁻⁵ Torr	10 sec (≈ 14 with duct volume)
Secondary pumps:	
1300 CFM blower stations	16
1400 liter/sec turbopumps	16

Neutral Beam Vacuum System Parameters

Number of beams	16 minimum
Gas load (Q out)/beam	See Table V-5
Pumping speed/beam	See Figure V-13
Pump type	Cryosorption
Pump size	1 m ² cyro-surface for each 50,000 liters/sec
Number of pumps	1/beam, minimum
Secondary pumps	Toroidal secondary system

If a less conventional energy storage system appears feasible, a research and development program should be started so that the feasibility of the technical designs can be tested on a timely basis for the construction of the TEPR.

The power supply for the equilibrium coil also requires an energy transfer unit that is basically similar to the ohmic-heating coil, except that the energy must be stored in the power supply for the duration of the exhaust and replenishment phase of the burn cycle.

The present TEPR design requires 40 MW of 180 keV neutral beam. At an efficiency of 50%, the neutral beam power supply must deliver 80 MW at 180 kV for a duration of 3 sec. A polyphase phase-controlled rectifier system is probably the most straight-forward way in which to generate that much power at 180 kV. Emergency phaseback will be employed to prevent damage to the accelerating column during sparking. Also, in order to prevent loading the power grid, a motor-generator-flywheel set will be used to distribute the power demand.

E. Primary Energy Conversion System

For the purpose of this report, all of the components and hardware that lie between the plasma and toroidal-field coils have been considered collectively under the heading primary-energy conversion system (PECS). Included in this system are: (1) the first wall; (2) the blanket (~ 99% of the neutron and gamma radiation is converted to sensible heat in these two regions); (3) the primary coolant, which removes sensible heat from the blanket; (4) the shield, which backs up the blanket region and provides the additional neutron and gamma attenuation required to protect the toroidal-field coils; and (5) all the penetrations into and/or through the blanket/shield region that provide access for neutral-beam injection, evacuation, coolant inlet and removal, diagnostics, maintenance, experimentation, etc. The preliminary PECS design for the Stage I and Stage II TEPR, together with results of the materials assessments and parameter analyses that supported the design activity, are summarized in this section. Detailed discussions of the assessment and parameter analyses that form the basis for this design are given in Chapter VI and Appendices C and D.

1. Stage I Primary-Energy Conversion System

The Stage I PECS is designed to: (1) permit the generation and removal of ~ 100 MW of sensible heat, (2) provide adequate protection of the magnet system from radiation damage and activation and excessive nuclear-energy deposition, (3) allow for continuous operation of reactor-level plasmas at a plant duty factor of $\sim 30\%$, and (4) demonstrate all operational aspects of a Tokamak fusion power reactor except tritium breeding and breeder-blanket performance. Materials and design approaches for the Stage I PECS were selected on the basis that they: (1) satisfy the nuclear requirements for nuclear-energy production and radiation attenuation, (2) permit PECS construction, operation and maintenance with minimum advancement in existing technology, and (3) provide a reasonable point for extrapolation to a demonstration scale Tokamak fusion power reactor.

(a) First Wall Design Considerations

The first wall (treated as a subsystem of the PECS) includes the vacuum vessel that surrounds the plasma region and other associated components, i.e. a low-Z liner, a plasma-aperture limiter, a flux breaker and the vacuum wall penetrations. The principal requirements of the TEPR first wall are: (1) to protect the plasma region from excessive atmospheric contamination, (2) to prevent excessive plasma contamination by products of plasma-wall interactions, and (3) to maintain its structural integrity for sufficient times under the severe irradiation, thermal and stress conditions imposed by an operating fusion reactor environment.

The first-wall system design options that have been considered for the TEPR include: (1) a bare-metal first wall, (2) a first wall fabricated from sintered metal-metal oxide product of the SAP type, (3) a composite consisting of a metal vacuum wall with a protective low-Z coating, and (4) a metal vacuum wall protected from the plasma by a separate low-Z liner. The bare-metal wall is the most attractive of the four options on the basis of fabricability; but results presented in Chapter III show that the relatively high atomic number (high-Z) of the atoms sputtered from typical structural metal surfaces (e.g. stainless steel, vanadium-base alloys) prevents the attainment of reasonable plasma ignition and burn conditions.

The latter three first-wall options are aimed at use of a low-Z material as the first surface facing the plasma. The SAP-type materials are considered to be of marginal utility because of their relatively low melting point and questionable radiation damage characteristics. For the design effort described in this report, emphasis was placed on the use of graphite or silicon-carbide, either as a coating on the vacuum vessel inside wall or as a separate, radiatively-cooled liner. The coating option has several attractive features with respect to utilization in the TEPR; the major one appears to be much simpler fabrication. It is conceivable that the coating could be put on after the vacuum wall is assembled, and replaced remotely after extended reactor operation. Drawbacks to the implementation of a radiatively-cooled liner stem from the high operating temperature of the liner ($> 1500^{\circ}\text{C}$), the possibility of establishing a discharge in the annulus between the liner and the vacuum vessel and the additional vacuum pumping requirements. Also, remote replacement for the separate-liner option appears to be less-easily accomplished than for the coated-wall option.

The preliminary first-wall design that has evolved for the Stage I PECS consists of circular sections of stainless steel coated with a thin (1-4 mm) layer of graphite on the inside to provide a low-Z interface between the plasma and the first structural surface facing it. Several of the stainless steel sections are fitted with brazed, insulated joints that are intended to disrupt the current that would otherwise develop in the first wall when the ohmic-heating coils are pulsed. The ends of each section are appropriately flanged so that leak-tight hydraulic couplings to adjacent sections can be made. The interfacing of the vacuum and neutral-beam systems with the first wall is illustrated in Figures II-2 through II-5.

(b) Blanket/Shield Design Considerations

The blanket region for the TEPR described in this report has been designed to convert the neutron and secondary gamma-ray kinetic energies to sensible heat. Other important functions of the combined blanket/shield region are: (1) to reduce radiation damage in the toroidal-field coils to acceptable levels from the standpoint of induced stabilizer resistivity; decreased critical current and superinsulation deterioration; (2) to reduce nuclear heating in the toroidal-field coil system to tolerable levels;

and (3) to minimize the induced activation and biological dose in the magnet structure such that magnet maintenance can be carried out in place with a minimal degree of radiation protection after reasonably short cooldown periods (i.e. a few weeks). All of these functions have to be performed with materials that: (1) are mutually compatible; (2) can withstand radiation damage for reasonable operating lifetimes; and (3) can be fabricated and/or implemented with existing or near-term technology.

The radiation-induced resistivity in the copper stabilizer of the toroidal-field coil system should not exceed $3 \times 10^{-8} \Omega\text{-cm}$ (which corresponds to 1.87×10^{-4} dpa) from the standpoint of magnet economics and reliability. This resistivity results from atomic displacement damage caused by the neutrons and can be annealed out by warming the magnet to near room temperature. Since the magnet cooldown could take as much as two months, the blanket/shield region should be designed to permit reactor operation at reasonable plant-duty factors (up to 50%) and wall loadings ($\approx 0.2 \text{ MW/m}^2$) for sufficiently long time spans between anneals (≥ 2 years). Nuclear heating of the toroidal-field coils must be reduced to the point at which the refrigeration power requirements are on the order of 1% of the reactor power output. In addition, there is incentive to minimize the blanket plus shield thickness on the inside of the torus so as to maximize the magnetic field in the plasma.

Nuclear performance characteristics of a variety of plausible material compositions, which might meet the design requirements for the TEPR blanket/shield region, have been investigated. (The results are described in detail in Section VI-B and Appendix D.) Some materials and compositions that have been considered are summarized in Table II-18. Options employing stainless steel (SS) were chosen because of the excellent radiation-attenuating characteristics of SS and because it is a construction material for which a substantial technology base exists. Options employing tungsten (W) and tantalum (Ta) were considered on the basis of their being superior to SS in attenuating neutron and gamma radiation. Vanadium was considered because of the favorable compatibility of vanadium-base alloys with liquid lithium and because of its reasonably good nuclear performance characteristics. Options containing graphite (C) and aluminum (Al) were investigated because they lend themselves to the development of a minimum activation blanket/shield assembly for a TEPR. Figure II-12 shows the relationship between displacement

Table II-18 Material-Composition Options Considered for the TEPR Blanket/
Shield Region

Design Number	Blanket (30 cm thick)	Magnet Shield (Variable thickness)
102	30 cm stainless steel (SS)	50% SS + 50% B ₄ C
108	30 cm 50% W + 50% B ₄ C	50% W + 50% B ₄ C
113	30 cm aluminum (Al)	50% SS + 50% B ₄ C
114	30 cm aluminum (Al)	50% Al + 50% B ₄ C
116	30 cm stainless steel (SS)	75% SS + 20% H ₂ O + 5% B
117	30 cm graphite	50% SS + 50% B ₄ C
118	30 cm graphite	50% Al + 50% B ₄ C
119	30 cm vanadium	50% SS + 50% B ₄ C

Figure II-12 Displacement Damage to the Superconductor Stabilizer (copper) versus Overall Blanket/Shield Thickness for the Materials Compositions Given in Table II-18

damage in the magnet stabilizer and overall blanket/shield thickness for the various materials compositions investigated. Results of a similar sensitivity study, wherein the refrigeration power (as a percentage of the plant electrical power output) needed to overcome the nuclear heat load generated in the toroidal-field coil is plotted against overall blanket/shield thickness in Figure II-13.

Implications of the results in Figures II-12 and II-13 and of other related parameter sensitivity analyses performed on the compositions given in Table II-18 may be summarized as follows: (1) mixtures of stainless steel (SS) and boron carbide (B_4C) were found to be superior to all material compositions investigated, except for mixtures of tungsten (or tantalum) and B_4C . from the standpoint of reduced stabilizer displacement damage and nuclear energy deposition in the magnet. (The composition 50% SS-50% B_4C was found to be optimal for reducing displacement damage to the stabilizer, while the composition 75% SS - 25% B_4C was optimal for minimizing nuclear energy deposition in the magnet.) (2) Studies of the induced radioactivity generated in the blanket/shield, after representative operating times, indicate that remote handling and maintenance would be required for the options employing stainless steel, tungsten (or tantalum), or vanadium even after a year of cooldown, whereas graphite and/or aluminum-containing compositions appear to be accessible (after only a few weeks cooldown) with a minimal degree of radiation protection. (3) Designs employing W-(or Ta)- B_4C and SS- B_4C mixtures offer the best prospects for achieving magnet-protection objectives with the smallest blanket/shield thickness (viz. Figure II-12). (4) The W-(or Ta)- B_4C and SS- B_4C designs offer the best prospects for operation with small additional refrigeration power requirements due to nuclear heat deposition in the magnets (viz. Figure II-13). (5) Compositions that offer the best prospects for minimal remote maintenance (e.g. compositions employing only C and/or Al together with B_4C) appear to be incapable of leading to a blanket/shield design with overall thickness less than 1 meter that would protect the magnets.

Figure II-13 Refrigeration Power Requirements (in % of Plant Electric Power Output) versus Overall Blanket/Shield Thickness for the Materials Compositions given in Table II-18 (Assumed Thermal-to-Electrical Conversion Efficiency = 30%)

In light of the above results, the materials option employing optimized compositions of SS and B_4C was selected for the preliminary blanket/shield design for the TEPR. An alternative design for the interior blanket/shield region, which consisted of an optimized composition of tungsten and B_4C , was carried along in parallel with the SS- B_4C design in the event that: (1) additional radiation attenuation, over and above that provided by the SS- B_4C mixture, would eventually be required, or (2) a sufficiently strong incentive arose to reduce the overall blanket/shield thickness to an absolute minimum value in order to increase the thermal power output. Dependence of the blanket/shield thickness that limits the radiation-induced resistivity in the stabilizer to $3 \times 10^{-8} \Omega\text{-cm}$ on neutron wall loading, time span between magnet anneals and plant-duty cycle are given in Table II-19 for the SS- B_4C preliminary design composition. Corresponding refrigeration powers are also given. Cases 6, 7, 11 and 12 are considered to represent reasonable operating conditions for the Stage I TEPR. For cases where the refrigeration power exceeds 10% of the plant electrical power output, it would probably be necessary to increase the blanket/shield thickness in order to bring the refrigeration power loading down to an economically more tenable level.

The optimized material disposition for the SS- B_4C reference TEPR interior blanket/shield region versus radial distance from the center of the plasma is given as Design I in Table II-20. Material dispositions for three alternative designs are also given. Design II is the same as Design I except that the SS in several interior zones is replaced by tungsten in order to enhance the total radiation attenuation. Designs III and IV represent optimized material dispositions for W- B_4C and Ta- B_4C mixtures. Stainless steel was included to a certain extent in Designs II through IV to accommodate structural requirements. Table II-21 contains a summary of important radiation damage parameters for the four designs given in Table II-20.

(c) Thermal Fluid Analysis for the Preliminary PECS Design

Both pressurized helium and pressurized water were considered for the primary blanket coolant in the reference PECS design. Preliminary thermal fluid analyses for the blanket configuration given as Design I in Table II-20 are summarized in Table II-22. For the same cooling configuration, blanket temperature profile, total thermal power and thermal power deposition profile (determined from the nuclear heating calculations), pressurized water is

Table II-19 Dependence of Blanket Plus Shield Thickness and Refrigerator-Power Requirements on Integral Wall Loading if the Maximum Allowable Radiation-Induced Resistivity in the Stabilizing Copper Conductor is $3 \times 10^{-8} \Omega\text{-cm}$

Case	Neutron Wall Loading (MW/m ²)	Span Between Magnet Anneals (years)	Duty Cycle (%)	Integral Wall Loading (MW-yr/m ²)	Blanket Plus Shield Thickness ^a (cm)	Refrigerator Power ^b (%)
1	0.1	2	20	0.04	59	113
2	0.1	2	50	0.10	67	33
3	0.1	2	80	0.16	71.5	18
4	0.1	5	80	0.40	77	9
5	0.1	20	80	1.60	87.5	1.5
6	0.2	2	20	0.08	65	3.3
7	0.2	2	50	0.20	72.5	16
8	0.2	2	80	0.32	75.5	11
9	0.2	5	80	0.8	81	6
10	0.2	20	80	3.2	92.5	1
11	0.5	2	20	0.20	72.5	16
12	0.5	2	50	0.50	78.5	7.5
13	0.5	2	80	0.80	81.0	6
14	0.5	5	80	2.0	89	1.4
15	0.5	20	80	8.0	97	0.6
16	1.0	5	80	4.0	93.5	1
17	1.0	20	80	16.0	107	0.15

- a. Based on optimized blanket and shield composition of stainless steel and boron carbide; the calculations allowed a void fraction (for helium cooling, thermal expansion, etc.) of 10 vol.% in the first 40 cm, and 5% in the rest of the shield.
- b. Refrigeration electric-power requirements expressed in percentage of plant electric-power output. Assumptions: 70% of the outer surface area covered by the magnet, plant thermal efficiency of 30%, 16.5 MeV are recoverable for each fusion reaction, and 500 W electric power required per watt of thermal-power input to the refrigerators.

Table II-20 Description of Alternative Designs for the Inner Blanket/Shield System

Zone	Radial Position, cm	Thickness cm	Density Factor	Design I ^a	Design II	Design III	Design IV
1	0-210	210	0	Plasma	Plasma	Plasma	Plasma
2	210-240	30	0	Vacuum	Vacuum	Vacuum	Vacuum
3	240-241	1	.5	SS ^b	SS	SS	SS
4	241-256	15	.9	SS	W ^d	W	Ta ^e
5	256-261	5	.9	B ₄ C ^c	B ₄ C	W	Ta
6	261-276	15	.9	SS	W	W	Ta
7	276-281	5	.9	B ₄ C	B ₄ C	B ₄ C	B ₄ C
8	281-291	10	.95	SS	SS	SS	SS
9	291-301	10	.95	B ₄ C	B ₄ C	W	Ta
10	301-311	10	.95	SS	W	W	Ta
11	311-321	10	.95	B ₄ C	B ₄ C	W	Ta
12	321-331	10	.95	SS	SS	SS	SS
13	331-340	9	.95	B ₄ C	B ₄ C	B ₄ C	B ₄ C
14	340-397	57	1	magnet (44% SS + 44% Cu + 6% NbTi + 6% He)			

^aReference design, ^bStainless steel, ^cBoron carbide, ^dTungsten alloy, ^eTantalum alloy

Table II-21 Radiation Damage Parameters in the TEPR Toroidal-field Magnet for Designs Described in Table II-20

Maximum Values in the Inner Magnet	Design Ia	Design II	Design III	Design IV
Displacements in Copper, dpa per MW-year/m ²	1.5×10^{-5}	3.0×10^{-6}	1.4×10^{-6}	2.3×10^{-6}
Radiation Induced Resistivity in Copper, Ω -cm per 10 MW-year/m ²	2.5×10^{-8}	5.0×10^{-9}	2.4×10^{-9}	3.9×10^{-9}
Neutron Fluence, n/cm ² per MW-year/m ²	4.7×10^{16}	9.3×10^{15}	1.2×10^{16}	1.5×10^{16}
Dose in mylar, rad per MW-year/m ²	3.7×10^7	7.4×10^6	4.0×10^6	6.2×10^6

^a Reference design.

Table II-22 Thermal Fluid Analysis Parameters for the TEPR Preliminary Blanket Design

<u>Parameter</u>	<u>Coolant</u>	
	<u>Water</u> ^a	<u>Helium</u> ^b
Thermal power removed ^c , MW	186 ^g	186 ^g
Coolant inlet pressure ^c , atm	136	50
Pressure drop in blanket ^e , atm	< 1	~ 1
Coolant channel diameter ^c , cm	1.0	2.5
Coolant channel volume fraction in blanket ^f , %	1 → 0.1	10 → 2
Pumping power for blanket, MW	< 1	~ 7
Coolant inlet temperature ^c , °C	38	357
Coolant exit temperature ^c , °C	302	527
Maximum blanket temperature ^c , °C	600	600
Tube wall temperature ^c , °C	327	550
Number of radial zones ^c	6	6
Coolant velocity	(d)	(d)

^a Parameters consistent with PWR technology.

^b Parameters consistent with HTGR technology.

^c Parameters fixed in calculations.

^d Incompressible flow was assumed, i.e., the coolant velocity was kept at < 0.3 x sonic velocity.

^e Average pressure drop per tube.

^f Required volume fraction for coolant decreases with radial distance from first wall.

^g Based on a conservative overestimate of the neutron wall load of 0.3 MW/m².

found to require significantly less pumping power and coolant-channel volume fraction. Details of the thermal fluid calculations are given in Section VI-C. [It should be noted that: (1) the two alternative coolant schemes were not optimized with respect to electrical power output per unit of thermonuclear power, (2) the coolant-channel geometry was not optimized, and (3) the stresses resulting from the indicated temperature profile remain to be analyzed.] While helium appears to have greater long-range potential as a fusion reactor coolant, the advantages of reduced coolant-channel volume fraction and lower pumping power afforded by water could help to ameliorate several of the design complications for the Stage I PECS.

The shield region, which backs up the blanket, is simply an extension of the blanket insofar as materials composition is concerned, but is operated at or near ambient temperature and is cooled with a low-temperature coolant circuit (probably borated water) that is separate and distinct from the primary coolant circuit. The substitution of tantalum or tungsten for the stainless steel zones in the blanket and shield will require that the respective coolants be channeled through stainless steel ductwork or that the tantalum (or tungsten) be cladded in stainless steel in order to avoid materials incompatibilities between helium or water and the refractory metals.

2. Stage II Primary Energy Conversion System

The criteria for the Stage II PECS module designs are largely the same as those for the Stage I design, except that appropriate modifications are required to include a tritium-breeding medium. These modifications consist of introducing a relatively thin zone (~ 10 cm) of liquid lithium or a solid lithium compound. In the liquid lithium design, the blanket structure would be altered to include the use of a vanadium-base alloy for lithium containment. The solid lithium compound would be selected to be compatible with stainless steel and would be operated in a packed-bed mode. If magneto-hydrodynamic effects on heat transfer and pressure drop, resulting from the circulation of a liquid metal in Tokamak-type, magnetic-field environments prove to be tolerable, consideration will be given to the use of liquid lithium as primary coolant in a Stage II blanket module. Otherwise, both the liquid lithium and solid lithium compound blankets could be cooled with pressurized helium; although thermal energy removal and material compatibility

problems are expected to be exacerbated in this case. Considerations related to tritium handling and blanket processing requirements for these breeding concepts are summarized in Section II-F and are discussed in detail in Chapter VII.

Consideration was given to the demonstration of net breeding gain for the Stage II modules. (The breeding gain with a Stage II module would be sufficient to achieve net breeding if the entire blanket was so constructed.) The overall thickness of the Stage II PECS modules must not exceed a value that is compatible with the basic Stage I design. The relationship between displacement damage to the toroidal-field coil stabilizer and overall blanket/shield thickness for three plausible breeding configurations is given in Figure II-14, together with the curve for an all stainless steel blanket for comparison purposes. The results in Figure II-14 show that a net breeding gain is possible with a relatively thin zone (< 10 cm) of enriched lithium ($\sim 90\% \text{ }^6\text{Li}$) and beryllium. A demonstrated breeding gain in a zone of thickness ≤ 10 cm does not appear to be possible for designs that employ enriched lithium (without any beryllium) or natural lithium ($\sim 92\% \text{ }^7\text{Li}$).

F. Tritium Processing and Containment

Results of analyses performed thus far for the TEPR tritium handling facility are described in Chapter VII. In summary, it appears that operation at a cycle averaged power of ~ 100 MWt will result in the consumption (by burn-up) of ~ 16 gm per day of tritium and ~ 11 gm per day of deuterium. Taking account of the fractional burn-up and the extent to which a cold fuel gas blanket is employed, the gross fuel throughput could approach 2700 gm per day (D+T). The cost of supplying tritium to the TEPR to offset the burn-up may run from 5 to 15 million \$ per year, for a 30% plant duty factor. The fueling costs associated with deuterium supply are negligible.

The tritium handling facility for Stage I operation has been broken down into six major systems: fuel-delivery, fuel-circulation, fuel-processing, fuel-storage, in-plant containment and purge-processing. Stage II operation requires all six of these systems plus an additional system to provide blanket-processing. The principal features of these systems, as they are currently conceived, are outlined below. A schematic drawing of the main-stream fuel cycle is shown in Figure II-15.

Figure II-14 Displacement Damage to Superconductor Stabilizer (copper) versus Overall Blanket/Shield Thickness for Selected Tritium-Breeding, Blanket-Materials Configurations.

Figure II-15 Preliminary Fuel-cycle Schematic for the TEPR

1. Fuel-Delivery System

The function of this system is vital to sustained operation of reactor level plasmas in a TEPR. A substantial fraction of the total quantity of D-T fuel involved in a single burn cycle may have to be delivered to the plasma after initiation of the discharge. Fueling schemes that involve either high speed injection of solid D-T pellets or application of a cold D-T gas blanket are leading candidates. Special features of each of these fueling schemes that are necessary to insure efficient recycle and adequate containment of tritium are being evaluated, and estimates of the additional costs associated with these special features are being made.

2. Fuel-Circulation System

The tritium handling facility for the TEPR is comprised of a network of systems which either operate on or store the fuel at various stages of the fuel cycle. The subsystems and components that fall under the fuel-circulation system are those which provide the interfaces between the other six tritium handling systems, e.g., all interconnecting ductwork and piping, compressors, circulation pumps, the vacuum pumping equipment, numerous intermediate conditioning steps, monitoring equipment, the overall facility-control center, valves, by-passes, etc. Identification of the methodology for performing all of these diverse operations, evaluations of existing technology and costing of the entire installation are in progress.

3. Fuel-Processing System

This system consists of an integrated process involving a hot-getter/cryogenic-trap step (to remove non-hydrogenous impurities) and a cryogenic-distillation step (to remove helium and protium). Alternatives to cryogenic distillation, including laser-excited isotope separation and multi-staged permeable membranes, will eventually come under study.

4. Fuel-Storage System

Various approaches to the provision of fail-safe fuel storage and storage access are being investigated. For the purposes of the present design study, consideration is given to a concrete-barricaded vault containing three or more independently-controlled storage cells. The cells house a temperature-sensitive hydrogen-gettering material, wherein uptake

or release of tritium is controlled by temperature adjustments. The vault itself is provided with a quenching system that can rapidly remove thermal energy from the cells in the event of an incident that could compromise the storage of tritium. Also included in the assessment of this system will be those considerations that are pertinent to fail-safe transportation of tritium from a distant production facility. Prospects for near-term development of the required transportation and storage facilities based on existing technology appear to be excellent.

5. In-Plant Containment System

Current thinking on this system suggests that consideration be given to three levels of tritium containment. Where it is possible to do so, construction of all components that come into direct contact with tritium should be made with low-permeability materials (e.g., selected alloys, metal composites, ceramic barriers), particularly in elevated temperature ($> 300^{\circ}\text{C}$) regions. It may be necessary to provide an external, closely-fitted jacket with provision for in-jacket purging around those tritium facility components that: (1) are susceptible to failure, (2) operate at elevated temperature, and/or (3) contain relatively large quantities of tritium. In addition, the main reactor hall and the other in-plant rooms that house tritium-handling systems will be designed to include hermetic sealing (from the environment) and an inert continuously processed atmosphere. The costs associated with these three levels of containment will be evaluated in parallel with the systems scoping and design activities.

6. Purge-Processing System

This system must be designed to remove tritium from both the jacketed and whole-room purge streams. Besides removing tritium from these purge streams under normal operating conditions, it will have to be capable of: (1) handling all plausible forms of high-level tritium release, and (2) providing sufficient enrichment of tritium in protium-contaminated, purge-stream effluents to permit its return to the mainstream fuel cycle. The prospects and economic incentives for various levels of concentration and enrichment of tritium collected by the purges are currently being evaluated.

7. Blanket-Processing System

This system is essential to Stage II operation. During recent years, considerable attention has been given to tritium recovery from various

potential blanket materials; the literature that has evolved on this subject is being reviewed. An attempt is being made to identify, in the broadest sense, all of the blanket processing requirements of a TEPR. Two promising methods for removing tritium from liquid lithium blankets are being given serious consideration at this time. One involves the removal of tritium (as LiT) and other salt-like impurities (e.g., Li_3N , Li_2C_2 , Li_2O) from the lithium blanket by extraction with an appropriately selected molten salt. The second consists of extraction of tritium from liquid lithium in a fluidized bed of a finely divided, solid, hydrogen-gettering material.

G. Research and Development Requirements

Identification of research and development requirements for the TEPR was an integral part of this study. Discussions of these requirements and, in some instances, outlines of research and development programs appear throughout the report. In this section, the major development needs are summarized.

1. Blanket and Shield Technology

Improved nuclear data and radiation transport computational methods need to be developed and verified with integral experiments. Primary coolant technology (coolant chemistry, coolant heat transfer and pressure drop data, materials compatibility, thermal-fluid analysis methodology, pump and heat exchanger development, etc.) must be improved for the Stage I coolant (H_2O or He) and must be developed for lithium for Stage II. Structural analysis methodology must be extended. Physical and chemical property data must be established for blanket and shield materials. Methods for measurement and control of impurity concentrations in blanket and coolant fluids must be developed. These requirements are discussed in Chapter VI and Appendices C and D, and a research and development program is outlined in Appendix H.

2. Materials

Sputtering and blistering data for low-Z coatings such as carbon, silicon-carbide and beryllium on stainless steel (selected vanadium alloys for Stage II) are needed. Outgassing rates must be established for these low-Z coated composites. Fabrication techniques for applying these low-Z

materials to the metallic vacuum wall must be developed. Mechanical property and radiation damage data for low-Z coated composite first walls must be obtained. Additional data on radiation-induced void swelling, embrittlement and transmutation effects in stainless steel (and vanadium alloys for Stage II) are needed. The chemical compatibility of the coating materials with the base metals and the plasma ions must be investigated for the proposed temperature ranges. These requirements are discussed in Chapter VI and Appendices C and D, and a research and development program is outlined in Appendix F.

3. Neutral Beam Injection

The injector system needed for the TEPR represents a considerable extension of current technology, but is a reasonable extrapolation beyond the system that must be developed for the TFTR. Requirements for neutral beam injectors are discussed in Chapters III and V, and the ion source development needs are discussed in Chapter V.

4. Plasma Physics

Confinement, impurity control, heating, refueling, MHD stability limitations and configurational stability of noncircular cross section plasmas are foremost among the plasma physics uncertainties that must be better understood. The effects of these uncertainties upon the TEPR performance and the anticipated effect of future experiments in reducing these uncertainties are discussed in Chapter III.

5. Power Supplies

A power supply/energy storage system must be developed for the ohmic heating system. Energy switching systems must be developed for the neutral beam injector systems. These requirements are discussed in Chapter V.

6. Remote Maintenance, Assembly and Fabrication

Special devices must be developed to perform the remote maintenance and assembly/disassembly operations that will be required for the TEPR. Component fabrication also requires development.

7. Superconducting Magnets

Materials property data and a better understanding of cryogenic stability are required for the toroidal field coil system. A pulsed-coil

development program is required for the ohmic-heating and equilibrium coils. These requirements are discussed in Chapter IV, and a research and development program is outlined in Appendix G.

8. Tritium Handling

Isotope separation technology, materials for tritium containment and tritium control technology must be developed for Stage I of the TEPR. The technology for recovering tritium from lithium blankets must be developed for Stage II. These requirements are discussed in Chapter VII, and a research and development program is outlined in Appendix I.

9. Vacuum Systems

Large cryosorption panels must be developed for the neutral beam injection system, and large cryosorption pumps must be developed for the toroidal vacuum system. Leak-tight, canned mechanical pumps must be developed for the roughing system. These requirements are discussed in Chapter V.

CHAPTER III

PLASMA PHYSICS AND REACTOR PERFORMANCE

This chapter is concerned with the evaluation of the thermonuclear performance of the reference TEPR design that was described in Chapter II.* Major uncertainties in the plasma confinement and stability physics, plus the requirement that several design options be considered for first-wall materials/impurity control measures and toroidal field strengths dictate that the reactor performance be evaluated over a range of conditions. Steady-state analyses, which approximate the conditions that obtain during the peak portion of the burn pulse, are useful in scoping the reactor performance and in determining technological requirements for the plasma support systems over this range of possible operating conditions. From this "performance envelope", plausible reference performance parameters were selected to guide the engineering analysis of the major systems. Dynamic analyses of the burn cycle were performed to obtain a more comprehensive assessment of reactor performance, to establish requirements upon the plasma driving and neutral beam injection heating systems, and to study methods of operation and the effect of wall-sputtered impurities.

One of the major plasma physics uncertainties for the TEPR is the energy confinement. The general philosophy taken in this study has been to assess the reactor performance assuming that the confinement was adequate for an ignition device and, as a backup position, to determine what the performance of a similar beam-driven device⁽¹⁾ would be in the event that the energy confinement was not adequate for ignition. In each instance, perspective is provided by comparison with the energy confinement that is predicted by trapped-ion-mode (TIM) theory,^(2,3) which yields the most pessimistic of the confinement scaling laws presently thought to be plausible for a TEPR-type plasma.

* A reactor parameter survey — in which tradeoffs among reactor size, plasma current, and injected power were examined as a function of toroidal field strength, blanket plus shield thickness, impurity concentration, safety factor (q) and confinement (n) — is summarized in Appendix B.

The primary emphasis in this study was upon reactor-type performance of the TEPR, and the focus was largely upon operating conditions which were favorable for the production of electrical power under breakeven or near-breakeven conditions. However, a secondary objective of the TEPR is to serve as a materials irradiation and, possibly, components test facility. Operating conditions which optimize the utilization of the TEPR as a test facility were examined.

Global (0-dimensional) particle and energy balances on the D-T ions, electrons, alpha particles, and background and wall-sputtered impurities constitute the basic plasma computational model. Plasma heating by fusion alphas, ohmic heating, and by injected neutral deuteron beams (with suprathermal fusion) and plasma cooling by radiation (bremsstrahlung, synchrotron, line and recombination) and transport are treated. The transport losses were computed from the trapped-particle instability scaling laws, with regime selection based upon the collision frequency. Plasma MHD stability (q and β_θ) constraints were imposed. A model for the coupled dynamics of the plasma and the plasma driving system was used to determine the requirements upon the latter. This computational model is described in detail in Appendix A.

A. Reactor Performance Analysis — Steady State

The performance of the TEPR will depend upon a number of plasma physics factors which are uncertain at this time. Foremost among these are the energy confinement which will exist in a reactor-grade plasma, the level of impurity concentration which will be present in the plasma, the constraints which will be imposed upon operating conditions by MHD stability requirements, as well as the stability and confinement characteristics of noncircular cross-section plasmas. Because of these plasma physics uncertainties, it is only meaningful to speak of an envelope of performance characteristics associated with the design that was described in Section II-A. However, it is necessary to select a reference set of plasma physics parameters and to define the performance characteristics of a "reference" design in order to proceed with the engineering design of the TEPR.

This section is organized in the following manner. The first two subsections establish the envelope of performance characteristics that are associated with the plasma physics uncertainties. In the third subsection, this envelope is narrowed to define the performance characteristics of reference designs with circular and noncircular plasma cross sections. The final subsection discusses the utilization of the TEPR as a radiation facility.

1. Confinement and Impurity Effects Upon Performance

One of the major uncertainties in the prediction of the performance of the TEPR is the energy confinement loss which will obtain in a reactor-grade plasma. If the energy confinement is less than is required for ignition, it will be necessary to provide supplemental heating (e.g. with neutral beams) to maintain the plasma energy balance. If, on the other hand, the energy confinement is greater than is required for ignition, it should be an easy matter to take advantage of this circumstance. Because the energy confinement is uncertain, it is necessary to examine the performance of the TEPR for a range of values of the confinement parameter, $n\tau$, extending from values characteristic of present-day experiments up to ignition values. To this end, the steady-state energy and particle balance equations were solved as a function of $n\tau$,* with the energy balance being maintained by a 180-keV deuteron beam in the sub-ignition cases. For all cases, $B_{\max}^{\text{TFC}} = 70 \text{ kG}$, $\beta_{\theta} = 2.2$, $q = 2.5$, and geometric parameters corresponding to the circular plasma in Table II-1 were used.

The general characteristics of the power balance in a 50-50 D-T plasma are shown in Fig. III-1. ($n_i^2 p_A$ = alpha heating power; $n_i^2 p_R$ = radiative power loss; $n_i^2 p_B$ = beam heating power; $n_i^2 p_L$ = power loss by transport processes.) The value of $n\tau$ below which the ratio, Γ , of the beam energy density to the total plasma energy density exceeds one-half is indicated by 0, and the ignition point is indicated by $\bullet$. At small values of $n\tau$, transport losses balance beam heating, and other processes

* For this purpose, the particle and energy confinement times were taken to be equal, as is the case for the trapped ion mode confinement law.

Fig. III-1. Power Balance as a Function of $n\tau$ (50% D-50% T Plasma, $q = 2.5$, $\beta_{ij} = 2.2$). (The points where $\tau = 0.5$ are denoted by $\circ$, and the ignition values are denoted by $\bullet$.)

are unimportant. This is independent of the plasma temperature and impurity content. As $n\tau$ increases, both the transport power loss and the required supplemental heating decrease, and the power balance becomes more complicated. At very low temperatures, the alpha heating is insignificant, and the supplemental heating balances the transport loss at small $n\tau$ and the radiative loss at large $n\tau$ (Fig. III-1-a). At $T_e = 10$ keV, the alpha heating can balance the transport loss at some value of $n\tau$ and in a relatively clean plasma ignition is achieved (Fig. III-1-b). However, the presence of a significant amount of impurity increases the radiative loss so that supplemental beam heating is required to maintain the energy balance for all $n\tau$ (Fig. III-1-c), and ignition is precluded. At $T_e = 15$ keV with the higher impurity level, ignition is nearly obtained (Fig. III-1-d).

Before proceeding to an examination of the reactor performance as a function of the confinement, it is useful to establish a baseline for judging the reasonableness of a given value of $n\tau$. Trapped-ion mode (TIM) confinement theory is the most pessimistic (in terms of predicting the smallest value of $n\tau$ for a given size, plasma current, and plasma temperature) of the theories which are generally thought to be plausible for the reactor regime.^(2,3) Thus, the quantity $\alpha_{TIM} = (n\tau) \div (n\tau \text{ predicted by TIM})$ provides a measure of perspective. [The TIM expression for $n\tau$ is given in Appendix A by Eq. (A-28).] This quantity is plotted in Fig. III-2 for the circular cross section plasma. The strong temperature dependence and rather weaker Z_{eff} dependence of the TIM confinement scaling is evident. If TIM scaling turns out to be correct, then the TEPR design of this report must operate in the $T_e \sim 5-8$ keV range. If the confinement scaling is roughly an order of magnitude better than predicted by TIM, then operation at $T_e \sim 10$ keV would be possible. However, confinement would have to be two orders of magnitude better than TIM for ignition at $T_e \sim 15$ keV. It can be argued that the common expression for TIM scaling is too pessimistic by an order of magnitude⁽⁴⁾ and, hence, that $\alpha_{TIM} \approx 10-20$ could be a reasonable value.

A measure of the efficiency with which thermonuclear power is produced in the plasma is given by the parameter

Fig. III-2. Ratio of Required $n\tau$ to $n\tau$ Predicted by TIM
($q = 2.5$, $B_0 = 2.2$)

$$Q_p \equiv \frac{P_T - P_B}{P_B},$$

where P_T is the thermal power out of the plasma by all means (neutrons, radiation, heat) and P_B is the supplemental beam heating power injected into the plasma. (A more precise definition, based upon averages over a burn cycle, is given in Appendix A. Ohmic heating is negligible in these calculations.) The TFTR⁽⁵⁾ is designed to obtain $Q_p \approx 1$, and an ignition device corresponds to $Q_p \approx \infty$. Allowing a factor of approximately one-third for thermal-to-electrical energy conversion, a factor of approximately one-half for beam efficiency, and another factor of approximately one-half for the duty cycle and other losses, it follows that the range of Q_p over which net electrical power production is plausible is $Q_p \gtrsim 10$ -12.

As noted previously,⁽⁵⁻⁷⁾ the tritium-deuterium mixture which maximizes Q_p varies from 100% tritium in highly driven, low $n\tau$ devices to 50% tritium-50% deuterium in near-ignition and ignition devices with larger values of $n\tau$. The maximum value of Q_p is plotted in Figs. III-3 and III-4 to illustrate the effect of plasma temperature and impurity content. Using $Q_p \gtrsim 10$ as a rough criterion, Fig. III-3 indicates that net electrical power is plausible for $n\tau \gtrsim 2 \times 10^{20}$ sec/m³ at $T_e = 15$ keV, for $n\tau \gtrsim 3 \times 10^{20}$ sec/m³ at $T_e = 10$ keV, and for $n\tau \gtrsim 5 \times 10^{20}$ sec/m³ at $T_e = 6$ keV. The presence of impurities increases the value of $n\tau$ required and the presence of a significant fraction (a few tenths percent) of high-Z impurity precludes obtaining $Q_p \gtrsim 10$. The effect of impurities, which is shown in Fig. III-4, is similar to that found previously for ignition.⁽⁸⁾ For $T_e \gtrsim 6$ -10 keV, which corresponds to confinement 1 to 10 times TIM, from Figs. III-2 through III-4 it follows that $n\tau > 3$ -5 $\times 10^{20}$ sec/m³ is required for net electrical power to be plausible in the circular plasma cross section TEPR described in this report.

The production of a significant amount of electrical power is feasible, however, at substantially smaller values of $n\tau$ than are required

Fig. III-3. Q_p^{max} (Optimized as to D-T Mixture) as a Function of $n\tau$ and Temperature for an 0.5% ^{16}O Impurity ($q = 2.5$, $\beta_0 = 2.2$)

Fig. III-4. Q_p^{max} (Optimized as to D-T Mixture) as a Function of $n\tau$ and Impurity Level for $T_e = 10 \text{ keV}$ ($q = 2.5$, $\beta_\theta = 2.2$)

for $Q_p \gtrsim 10$. The thermal power* which could be obtained during the burn pulse is shown in Figs. III-5 and III-6, as a function of $n\tau$. For low $n\tau$ the power decreases with increasing $n\tau$ and temperature in a β_0 -limited device. From the viewpoint of power output there is actually some advantage to operating at low temperature and obtaining a low $n\tau$ — the disadvantage is in terms of the supplemental beam power that must be injected to maintain the power balance. There is an additional small power advantage in using a 100% tritium plasma if $n\tau$ is low. The maximum power, corresponding to the optimum D-T mixture, is relatively insensitive to the impurity concentration, as shown in Fig. III-6. This figure also illustrates a relative insensitivity of the power level to the value of $n\tau$, for $T_e = 10$ keV. Approximately 120 MWt must be produced during the burn pulse in order to produce approximately 20 MWe cycle-average power.** It is seen from Fig. III-6 that the TEPR objective of 5-50 MWe cycle-average electrical power could be realized over a wide range of possible values of $n\tau$ and of impurity concentration, so that uncertainties in confinement and impurity concentration do not compromise this design objective.

The flux of 14-MeV neutrons incident on the first wall is shown in Figs. III-7 and III-8. The neutron flux also decreases with increasing $n\tau$ and plasma temperature in a β_0 -limited device, and a 100% tritium plasma is preferable for maximizing the neutron flux at low $n\tau$. Again, the price that must be paid for the enhanced output in the low T — low $n\tau$ range is the enhanced supplemental beam heating that is required to maintain the plasma power balance. At $T_e = 10$ keV, the maximum neutron flux is insensitive to confinement and impurity concentration. However, at $T_e = 3$ keV, this is not the case, as shown in Fig. III-8.

* A 10% enhancement of the neutron energy by blanket reactions was used in these calculations. For the blanket design discussed in Chapter VI, there may be a slight reduction in convertible neutron energy in the blanket, so that the power may be overpredicted by $\sim 10\%$. On the other hand, $B_{TFC}^{max} = 70$ kG in these studies. The B_{TFC}^{max} of 75 kG used in the reference design would increase the power by 30%.

** Assuming a thermal-to-electrical conversion efficiency of one-third and a duty factor of one-half.

Fig. III-5. Total Power as a Function of $n\tau$ (0.5% ¹⁶O Impurity, $q = 2.5$, $\beta_0 = 2.2$)

Fig. III-6. Maximum Total Power (Optimized as to D-T Mixture) as a Function of $n\tau$ and Impurity Level at $T_e = 10 \text{ keV}$ ($q = 2.5$, $\beta_\theta = 2.2$)

Fig. III-7. First-Wall Neutron Current and Power Loading as a Function of $n\tau$ ($q = 2.5$, $\beta_\theta = 2.2$)

Fig. III-8. Maximum First-Wall Neutron Current and Power Loading (Optimized as to D-T Mixture) as a Function of $n\tau$ and Impurity Level ($q = 2.5$, $\beta_{ij} = 2.2$)

Up to this point, the reasonableness of injecting sufficient beam energy to operate in the low- n_i regime has not been addressed explicitly. It is generally felt that as long as the ratio of beam energy density to plasma thermal energy density is less than unity, then there will be no deleterious beam-plasma interaction effects. Unity for this ratio corresponds roughly to one-half for the ratio of beam energy density to plasma total energy density. Thus, beam-plasma stability considerations may exclude the regimes to the left of 0 in Figs. III-1 through III-8. This would require n_i greater than roughly 10^{17} sec/m³, depending on the case considered.

Figure III-9 summarizes several of the previous results, as well as showing the injected beam power that would be required to maintain the plasma power balance. All powers and neutron fluxes correspond to the optimum D-T mixture at that n_i . Although this figure indicates that higher thermal powers and neutron fluxes can be obtained by operating at low temperature, the required beam power is so large (500-1000 MW) as to be impractical in this size device. If, however, the power was reduced by operation, say, at higher q or lower β_0 , the beam power required would be correspondingly less, and this mode of operation might be desirable.

In summary, ignition requires $n_i \gtrsim 4 \times 10^{20}$ sec/m³ and net electrical power production probably requires $n_i \gtrsim 3 \times 10^{20}$ sec/m³ for a relatively clean plasma. The presence of impurities increases the values of n_i which are required for these two conditions and may preclude their being obtained if the impurities are of high enough Z and/or are present in sufficient concentration. However, production of thermal power output during the burn pulse in the range of ~ 100 - 300 MWt, with a corresponding neutron wall load of ~ 0.1 - 0.3 MW/m², can be achieved over a range of possible values of the energy confinement parameter $n_i \geq 10^{19}$ sec/m³ by using supplemental injection heating. This power production capability is relatively unaffected by the presence of impurities at $T_e \gtrsim 10$ keV.

2. MHD Stability and Magnetic Field Effects Upon Performance

In this section, the energy confinement is assumed to be $n_i = 4.2 \times 10^{20}$ sec/m³, which is the ignition value for a relatively

Fig. III-9. Several Parameters that Determine the Performance of a TEPR as a Function of $n\tau$ for a 0.5% ^{160}Pu Impurity ($q = 2.5$, $\beta_0 = 2.2$)

clean ($0.5\% \text{ } ^{16}\text{O}$) plasma (50% D-50% T) at $T_e = 10 \text{ keV}$. The performance of the circular and noncircular plasma cross-section TEPR designs described in Section II-A are evaluated for a range of values of the MHD stability parameters and of the toroidal field strength.

Three of the most important factors determining the performance of a TEPR are the safety factor, q ; the ratio, β_θ , of the plasma pressure to the poloidal magnetic pressure; and the peak magnetic field, $B_{\text{max}}^{\text{TFC}}$, at the toroidal field coils. The safety factor determines the plasma current, I_p , hence the poloidal magnetic field, B_θ , and the required magnetic flux, $\Delta\phi_{\text{ind}}$, which must be induced by the ohmic heating coil. The density is proportional to $\beta_\theta (B_{\text{max}}^{\text{TFC}})^2 / q^2$ (see Section I-B), and most of the power densities are proportional to n_i^2 . In view of the sensitivity of the reactor performance to these parameters, it is important that reasonable values are used to predict the expected performance and that an appreciation is developed for how the performance would vary if the parameters were different.

A lower limit for the safety factor $q = B_t / (\bar{A} B_\theta)$ is usually considered to be $q(a) = 2.5$.⁽³⁾ This is essentially the value of $q(a)$ that insures that $q(r) > 1$ (Kruskal-Shafranov Limit) everywhere in the plasma for a parabolic current distribution. For a flatter current distribution, $q(a)$ could be less, and for a more peaked distribution, it must be more. This limit insures against kink-mode instabilities. Present experiments typically operate at $q \approx 4-5$ (see Table II-1). Operation at q down to below 2.5 is possible, but the energy confinement time degrades.⁽³⁾

An upper limit for β_θ is often considered to be $\beta_\theta \approx A$.⁽³⁾ It should be noted that this limit is relatively insensitive to elongation of the cross section.⁽⁹⁾ More restrictively, it has been found numerically that equilibria with $\beta_\theta \gtrsim 2$ would be unstable to kink modes, have reversed currents, be squashed to one side of the chamber, or have other undesirable features.⁽¹⁰⁾ Experimentally, β_θ is typically 0.5 to 1.0. This is the limit that can be obtained by ohmic heating and may not apply to an ignited or beam-driven plasma.

Stray fields at the toroidal-field coil reduce the allowable self-field below the limiting value of approximately 85 kG for niobium-

titanium superconductors. Thus, values of $B_{\max}^{\text{TFC}}$ in the range of 70-80 kG are anticipated.

Table III-1 shows the variation of the performance with the parameters q , β_0 , and $B_{\max}^{\text{TFC}}$ for the circular cross-section plasma. It can be seen that $\Delta\psi_{\text{ind}}$ is less than 60 V-sec in all cases and that ΔB_{OH} is less than 50 kG. Even allowing for a factor of two in additional flux and field to overcome the resistive losses (see Section III-B), these demands should be well within the capability of the ohmic-heating and equilibrium coil systems (see Chapter IV).

The total power and wall loading are very sensitive to q , β_0 , and $B_{\max}^{\text{TFC}}$. In order to achieve the power objectives of the TEPR, q must be less than about 3. β_0 must be greater than about 2 for $q = 2.5$ and greater than about 2-3 for $q = 3$, depending on $B_{\max}^{\text{TFC}}$. This same parameter range must be achieved for the confinement to be plausible ($\alpha_{\text{TIM}} \sim 10-20$). These values, while consistent with theoretical limits, represent moderately optimistic assumptions about achievable q and β_0 . The above results are for an 0.5% ^{16}O impurity with $Z_{\text{eff}} = 1.3$. As discussed earlier in this section, ignition would be harder to achieve with a higher Z_{eff} .

Table III-2 presents the same type of performance characteristics for a noncircular cross section with $\kappa = 3$ elongation. The plasma volume is the same as for the circular case. For the same values of q , β_0 , and $B_{\max}^{\text{TFC}}$ the performance is substantially better for the noncircular design.

For the noncircular cross section, it can be seen that the TEPR power objectives can be exceeded for almost all the indicated values of q and β_0 . These values include a range of experimentally achieved values. For example, Doublet II, an approximately $\kappa = 3$ noncircular device, has achieved $q = 3.4$ with $\beta_0 = 0.8$.⁽¹¹⁾ The values of α_{TIM} for the noncircular cross section are close to unity. It should be noted that the TIM formula for $\pi\tau$ has been taken to be the same as for the circular case with the plasma radius, a , replaced by $a\ell$. (See Appendix A. The quantity $a\ell$ is the average value of r in the sense that it is the average distance from the center to the edge of the plasma.) Initial studies indicate that apart from this effect, diffusion theory is not greatly different from that for the circular cross sections.^(12,13)

Table III-1. Sensitivity of Performance Parameters to q , β_θ , and $B_{\text{TFC}}^{\text{max}}$ — Circular Plasma
 $(T_e = 10 \text{ keV, ignition, } n\tau = 4.2 \times 10^{20} \text{ sec/m}^3, 0.5\% \text{ } ^{16}\text{O}, Z_{\text{eff}} = 1.3)$

q	β_θ	$B_{\text{TFC}}^{\text{max}}$ (kG)	I (MA)	B_t (kG)	$\Delta\phi_{\text{ind}}$ (V-sec)	$(\Delta B_{\text{OH}})_{\text{ind}}$ (kG)	n_i (10^{20} m^{-2})	P_T (MW)	P_w (MW/m ²)	J_w ($10^{16} \text{ sec}^{-1} \text{ m}^{-2}$)	a_{TIM}
2.5	1	70	4.5	32	50	44	0.22	20	0.02	1.1	76
	2						0.44	78	0.10	4.4	19
	3						0.65	177	0.22	9.8	8
3.0	1	70	3.8	32	42	37	0.15	9	0.01	0.5	159
	2						0.30	38	0.05	2.1	39
	3						0.45	85	0.11	4.7	18
4.0	1	70	2.8	32	31	28	0.08	3	0.004	0.2	507
	2						0.17	12	0.02	0.7	125
	3						0.26	27	0.03	1.5	55
2.5	1	80	5.2	36	57	51	0.28	34	0.04	1.9	34
	2						0.57	134	0.17	7.4	9
	3						0.85	301	0.38	16.7	4
3.0	1	80	4.3	36	48	42	0.20	16	0.02	0.9	71
	2						0.40	65	0.08	3.6	18
	3						0.59	145	0.18	8.0	8
4.0	1	80	3.2	36	36	32	0.11	5	0.006	0.3	227
	2						0.22	20	0.03	1.1	56
	3						0.33	46	0.06	2.6	25

Table III-2. Sensitivity of Performance Parameters to q , β_0 , and $B_{\text{max}}^{\text{TFC}}$ — Noncircular Plasma
 ($T_e = 10$ keV, ignition, $n\tau = 4.2 \times 10^{20}$ sec/m³, 0.5% ¹⁶O, $Z_{\text{eff}} = 1.3$)

	β_0	$B_{\text{max}}^{\text{TFC}}$ (kG)	I (MA)	B_t (kG)	$\Delta\phi_{\text{ind}}$ (V-sec)	$(\Delta B_{\text{OH}})_{\text{ind}}$ (kG)	n_i (10^{20} m ⁻²)	P_T (MW)	P_w (MW/m ²)	J_w (10^{16} sec ⁻¹ m ⁻²)	a_{TIM}
2.5	1	70	11.3	37	94	83	0.72	216	0.21	9.2	4.5
	2						1.45	865	0.83	36.8	1.1
	3						2.17	1950	1.87	82.9	0.5
3.0	1	70	9.5	37	79	69	0.50	104	0.10	4.4	9.4
	2						1.00	417	0.40	17.8	2.4
	3						1.51	938	0.90	40.0	1.0
4.0	1	70	7.1	37	59	52	0.28	33	0.03	1.4	29.9
	2						0.56	132	0.13	5.6	7.4
	3						0.85	297	0.29	12.6	3.3
2.5	1	80	13.0	42	108	95	0.94	369	0.36	15.7	2.0
	2						1.89	1470	1.42	62.8	0.5
	3						2.83	3320	3.19	141.0	0.3
3.0	1	80	10.8	42	90	79	0.66	178	0.17	7.6	4.2
	2						1.31	711	0.68	30.3	1.1
	3						1.97	1600	1.54	68.2	0.5
4.0	1	80	8.1	42	67	59	0.37	56	0.05	2.4	13.4
	2						0.74	225	0.22	9.6	3.3
	3						1.11	506	0.49	21.6	1.5

The rather large plasma currents associated with the $q = 2.5$ cases in Table II-1 may place excessive demands on the plasma driving system. A superconducting ohmic-heating coil would be driven from $-B_{OH}$ to $+B_{OH}$, so that the total field change is limited to twice the maximum allowable field, which, accounting for stray fields, is about 75 kG. Thus, $\Delta B_{OH}^{max} \sim 150$ kG. The inductive ΔB_{OH} is in the range 80-100 kG for the $q = 2.5$ cases. Calculations that are discussed in a subsequent section of this chapter indicate that the ΔB_{OH} required to compensate the plasma resistive losses is significantly less than the inductive ΔB_{OH} , but that allowing twice the inductive ΔB_{OH} is a reasonable and conservative approximation.

3. Reference TEPR Designs

It is apparent from the results of the previous two subsections that within the geometrical envelope defined by the circular and $\kappa = 3$ noncircular plasma designs of Table II-1, a wide range of performance characteristics is possible, depending upon confinement, impurity concentration, toroidal magnetic field strength and the MHD stability constraints on q and β_θ . This range of possible performance characteristics encompasses thermal power outputs varying from ~ 1 to 10^3 MWt with first-wall neutron power fluxes varying from ~ 0.01 to 3.2 MW/m².

In this section, reference designs are selected which satisfy the TEPR objectives of 5-50 MWe cycle-averaged power production. Both a circular and a $\kappa = 3$ noncircular design are specified.

For the circular cross-section plasma q has been taken to be 2.5. This is the lower limit described in Ref. 3 and has been achieved experimentally, although the energy confinement has been observed to degrade at such low values. β_θ has been taken to be 2.2. This limit is within theoretical upper bounds for β_θ . The electron temperature, T_e , has been taken to be 10 keV. The power is maximized at about 13 keV (see Fig. I-2), but α_{TIM} increases rapidly for higher temperatures. For $T_e = 10$ keV the necessary confinement is within an order of magnitude of the predicted value for TIM. This prediction could be too pessimistic by an order of magnitude.⁽⁴⁾ In addition, $T_e = 10$ keV allows operation with a relatively wide range of impurity concentrations, as can be seen in

Fig. I-4. For these calculations the impurity concentration has been taken to be 0.5% ^{16}O with a Z_{eff} of 1.3. (It is too early to tell if this level is achievable. Studies of the effect of the impurity level have been described in an earlier section, and the buildup of impurities from wall sputtering is described in a subsequent section.)

The design value of the maximum field, $B_{\text{max}}^{\text{TFC}}$, at the toroidal field coils is 75 kG, as discussed in Chapter IV. Performance characteristics are also developed for 70 and 80 kG.

The performance of the circular cross-section reference design is shown in Table III-3. Operation at $Q_p = \text{ignition}$, 6.5, and 1.0 is shown, providing an indication of the performance as a function of the value of $n\tau$. At ignition the reference design produces a thermal power output of 129 MW and a wall loading of 0.16 MW/m^2 for the 75-kG peak field. The confinement required for ignition must be a factor of 10 better than predicted by TIM.

The operation of a $\kappa = 3$ noncircular plasma with the same volume in the same toroidal field coil has also been considered. The advantages associated with the noncircular plasma can be utilized in several ways. Using the same values of $B_{\text{max}}^{\text{TFC}}$, q , and β_θ as for the circular design, much higher power can be obtained with the noncircular design. It has been chosen to use more readily achievable values of the stability parameters, $q = 4$ and $\beta_\theta = 2$, and to also obtain an increase in power to 174 MW at $B_{\text{max}}^{\text{TFC}} = 75 \text{ kG}$. The quantity α_{TIM} is a factor of 2 smaller than for the circular plasma design operating at lower q and higher β_θ . The performance of this design is shown in Table III-4.

The noncircular cross-section plasma design was chosen to fit in the toroidal field coil designed for the circular cross section. If the entire reactor had been designed for the noncircular cross section, it would have been designed somewhat differently. In particular, the increased performance could have been utilized to make the size smaller.

It is apparent that noncircular cross-section plasmas do have a significant potential advantage, even if it turns out that they must operate at slightly higher q and lower β_θ . Most of this advantage comes

Table III-3. Performance Summary (Steady-State) — Circular Plasma
 $(\beta_p = 2.2, q = 2.5, Z_{eff} = 1.3, T_e = 10 \text{ keV})$

Q_p	$n\tau$ (10^{20} sec/m^3)	I (MA)	B_t (kG)	$\Delta\phi_{ind}$ (V-sec)	$(\Delta B_{OH})_{ind}$ (kG)	n_i (10^{20} m^{-3})	T_i (keV)	P_T (MW)	P_B (MW)	P_w (MW/m ²)	J_w ($10^{16} \text{ sec}^{-1} \text{ m}^{-2}$)	Γ	α_{TIM}
<u>$B_{max}^{TFC} = 70 \text{ kG}$</u>													
Ignition	4.2	4.5	32	50	44	0.49	9.6	98	0	0.12	5.4	0	15
6.5	1.7					0.47	10.2	130	17	0.14	6.2	0.04	7
1.0	0.057					0.14	19.6	223	111	0.14	6.2	1.05	5
<u>$B_{max}^{TFC} = 75 \text{ kG}$</u>													
Ignition	4.2	4.8	34	54	48	0.56	9.6	129	0	0.16	7.2	0	10
6.5	1.7					0.54	10.2	171	23	0.18	8.2	0.04	5
1.0	0.057					0.16	19.6	294	146	0.18	8.2	1.05	3
<u>$B_{max}^{TFC} = 80 \text{ kG}$</u>													
Ignition	4.2	5.2	36	57	51	0.64	9.6	167	0	0.21	9.3	0	7
6.5	1.7					0.61	10.2	221	30	0.24	10.6	0.04	3
1.0	0.057					0.18	19.6	380	189	0.24	10.6	1.05	2

Table III-4. Performance Summary (Steady-State) — $\kappa = 3$ Noncircular Plasma
 $(\beta_p = 2, q = 4, Z_{eff} = 1.3, T_e = 10 \text{ keV})$

Q_p	n (10^{20} sec/m^3)	I (MA)	B_t (kG)	i_{ind} (V-sec)	$(\langle B_{OH} \rangle)_{ind}$ (kG)	n_i (10^{20} m^{-3})	T_i (keV)	P_T (MW)	P_B (MW)	P_W (MW/m ²)	J_W ($10^{16} \text{ sec}^{-1} \text{ m}^{-2}$)	Γ	α_{TIM}
<u>$B_{max}^{TFC} = 70 \text{ kG}$</u>													
Ignition	4.2	7.1	37	59	52	0.56	9.6	132	0	0.13	5.6	0	7
6.5	1.7					0.54	10.2	174	23	0.14	6.4	0.04	4
1.0	0.057					0.16	19.6	300	149	0.14	6.4	1.05	2
<u>$B_{max}^{TFC} = 75 \text{ kG}$</u>													
Ignition	4.2	7.6	39	63	56	0.65	9.6	174	0	0.17	7.4	0	5
6.5	1.7					0.62	10.2	230	31	0.19	8.5	0.04	2
1.0	0.057					0.18	19.6	395	196	0.19	8.5	1.05	2
<u>$B_{max}^{TFC} = 80 \text{ kG}$</u>													
Ignition	4.2	8.1	42	67	59	0.74	9.6	225	0	0.22	9.6	0	3
6.5	1.7					0.71	10.2	297	40	0.25	11.0	0.04	2
1.0	0.057					0.21	19.6	511	254	0.25	11.0	1.05	1

from the fact that more of the plasma is in the higher magnetic field region close to the toroidal field coils. On the other hand, achieving the noncircular shape is technically more difficult. In particular, it requires field shaping coils near to the plasma, where, in a reactor, they are subject to intense thermal and radiation loads. The circular cross-section plasma is taken as the basis for the engineering design calculations in this report because circular plasmas have a more extensive experimental basis. However, the major technological systems are designed to be adequate for the noncircular plasma, also.

4. Utilization of the TEPR as a Radiation Facility

Although the primary objectives of the TEPR relate to plasma physics, the production of power, and the demonstration of engineering technology, it will also be a valuable radiation test facility. In this section, the characteristics of the TEPR as a radiation test facility are examined.

A major feature of the TEPR design as a test facility is its large surface area, $A_w = 592 \text{ m}^2$, only a few percent of which is required for beam and vacuum ports. Table III-5 gives an indication of the particle currents incident on the first wall assuming the wall loading is $P_w = 0.25 \text{ MW/m}^2$, a value which could be achieved, with a slightly different set of design parameters, by both the circular and (more readily) the noncircular designs. The energy spectra are given as well. The mean energy of the slowing-down alpha particles is 1.3 MeV. Table III-6 shows the neutron spectrum at the first wall. It is assumed that 7% of the 3.52-MeV alphas are lost because of banana orbits that intersect the wall⁽¹⁴⁾ or because of trapping in asymmetric field ripples,⁽¹⁵⁾ and that the remaining alphas thermalize, then diffuse out at the same rate as the ions.

Several designs for fusion engineering research facilities (FERFs) have been proposed based on mirror confinement,⁽¹⁶⁾ theta-pinch confinement,^(17,18) and tokamak confinement.⁽¹⁹⁾ These machines, which are explicitly designed as FERFs, have wall loadings of 1 MW/m^2 or over, although they have substantially less wall area. The tokamak FERF, which is the most comparable, produces 1 MW/m^2 over 149 m^2 with $\kappa = 2$, $q = 2.5$, $\beta_\theta = 3.5$, and $B_{\text{max}}^{\text{TFC}} = 80 \text{ kG}$. It operates at low n_T and low T_e where the power is

Table III-5. First-Wall Particle and Radiation Currents for the TEPR

Species	Current ^a (particles/sec-m ²)	Spectrum
Fast neutrons	1×10^{17}	E = 14.1 MeV
Fast alphas	7×10^{15}	E = 3.52 MeV
Deuterium	5×10^{18}	10-keV Maxwellian
Tritium	5×10^{18}	10-keV Maxwellian
Slow alphas	1×10^{17}	10-keV Maxwellian
Total neutrons	4×10^{17}	(Table III-6)
<u>(Watts/m²)</u>		
Bremsstrahlung	1×10^4	10-keV Boltzmann
Synchrotron	8×10^3	Approximately flat (ω_c to $20 \omega_c$)

^a Normalized to a neutron wall load $P_w = 0.25 \text{ MW/m}^2$.

^b The calculational model assumes the alphas slow down, then diffuse out so that they would have a characteristic temperature of 10 keV. On the other hand there is an equilibrium distribution of slowing-down alphas with average energy $\bar{E} = 1300 \text{ keV}$. Some of these will diffuse out before they slow down, although a quantitative estimate of the current is beyond the scope of the model.

Table III-6. Neutron Flux and Currents at the Inner Surface of the TEPR First Wall
(Results normalized to a neutron wall loading of 0.25 MW/m²)

Group	E (Top) (eV)	Flux (n/m ² -sec)	J ₊ ^a (n/m ² -sec)	Group	E (Top) (eV)	Flux (n/m ² -sec)	J ₊ ^a (n/m ² -sec)
1	1.4913 (+7)	2.159 (+17)	1.132 (+17)	25	4.0762 (+5)	1.523 (+17)	3.782 (+16)
2	1.3499 (+7)	9.695 (+15)	2.137 (+15)	26	3.0197 (+5)	9.991 (+16)	2.492 (+16)
3	1.2214 (+7)	1.403 (+15)	3.205 (+14)	27	2.2371 (+5)	7.277 (+16)	1.828 (+16)
4	1.1052 (+7)	1.313 (+15)	2.985 (+14)	28	1.6573 (+5)	9.295 (+16)	2.341 (+16)
5	1.0000 (+7)	2.281 (+15)	5.137 (+14)	29	1.2277 (+5)	1.567 (+17)	3.961 (+16)
6	9.0484 (+6)	9.784 (+14)	2.237 (+14)	30	6.7379 (+4)	5.961 (+16)	1.509 (+16)
7	8.1873 (+6)	4.651 (+14)	1.096 (+14)	31	3.1828 (+4)	4.327 (+16)	1.099 (+16)
8	7.4082 (+6)	6.715 (+14)	1.549 (+14)	32	1.5034 (+4)	3.929 (+16)	9.977 (+15)
9	6.7032 (+6)	5.674 (+14)	1.316 (+14)	33	7.1017 (+3)	5.331 (+16)	1.354 (+16)
10	6.0653 (+6)	8.655 (+14)	2.003 (+14)	34	3.3546 (+3)	5.006 (+16)	1.270 (+16)
11	5.4881 (+6)	1.094 (+15)	2.526 (+14)	35	1.5846 (+3)	2.897 (+16)	7.353 (+15)
12	4.9659 (+6)	1.398 (+15)	3.238 (+14)	36	7.4852 (+2)	2.374 (+16)	6.030 (+15)
13	4.4933 (+6)	1.668 (+15)	3.869 (+14)	37	3.5358 (+2)	2.019 (+16)	5.126 (+15)
14	4.0657 (+6)	2.047 (+15)	4.758 (+14)	38	1.6702 (+2)	1.720 (+16)	4.366 (+15)
15	3.6788 (+6)	2.475 (+15)	5.756 (+14)	39	7.8893 (+1)	1.424 (+16)	3.614 (+15)
16	3.3287 (+6)	2.919 (+15)	6.816 (+14)	40	3.7267 (+1)	1.126 (+16)	2.859 (+15)
17	3.0119 (+6)	3.937 (+15)	9.192 (+14)	41	1.7603 (+1)	8.372 (+15)	2.124 (+15)
18	2.7253 (+6)	6.219 (+15)	1.452 (+15)	42	8.3153 (+0)	5.728 (+15)	1.453 (+15)
19	2.4660 (+6)	2.705 (+16)	6.361 (+15)	43	3.9279 (+0)	3.513 (+15)	8.915 (+14)
20	1.8268 (+6)	4.301 (+16)	1.018 (+16)	44	1.8554 (+0)	1.872 (+15)	4.750 (+14)
21	1.3534 (+6)	5.419 (+16)	1.293 (+16)	45	8.7643 (-1)	8.385 (+14)	2.128 (+14)
22	1.0026 (+6)	6.386 (+16)	1.541 (+16)	46	4.1399 (-1)	1.677 (+14)	4.260 (+13)
23	7.4274 (+5)	1.071 (+17)	2.561 (+16)				
24	5.5023 (+5)	8.066 (+16)	1.999 (+16)	SUM	—	1.588 (+18)	4.543 (+17)

^aCurrent of neutrons incident on the plasma side of the first wall, but including neutrons which have undergone interactions in blanket and shield. The current of neutrons incident on the inner surface of the first wall from the blanket side is identical, except in the first group where $J_n^- = 2.48 \times 10^{15}$ n/m²-sec.

maximized with a highly driven ($P_B = 177$ MW) plasma. (See Section III-A-1.) As mentioned earlier, the power required to run the TEPR reference design in this mode is prohibitive. On the other hand, assuming operation at the same values of q , β_0 , and $B_{\max}^{TFC}$ used in Ref. 19 ($q = 2.5$, $\beta_0 = 3.5$, $B_{\max}^{TFC} = 80$ kG), the TEPR can produce a wall loading of ≈ 0.5 MW/m² over 592 m² surface area in an ignition device if the confinement turns out to be adequate ($n\tau = 4.2 \times 10^{20}$ sec/m³) or in a beam-driven device for lower $n\tau$. The driven devices would require injected powers varying from zero at ignition up to ≈ 300 MW at $n\tau \approx 10^{19}$ sec/m³. These estimates follow from Fig. III-9 scaled up according to Eq. (I-5). Operation would be at $T_e = 10$ keV. The $\kappa = 3$ noncircular cross-section design with the same parameters could produce a wall loading of over 4 MW/m² with correspondingly more injected power. $\beta_0 = 3.5$ exceeds the aspect ratio, $R/a = 3$, for the circular case, but is less than $R/a = 4.2$ for the $\kappa = 3$ noncircular case.

It is apparent that the TEPR has great potential as a radiation test facility. Operated at the modest power levels indicated in Tables III-3 and III-4 for the reference designs, it produces first-wall loads that are down by factors of 4 to 10 relative to the ≈ 1 MW/m² neutron load considered for FERFs. However, with different, but still plausible, choices for the parameters (q , β_0), the TEPR performance as a test facility becomes comparable to that envisioned for a FERF. The practicality and consequences of designing the TEPR to optimize its use as a radiation facility during Stage II will be examined.

B. Burn Cycle Dynamics

The steady-state analyses of the previous section are useful in determining certain features of the TEPR performance and are invaluable in establishing the envelope of performance characteristics that is associated with the plasma physics uncertainties. However, a dynamic simulation of the plasma, plasma heating system, and plasma driving system throughout the burn cycle is necessary to establish the detailed power performance and the requirements upon the plasma heating, fueling, and driving systems.

It is impractical to carry out the detailed burn cycle simulations for the same broad range of plasma physics parameters that was considered in the steady-state analyses. The approach taken in this section is to examine a representative case in some detail, including the consideration of various first-wall sputtered impurities, modes of operation, technological parameters characterizing the plasma driving and heating systems, anomalous plasma resistivities, and plasma confinement theories. The qualitative features of these analyses should be broadly applicable to designs characterized by the other plasma physics and technological parameters discussed in the previous section.

The base case chosen for the burn cycle simulations corresponds to the first line in Table III-3. From the viewpoint of power production, this case is the most pessimistic of the six cases described in Tables III-3 and III-4. (Note that a peak field of 70 kG is used, even though the analysis of Chapter IV indicates that the reference design value of 75 kG is obtainable.) Thus, this case represents something of a "worst-case" for power performance, and more favorable results could be obtained by using a higher peak field, B_{max}^{TFC} , and/or by considering the noncircular plasma design. The particle and energy confinement law varies with collision frequency during the burn cycle, as described in Appendix A. Pseudo-classical (electron energy and particles) or neoclassical (ion energy) collisional transport govern confinement at the low temperatures of the initial part of the burn cycle. The trapped particle instability modes, which can be excited when the collision frequency is less than the bounce frequency, become the dominant energy transport mechanism as operating temperatures are reached. Except as otherwise noted, the trapped particle confinement during the burn pulse is assumed to be 15.2 times larger than predicted by the theory, so that the base case is an ignition device at $T_e = 10$ keV with 0.5% ^{16}O impurity. (Note that the other cases in Tables III-3 and III-4 not only would have better power performances, but would operate as ignition devices with confinement closer to TIM, i.e. smaller α_{TIM} .)

The general scenario of the burn cycle simulation is depicted in Fig. III-10. The simulation is initiated after the plasma has been ionized and heated to a temperature of 25 eV. The plasma current is

Fig. III-10. Burn Cycle Scenario

brought up to its full value (4.5 MA) during the first second, during which time the plasma is ohmically heated. At this time (1 sec), supplemental heating by injection of neutral deuteron beams is initiated. The beam heating phase lasts until the operating temperature ($T_e = 10$ keV) is reached — for the nominal case of 40 MW at 180 keV, this phase requires about 3 sec — at which time the beam heating is terminated. The plasma density is held constant during both heating phases and during the burn phase by a combination of plasma recycling and refueling. During the burn phase the plasma temperature is maintained by alpha heating (if the reactor must be operated in the driven mode, the alpha heating would be supplemented by beam heating as required to maintain the operating temperature). The shutdown is initiated by reducing the refueling rate and allowing the plasma density to decrease for about 4 sec. Then the plasma current is ramped down in 1 sec, terminating the burn pulse. During the final phase, the toroidal vacuum chamber is pumped down from $\sim 10^{-3}$ torr to $\sim 10^{-5}$ torr, then recharged with a fresh D-T mixture.

The length of the burn phase in the TEPR burn cycle will be determined by the time that a stable plasma can be confined or by the buildup of impurities. Present tokamak experiments have maintained steady-state discharges for roughly 5-10 particle confinement times, or on the order of tens of msec.⁽³⁾ The TFTR⁽⁵⁾ may attain burn pulses on the order of tenths of seconds to seconds. In this study, burn pulses in the range 10-60 sec are considered for the TEPR. (The plasma driving system has adequate volt-seconds for much longer pulses.) One of the objectives of the analysis is to determine how long the burn phase must be in order for significant cycle-averaged electrical power to be produced and for net electrical power production to be feasible.

1. Base Case

The base case corresponds to the first entry in Table III-3. With $\alpha_{TIM} = 15.2$, the equilibrium conditions indicated in the table obtain during the burn phase. A 20-sec burn pulse was chosen for detailed analysis.

During the startup and burn phase the density is held constant by means of a cold refueling current, which is indicated by I_C in Fig. III-11. This current is adjusted to compensate for the difference between the ions

Fig. III-11. Ion Densities and Refueling Current
During Base-Case Burn Cycle

lost by diffusion and burnup and the ions injected by the neutral beam. Physically, this current represents either an actual refueling operation whereby a D-T mixture is injected into the plasma and/or a recycling process in which hot D and T ions impinge on the first wall, cool off, then diffuse back into the plasma. On present-day tokamaks recycling is responsible for maintaining a nearly constant density for many particle confinement times.⁽³⁾ However, these are small machines operated at low temperatures ($T_e \lesssim 1$ keV). If recycling takes place in the TEPR this would ameliorate the problem of refueling since the fractional burnup is quite small ($\sim 2-3\%$). If recycling does not occur, or is only partial, then some form of external refueling must be supplied by pellet injection or other means. One possibility might be the neutral beam itself. Note in Fig. III-11, that the injected beam (220 A equivalent neutral current) is sufficient to offset the particle losses during the beam heating phase. Maintaining n_i constant also suppresses the thermal instability which would otherwise characterize this equilibrium condition.⁽²⁰⁾ With TIM scaling, the instability is a mixed temperature-density fluctuation and can be suppressed by density control.

At 15 sec, the cold current is sharply reduced and the density falls off, reaching a value of about one-hundredth of its equilibrium value by the end of the cycle. This is done so that when the plasma current starts down at 19 sec, n_i is low enough so that the β_θ constraint is satisfied and the plasma energy can be transferred to the first wall in a controlled manner during the shutdown phase. β_θ in the shutdown period is approximately given by:

$$\beta_\theta \approx \frac{n_i T_i + n_e T_e + n_I T_i}{I_p^2} \approx \frac{2n_i T_i}{I_p^2},$$

where n_i is the ion density, n_e is the electron density, n_I is the impurity density, T is the temperature, and I_p is the plasma current. During the shutdown phase, the ion density satisfies

$$\frac{dn_i}{dt} \approx -\frac{n_i^2}{n\tau_p} \approx -\frac{n_i}{\tau_{peq}},$$

which leads to

$$n_1(t) \approx n_{1eq} \left[1 - \frac{t - t_{SD}}{\tau_{peq}} \right], \quad t_{SD} \leq t \leq t_{SD} + \Delta t_{SD},$$

where

t_{SD} = start of shutdown phase, Δt_{SD} = duration of shutdown phase,

τ_{peq} = particle confinement time at the start of shutdown $\approx$
equilibrium particle confinement time = 8.6 sec.

The density therefore diminishes at about a linear rate in the absence of any cold current. By maintaining and adjusting a small cold current, an acceptable density fall-off can be obtained; using this method, approximately 5 sec are required for shutdown. The β_θ behavior obtained in this manner is shown in Fig. III-12, together with the safety factor, q .

There is strong incentive to minimize the duration of the shutdown phase. The fusion energy during the shutdown phase is:

$$U_{FSD} = \int_{t_{SD}}^{t_{SD} + \Delta t_{SD}} P_F(t) dt \approx P_{Feq} \int_{t_{SD}}^{t_{SD} + \Delta t_{SD}} \left[1 - \frac{t - t_{SD}}{\tau_{peq}} \right]^2 dt$$

$$\approx \frac{\tau_{peq}}{3} P_{Feq},$$

where P_F is the fusion power, and the subscript "eq" denotes the equilibrium value during the burn phase. The ratio of fusion energy obtained during the shutdown phase to that during a part of the burn phase of equal duration is $\approx \left(\tau_{peq} / 3\Delta t_{SD} \right) \approx 0.6$. The shutdown phase therefore represents a considerable overhead for the burn cycle, particularly for a short burn phase.

The ion and electron temperatures (Fig. III-13) are nearly equal throughout the burn cycle. During the ohmic heating phase T_e and T_i rise from 25 eV to ≈ 300 eV. Beam heating then takes the plasma up to the burn

Fig. III-12. Safety Factor, q , and Poloidal Beta, β_θ , During Base-Case Burn Cycle

Fig. III-13. Ion and Electron Temperatures During Base-Case Burn Cycle

phase temperatures. The beam is turned off when β_0 (Fig. III-12) reaches 2.25. At this point, β_0 dips slightly since the beam no longer contributes to the kinetic pressure. β_0 then rises back to 2.25. During shutdown, T_e and T_i first rise somewhat because of the reduction in the cold ions injected into the plasma. Subsequently, T_i and T_e decrease. This decrease is hastened by a slight increase in the refueling current at the very end of the shutdown.

Other shutdown modes are possible. For example, a high-Z impurity could be injected to reduce the plasma temperature by enhanced radiation.⁽²¹⁾

The component powers during the burn cycle follow closely the different cycle events, as shown in Fig. III-14. The ohmic power, P_Ω , reaches approximately 20 MW during the ohmic heating phase and drops off thereafter to a negligible value as T_e increases and the Spitzer resistivity decreases. The beam power, P_B , is 40 MW during the beam heating phase. The neutron power, P_N , increases with T_i during the beam heating phase; the large peak at ~ 4 sec is due to the significant suprathermal fusion of fast deuterons with bulk plasma tritons before the beam is turned off. When the shutdown starts, P_N decreases and the power loss by energy transport to the first wall, P_L , rises rapidly because of the decreased n resulting from the increased temperature. The radiation power, P_R , reaches approximately 20 MW during the early part of the startup because of the line and excitation radiation of the 1/2% ^{16}O background impurity, but is small during most of the burn cycle.

The operation of the plasma driving (ohmic heating) system depends on the type of ohmic heating primary and the charging scheme used. For the superconducting primary coil of the TEPR, the primary is driven by first establishing a negative-flowing primary current in the period between plasma burn pulses. At the start of the burn cycle, the plasma is injected, ionized, and then acts as a one-turn conducting secondary. At the end of the cycle, the process is reversed; the primary current ramps down, and takes down the plasma current. The primary, I_{pri} , and plasma I_p , currents are shown in Fig. III-15. The difference in the starting and end points of I_{pri} is due to the resistive volt-seconds expended during the burn cycle. After the burn, I_{pri} would be recharged downwards before initiation of the next burn pulse. (See Section IV-C.)

Fig. III-14. Instantaneous Powers During Base-Case Burn Cycle

Fig. III-15. Primary and Plasma Currents During Base-Case Burn Cycle (Primary current in MA-turns.)

The ohmic heating power supply must be capable of storing, discharging, and recovering energy as needed by the plasma driving system, as shown in Fig. III-16. For this cycle the instantaneous power required from the ohmic heating system power supply, P_{PS} , varies from about -1000 MW to +800 MW. U_{PS} is the energy delivered by the power supply. (Negative values of P_{PS} means energy is flowing back into the power supply. This occurs when the primary current, I_{pri} , and hence the inductive energy in the primary, $U_{pri} = 1/2 L_{pri} I_{pri}^2$, is large and decreasing, but the plasma current, I_p , and hence the inductive energy in the plasma, $U_p = 1/2 L_p I_p^2$, and the mutual inductive energy, $U_M = M I_{pri} I_p$, are small. The magnetic energy stored in the primary has nowhere to go except back to the power supply.) In practice, I_{pri} will probably be some form of sinusoid and hence P_{PS} and U_{PS} will be somewhat different. (A simulation with a sinusoidal primary current gave substantially identical results.) The power required to ionize the initial charge of D-T gas and heat it to 25 eV is not included in these results, as the initial stages of the startup were not treated in the analysis.

Figure III-17 indicates the energies associated with the various processes. These energies are, of course, the integrals of the corresponding powers shown in Fig. III-14, with a few exceptions. U_T is the total thermal energy which leaves the plasma in the form of 14 MeV neutrons, radiation, and heat transported to the first wall. U_p is the thermal energy of the plasma (approximately $3 n_i k_i T_i$); this is transported to the first wall during the shutdown phase. U_{PS} is the energy delivered by the power supply for the plasma driving (ohmic heating) system. Most of this energy is stored as magnetic energy in the poloidal field associated with the plasma current.

The instantaneous net electrical power is defined:

$$P_{net}(t) = P_T \epsilon_{TH} - \frac{P_B}{\epsilon_B} - P_\Omega - f_{PS} |P_{PS}| ,$$

where the thermal power output is $P_T = P_N + P_L + P_R$, and ϵ_{TH} is the

Fig. III-16. Plasma Driving System Power Supply Power and Energy Output During Base-Case Burn Cycle (Negative value indicates power input to power supply.)

Fig. III-17. Cumulative Energy Components During Base-Case Burn Cycle

thermal-to-electric conversion efficiency, ϵ_B is the beam generation efficiency, and $f_{PS} = 1 - \eta_{PS}$, where η_{PS} is the efficiency with which the plasma-driving system power supply delivers and recovers energy. For these analyses, $\epsilon_{TH} = 0.3$, $\epsilon_B = 0.5$ (see Section V-A), and $\eta_{PS} = 0.95$. The instantaneous values of P_{net} and U_{net} are shown in Fig. III-18 for the 20-sec burn cycle.

The electrical energy produced during the 20-sec burn pulse is $U_T(20 \text{ sec})\epsilon_{TH}$. The cycle-average electrical power is then:

$$\bar{P}_E = \frac{U_T(20 \text{ sec})\epsilon_{TH}}{20} = 23.2 \text{ MWe.}$$

The cycle-average net electrical power is given by

$$\bar{P}_{net} = \frac{U_{net}(20 \text{ sec})}{20} = 8.88 \text{ MWe.}$$

The reactor Q at 20 sec is defined as

$$Q_R = \frac{U_T(20 \text{ sec})\epsilon_{TH}}{\left[U_{\Omega} + \left(U_B / \epsilon_B \right) + U_{NR} \right] 20 \text{ sec}} = 1.62 ,$$

$U_{\Omega} + U_{NR}$ is the nonrecovered energy delivered by the ohmic heating power supply. These definitions are for the basic burn cycle only. They do not take into account the downtime (Phase 5 in Fig. III-10) and other power requirements (refrigeration of superconductors, coolant pumping, auxiliary systems, etc.) which may substantially reduce $\bar{P}_{net}$ and Q_R . However, $\bar{P}_{net}$ and Q_R provide an indication of the power performance.

For longer burn phases, $\bar{P}_{net}$, $\bar{P}_E$, and Q_R increase, since there is more burn for a fixed "overhead" time associated with the startup and shutdown phases. These parameters are tabulated as a function of the burn cycle (Phases 1-4) duration in Table III-7. A burn cycle of approximately 14 sec is the minimum for which $\bar{P}_{net} > 0$. However, significant electrical power could be produced with shorter burn cycles.

Fig. III-18. Instantaneous Net Electrical Power and Cumulative Net Electrical Energy During Base-Case Burn Cycle

Table III-7. Power Performance Versus Burn Cycle Duration
(Circular Plasma)

Burn Cycle Duration (sec)	Cycle-Average Net Electrical Power, $\bar{P}_{net}$ (MW)	Cycle-Average Electrical Power, $\bar{P}_E$ (MW)	Reactor " Q_R "
10	-10.6	18.0	0.63
12	-4.06	19.7	0.83
14	0.56	21.0	1.03
15	2.4	21.5	1.13
16	4.0	21.9	1.22
18	6.7	22.6	1.42
20	8.9	23.2	1.62
25	12.8	24.3	2.11
30	15.5	25.1	2.61
35	17.4	25.6	3.11
40	18.9	26.1	3.61
45	20.0	26.4	4.11
50	20.9	26.7	4.61
55	21.7	27.0	5.11
60	22.4	27.2	5.60
(∞)	(29.1)	(29.1)	

For a 60-sec burn cycle, $\bar{P}_{\text{net}}$ has reached 77% of its asymptotic value, and $\bar{P}_E$ has reached 94% of its asymptotic value.

2. Wall-Sputtered Impurities

It is well known that even small amounts of impurities can seriously affect the plasma power balance and performance. This was amply demonstrated in Section A of this chapter by the steady-state analysis which dealt with a fixed impurity level in an equilibrium plasma. In this section, the effect of the buildup in the plasma of wall-sputtered impurities during the burn cycle is examined.

Although impurities have an effect on the plasma transport coefficients, their chief effect is to cause increased bremsstrahlung, line, and recombination radiation power losses. These losses can extinguish a reacting plasma or, if the impurity level is high enough during startup, prevent the plasma from being heated to operating temperatures. Most of the anticipated impurities in the TEPR will come from sputtering* off either the first wall or some type of intermediate liner between the first wall and the plasma. The amount of sputtered impurities actually in the plasma will depend on several factors. These are the number and type of particles hitting the wall, the sputtering coefficients for each type of particle, the transport of sputtered impurities back to the plasma, and finally the transport of impurities within the plasma itself.

To assess the effect of different wall materials, an analysis was performed using a simple impurity transport model together with sputtering coefficients and representative first-wall materials. The model is described in Appendix A. To reiterate briefly, wall sputtering contributes a single impurity species to the plasma, the concentration of which is governed by:

$$\frac{dn_I}{dt} = S_{DT} \frac{n_I^2}{n\tau_p} + S_\alpha \frac{n_\alpha n_I}{n\tau_p} + S_N n_I^2 \frac{\overline{\sigma v}}{\sigma v} - \frac{n_I n_I}{n\tau_p},$$

*Sputtering, as used herein, includes all surface erosion processes.

where S_{DT} = deuterium-tritium sputtering coefficient = 0.006, S_{α} = alpha particle-sputtering coefficient = 0.05, and S_N = neutron-sputtering coefficient = 0.005. These coefficients are representative of current theoretical and experimental data (see Appendix C) and are assumed to be independent of wall material or incident particle energy. The sputtered impurity is in addition to the 0.5% background oxygen impurity which is present at all times. It is assumed that the impurities diffuse out at the same rate as the ions (as opposed to concentrating at the center, etc.). The alpha particle-sputtering coefficient is the largest; however, n_{α}/n_i is only about 0.01 at the most, so the total alpha sputtering is small. For typical reactor operating conditions (e.g. at the base case equilibrium the individual sputtering contributions are:

$$S_{DT} \frac{n_i^2}{n\tau_p} = 3.3 \times 10^{16} \text{ impurity atoms/sec}$$

$$\frac{S_{\alpha} n_{\alpha} n_i}{n\tau_p} = 2.9 \times 10^{15} \text{ impurity atoms/sec, and}$$

$$S_N \overline{\sigma v} n_i^2 = 1.3 \times 10^{14} \text{ impurity atoms/sec.}$$

Therefore, D-T sputtering is ~ 250 times as great as neutron sputtering and ~ 10 times as great as alpha sputtering. Neutron sputtering is insignificant. If surface conditions are such that "chunk" emission⁽²²⁾ occurs, the neutron sputtering would be two or three orders of magnitude larger and would be the dominant plasma contamination process.

For a plasma at equilibrium, the sputtered impurity fraction is:

$$\frac{n_I}{n_i} \sim S_{DT} = 0.006 \rightarrow 0.6\% \text{ concentration.}$$

The approach to equilibrium of n_I goes approximately as:

$$\frac{dn_I}{dt} \approx S_{DT} \frac{n_I^2}{n\tau_p} - \frac{n_I n_i}{n\tau_p}.$$

For $n\tau_p \approx$ constant, this implies that

$$\frac{n_I}{n_i} \approx S_{DT} \left(1 - e^{-t/\tau_p} \right).$$

The impurity buildup is therefore characterized by a time constant τ_p which is ≈ 10 sec for the confinement scaling used in these calculations. Therefore, for an impurity concentration sufficient to preclude equilibrium operation, the maximum possible cycle time will be $\approx 3\tau_p \leq 30$ sec.

Three different first-wall materials were considered:

- (1) $^{56}_{26}\text{Fe}$ — typical of a stainless steel first wall.
- (2) $^{28}_{14}\text{Si}$ — typical of a silicon-carbide first-wall coating or liner or an aluminum first wall.
- (3) $^{12}_6\text{C}$ — typical of a graphite first-wall coating or liner or of a beryllium first-wall coating.

Using the same reactor model and startup procedures as for the base case, a burn-cycle simulation with $^{56}_{26}\text{Fe}$ sputtering was examined. The buildup of the iron density within the plasma during the startup period is shown in Fig. III-19. When the beam heating was terminated at ≈ 4 sec, the radiation power loss due to the iron impurity is so great that the plasma temperature drops rapidly, as shown in Fig. III-20, extinguishing the burn cycle in less than 6 sec. The corresponding power components are shown in Fig. III-21. The large peak in the radiative power loss corresponds to the peak in the line and recombination radiation, which is at $T_e = 1.94$ keV for $^{56}_{26}\text{Fe}$ (see Appendix A). The instantaneous net electrical power (P_{net}) and cumulative net electrical energy (U_{net}) are shown in Fig. III-22. Although $P_{\text{net}} > 0$ over part of the burn cycle, $U_{\text{net}} < 0$, so that cycle-average net electrical power is negative, although a cycle-average electrical power of $\bar{P}_E = 17$ MWe is obtained.

Fig. III-19. Ion Densities During Burn Cycle with $^{56}_{26}\text{Fe}$ Sputtering

Fig. III-20. Ion and Electron Temperatures During Burn Cycle with $^{56}_{26}\text{Fe}$ Sputtering

Fig. III-21. Instantaneous Powers During Burn Cycle with ^{56}Fe Sputtering

Fig. III-22. Instantaneous Net Electrical Power and Cumulative Net Electrical Energy During Burn Cycle with ^{56}Fe Sputtering

The possibility of improving the power performance by using a neutral beam to provide supplemental power input during the burn phase was examined. After the beam heating phase, the supplemental beam power was varied to maintain the power balance, subject to some specified maximum value. For a maximum allowable supplemental beam power of 20 MW, the burn phase can be extended to about 8 sec before the radiative power losses extinguish the plasma. The plasma temperatures and power components are shown in Figs. III-23 and III-24. In this case, the cycle-average net electrical power is $\bar{P}_{\text{net}} = -12$ MWe and the cycle-average electrical power is $\bar{P}_E = 25$ MWe. The burn phase could be extended by increasing the supplemental beam power limit above 20 MW, but the power performance cannot be significantly improved with regard to $\bar{P}_{\text{net}}$.

For carbon (^{12}C) sputtering the impurity radiation is appreciably lower than for iron, and a more subtle effect comes into play involving the effect of Z_{eff} on the confinement. Since $n\tau \propto Z_{\text{eff}}$, $P_L \propto 1/Z_{\text{eff}}$ for TIM scaling. Relative to the base case, the increase in P_R due to carbon is more than offset by the decrease in P_L . The plasma reaches an equilibrium at a higher temperature than for the base case, thereby violating the β_θ constraint. To prevent this, n_1 must be reduced relative to the base case by 4.3%. This, in turn, reduces the fusion rate. Another problem caused by carbon involves the shutdown phase. As mentioned previously, the length of the shutdown phase is determined by the rate at which the ion density decreases, in order to maintain $\beta_\theta < \beta_{\theta\text{max}}$ during the current ramp down during the final second. During the shutdown, n_I/n_1 tends to increase, causing an increase in Z_{eff} . This, in turn, results in a slower decrease in the ion density, relative to the base case. This effect must be compensated for by increasing the duration of the shutdown phase (6 sec versus 5 sec). The price of doing this is that somewhat less energy is obtained. (For silicon, this effect is even more pronounced; see below.) With these exceptions of the lowered density and the longer shutdown, the burn cycle is substantially the same as the base case. Carbon seems quite acceptable as a first-wall coating or liner material. Beryllium, with $Z = 4$, would also be an acceptable material for this purpose.

Fig. III-23. Ion and Electron Temperatures During Beam-Driven Burn Cycle with $^{56}_{26}\text{Fe}$ Sputtering

Fig. III-24. Instantaneous Powers During Beam-Driven Burn Cycle with $^{56}_{26}\text{Fe}$ Sputtering

For $^{28}_{14}\text{Si}$ sputtering, the reactor performance is a composite of that for $^{12}_6\text{C}$ and $^{56}_{26}\text{Fe}$. The plasma is ignited with no trouble; however, as with carbon, n_i has to be decreased by 6.1% to compensate for the $n_i \propto Z_{\text{eff}}$ effect. As the impurities build up, P_R increases and overrides any gain due to the lower P_L . At 30 sec the temperature starts to drop rapidly and by 40 sec the plasma is out, $T_i \sim 200$ eV. A burn cycle for silicon, therefore, may range from 10 to 40 sec. An analysis of different cycle times within this range revealed an interesting phenomenon: the shutdown time for different burn cycles is quite variable. The maximum burn cycle, 40 sec, is easy to shut down since near 40 sec, T_i and, hence, β_θ decrease rapidly because of radiation. On the other hand, for intermediate cycles, e.g. 20 sec, a full 10-sec shutdown period is required in order to satisfy $\beta_\theta < \beta_{\theta\text{max}}$ near the end of the shutdown phase. This difficulty arises because the particle confinement increases with Z_{eff} . (For pseudoclassical scaling, just the opposite is true, i.e. $n_i \propto Z_{\text{eff}}^{-1}$.) The shutdown question is secondary, however. The primary effect of impurities, increased radiation, will definitely reduce the power performance relative to the base case.

In the previous wall-sputtering impurity calculations, the impurities were assumed to escape from the plasma with a confinement time equal to that of the D-T ions. It is possible, however, that the impurities may be recycled back into the plasma or may concentrate towards the center and not escape. Both of these possibilities may be incorporated into the simulation by eliminating the loss term from the impurity balance equation. This was done for the $^{28}_{14}\text{Si}$ case. The effect is to reduce the time at which the plasma is extinguished from ~ 40 sec to ~ 23 sec because of the more rapid buildup of the impurity concentration. The maximum cycle-average net electrical power is reduced by about a factor of 2.

Figure III-25 summarizes the power performance, as a function of first-wall material and burn cycle duration, of the circular plasma cross-section design with $B_{\text{max}}^{\text{TFC}} = 70$ kG. Both the cycle-average electrical power, $\bar{P}_E$, and the cycle-average net electrical power, $\bar{P}_{\text{net}}$, are shown.

With a stainless steel first wall, the total electrical power is comparable to the base case with the same duration burn cycle, although the

Fig. III-25. Cycle-Average Electrical Power Summary for Burn Cycles with Sputtering

plasma is extinguished so quickly that the burn cycle is about the same duration (10-15 sec) that is required for Phase 5 (exhaust and recharge). In this case, the cycle duty factor is less than one-half and the net power is negative. The stainless steel wall cases, with (Fe^*) and without (Fe) supplemental beam heating during the burn phase, are indicated by single points in Fig. III-25. With a 90% effective impurity control mechanism (curves labeled 1/10 Fe in Fig. III-25), burn cycles up to 30 sec are possible with stainless steel first walls, so that cycle duty factors of about two-thirds are possible. The cycle-average total and net electrical powers with a 90% effective impurity control mechanism are somewhat reduced relative to the base case because the D-T ion density must be reduced in a beta-limited device.

With a silicon-carbide (or sintered aluminum, which has about the same Z) first-wall or liner, burn cycles up to 40 sec are possible. However, the power performance is significantly degraded relative to the base case. With a graphite (or beryllium, with $Z = 4$) first-wall coating or liner, the power performance is only slightly degraded relative to the base case, and the duration of the burn cycle is not limited by impurity buildup.

Several qualifications are in order with respect to the results discussed in this section and summarized in Fig. III-25. The same sputtering coefficients were used for all wall materials (see Appendix C for discussion), and the relative attractiveness of the various materials is dependent upon this assumption. For example, if the carbon sputtering coefficients turn out to be substantially larger than those used in these calculations, the attractiveness of a graphite coating or liner relative to an impurity control mechanism such as a divertor would be diminished. The net electrical power, $\bar{P}_{net}$, does not include coolant pumping, refrigeration or auxiliary systems power, and these could easily be as large as or greater than $\bar{P}_E$. On the other hand, the circular plasma with $B_{max}^{TFC} = 70$ kG is the least favorable of the six reference cases tabulated in Tables III-3 and III-4; $\bar{P}_E$ and $\bar{P}_{net}$ would be larger if the same calculations were performed for a reference case with a larger B_{max}^{TFC} or a noncircular plasma cross section. The confinement may turn out to be better than 15.2 times

TIM, in which case $\bar{P}_{\text{net}}$ and $\bar{P}_E$ could be increased by operating at a higher temperature during the burn phase; or the confinement could be worse than 15 times TIM, in which case $\bar{P}_{\text{net}}$ and $\bar{P}_E$ would have to be decreased due to a lower temperature during the burn phase. In view of these qualifications, the results shown in Fig. III-25 must be considered as preliminary. Nevertheless, it is encouraging that these preliminary calculations indicate that net electrical power is plausible with burn cycles less than a minute in duration.

3. Plasma Heating and Driving Systems Requirements

Factors which determine the neutral beam heating system and plasma-driving (ohmic heating) system requirements are examined in this section. These two systems interact closely with the plasma in the determination of the TEPR power performance.

In the cases considered in the previous section, a 40-MW beam of neutral deuterons was used for ~ 3 sec to heat the plasma. At 180-keV ion beam energy, this beam can be produced with at best approximately 50% efficiency. The beam efficiency depends, among other factors associated with the injection system, upon the ion beam energy, which is determined by plasma penetration requirements (see Section V-A).

The base case burn cycle (without wall-sputtered impurities) was simulated for several different beam heating power levels, and the net electrical power for the cycle was computed for three different values of the beam efficiency ($\epsilon_B = 0.25, 0.50, 0.75$). The results, for three different burn-cycle durations (20, 30, 60 sec) are summarized in Fig. III-26. In each case, the beam heating phase is terminated when the operating temperature ($T = 10$ keV) is reached. The duration of the beam heating phase decreases as the beam heating power increases. The slower startups associated with the lower beam heating powers require somewhat more total heating energy to offset the greater transport power losses during startup. However, the primary effect of the slower startups is the added overhead on the entire burn cycle — the burn phase fraction is reduced, for a given total burn cycle duration. A beam power of ~ 10 MW seems to be the minimum for burn cycles of a minute or less; with 5 MW of beam power the

Fig. III-26. Cycle-Average Net Electrical Power as a Function of Neutral-Beam Heating Power and Beam-Generation Efficiency

plasma does not reach the operating temperature within 1 min. On the other hand, there is no significant advantage to using a beam heating power in excess of ~ 40 MW, even for the 20-sec burn cycles, and ~ 20 MW beam power would be adequate for the 60-sec burn cycles. The beam efficiency, ϵ_B , has a significant impact on $\bar{P}_{net}$, particularly for the shorter burn cycle durations. Beam efficiencies in the range $0.25 \leq \epsilon_B \leq 0.50$ are realistic with positive ion sources (see Section V-A), but $\epsilon_B \approx 0.75$ would require the development of negative ion sources.

The requirements upon the plasma driving system may be defined in terms of the volt-seconds that must be provided to induce and maintain the plasma current, the maximum power which must be provided by the ohmic heating coil (OHC) power supply, and the total energy which must be provided by the OHC power supply. The parameters which characterize the plasma driving system are the coefficient of coupling, K , to the plasma and the power supply loss factor $f_{PS} = 1 - \eta_{PS}$, where η_{PS} is the efficiency with which the plasma driving system power supply delivers and recovers energy. Except as otherwise noted, $K = 0.35$ and $f_{PS} = 0.95$ were used in the analyses.

Table III-8 summarizes the plasma heating and driving system requirements, under a number of different conditions. These results were obtained from burn cycle dynamics simulations of the coupled plasma-plasma heating-plasma driving system, as described in Appendix A.

For the base case conditions, 44 V-sec are needed to induce the 4.5 MA plasma current,* 7.5 V-sec are needed to overcome plasma resistive losses during the startup phases, and 0.9 V-sec are needed for each minute of burn to overcome plasma resistive losses. A maximum power of ~ 1000 MWe must be provided by the plasma driving system power supply, and this power supply must provide 262 MJ of energy over a period of 1 sec. The neutral beam injection system requires 228 MJ of energy to inject 40 MW for ~ 3 sec.

* Accounting for the coefficient of coupling, $K = 0.35$, between the plasma driving system and the plasma reduces the inductive volt-seconds by 6 relative to the estimate given in Table III-3 for which $K = 0$.

Table III-8. Plasma Heating and Driving System Requirements — Circular Plasma

Condition	Neutral Beam Energy ^a (MJ)	Plasma Driving System				Reactor Net Power ^b (MWe)
		V-sec		Power Supply		
		Startup, Total (Resistive)	Burn, per min	Maximum Power (MWe)	Energy (MJ)	
Base case ^c	228	51.5 (7.5)	0.9	979	262	29.1
$\eta = 3 \eta_{sp}$	216	54.0 (9.9)	2.7	1050	293	29.1
$\eta = 10 \eta_{sp}$	195	57.8 (13.7)	8.7	1150	348	28.8
Si sputtering	226	51.0 (6.9)	1.37	979	251	10.4 ^d
C sputtering	226	51.1 (7.0)	1.0	979	253	28.6 ^d
Fe $\times 1/10$ sputtering	230	51.5 (7.5)	1.14	979	262	10.1 ^d
K = 0.25	228	54.5 (7.5)	0.9	2020	502	29.1
K = 0.50	228	45.1 (7.5)	0.9	472	135	29.1
K = 0.75	228	29.4 (7.5)	0.9	133	67	29.1
Ramp time						
0.5 sec	230	48.5 (4.4)	0.9	1740	262	29.1
5.0 sec	224	73.6 (29.5)	0.9	530	523	29.1
Beam turned on						
at t = 0	248	44.6 (0.5)	0.9	848	204	29.1
at t = 0 with $\eta = 10 \eta_{sp}$	234	46.8 (2.8)	8.7	998	239	28.8

^a40-MW beam D_0 ; 50% beam generation efficiency.^bAsymptotic value. Reaches approximately 80% of this for burn cycle ≈ 1 min.^c $\eta = \eta_{sp}$, no wall sputtering, K = 0.35, 1-sec current ramp, beam on at $t = 1$ sec.^dFor a 30-sec cycle.

Anomalously large plasma resistivities have been observed in some experiments.³ The main effect of increasing the plasma resistivity to as much as ten times the Spitzer value ($\eta = 10 \eta_{sp}$) is to increase the resistive volt-seconds required for startup to 13.7 and for the burn phase to 8.7 per minute, and hence to increase the energy that must be provided by the plasma driving system power supply. Because of the additional ohmic heating, the beam heating requirements are reduced.

The presence of wall-sputtered impurities does not significantly affect the requirements on either the plasma heating or driving systems. With wall-sputtered impurities, the D-T ion density must be reduced, and somewhat different burn cycles are followed than for the base case, so that the differences are not straightforward.

The coefficient of coupling, K, has a major effect upon the plasma driving system requirements. K is determined primarily by the placement of the coils. These calculations are based upon an estimated value of $K = 0.35$ for the coil configuration of the TEPR, with the plasma driving coils located external to the toroidal field coils. The results reported in Table III-8 for different values of K indicate the high degree of sensitivity of the plasma driving system power supply requirement to the coupling between the plasma driving system and the plasma. In general, the inductive volt-seconds scale as $(1 - K^2)$, and the maximum power and the energy required from the power supply scale as $(1/K^2 - 1)$.

The duration of the current ramp (Phase 1) also has a major effect on the plasma driving system requirements. Halving the current rise time almost doubles the maximum power required, while almost halving the plasma resistive volt-second requirements during startup. On the other hand, a longer current rise time substantially increases the plasma resistive volt-second requirements and the energy that must be provided by the power supply, while decreasing the maximum power required from the power supply.

It is not necessary to divide the startup into two separate phases. The beam heating can be initiated simultaneously with the initiation of the current rise. This procedure heats the plasma more rapidly, thereby reducing the plasma resistive volt-second requirements during

startup. For the base case the plasma resistive volt-seconds during startup are small, and there is little advantage in operating in this manner. However, if the plasma resistivity was anomalously large, the total volt-second requirement could be substantially reduced by this startup procedure. The rise time and beam turn-on time also affect the electrical power performance as shown in Fig. III-27.

These results do not include the initial plasma ionization and heating to 25 eV requirements upon the plasma driving system. These latter requirements could be substantial and may necessitate a separate plasma initiation system.

The effect of the plasma driving system parameters upon the TEPR power performance is illustrated in Fig. III-28. The net cycle-average electrical power is relatively insensitive to the coefficient of coupling, K . Although the amount of energy which must be provided and recovered by the plasma driving system power supply increases rapidly with decreasing K , only 5% of this energy enters $\bar{P}_{\text{net}}$ as a loss. The power performance is more sensitive to the efficiency $\eta_{\text{PS}} = 1 - f_{\text{PS}}$ with which the plasma driving system power supply provides and recovers energy. However, even for $\eta_{\text{PS}} = 0.85$, $f_{\text{PS}} = 0.15$, which would be a rather poor power supply by current standards, the reactor power performance is not degraded too severely.

The analysis discussed in this section was based upon the circular plasma design with $B_{\text{max}}^{\text{TFC}} = 70 \text{ kG}$, which was chosen to define something of a lower bound on the expected power performance. This design model also yields a lower bound (in the same sense) on the requirements on the plasma heating and driving system, because it was the lowest plasma density ($n_1 = 0.49 \times 10^{20} \text{ m}^{-3}$) and plasma current ($I_p = 4.5 \text{ MA}$) of the ignition cases considered in Tables III-3 and III-4. It is useful to estimate these requirements for the other cases. The beam energy required to heat the plasma to a given operating temperature should scale approximately as the plasma density, since the plasma volume is the same for the circular and noncircular designs and the heating energy was found to be approximately equal to the plasma thermal energy. The plasma density only varies by ~ 1.5 among the various ignition cases in Tables III-3 and III-4, so 1.5 times the neutral beam energy shown in Table III-8 should

Fig. III-27. Cycle-Average Electrical Power as a Function of Current Ramp and Beam Turn-On Time

Fig. III-28. Cycle-Average Net Electrical Power as a Function of Plasma Driving System Parameters

be a reasonable upper limit on the beam heating requirements. The inductive volt-seconds are larger for the noncircular design, as shown in Tables III-3 and III-4. Since magnetic energies scale as I_p^2 , the power supply requirements for the plasma driving system should scale similarly, to a first approximation. Thus, for the noncircular design with $B_{TFC}^{max} = 75$ kG, the power supply requirements are estimated to be ~ 2.9 times those shown in Table III-8.

It is well to keep in mind that there are a number of factors which could cause these estimates of the plasma heating and driving systems requirements to change substantially. A significant impurity concentration in the plasma could necessitate the use of higher energy (hence less efficient) neutral beams in order to penetrate the plasma and deposit the energy preferentially towards the center. The sizeable orbits of the fast ions could result in an appreciable fraction of the beam energy being dumped on the first-wall, particularly for counter-injected beams. Both of these effects, which are discussed in Section V-A, tend to increase the neutral beam power requirements. The requirements on the plasma driving system were established on the basis of a model which represented this system as the primary of a simple transformer, whereas the ohmic heating and the equilibrium field systems together constitute the plasma driving system. A more refined representation, in which the interactions among the plasma, the ohmic heating system, and the equilibrium field system were treated explicitly, would yield more specific, and probably less demanding, requirements upon the individual components of the plasma driving system.

4. Other Confinement Laws

The plasma particle and energy confinement is perhaps the biggest area of uncertainty in projecting today's plasma physics to a device in the TEPR class. The uncertainty includes both the magnitude of the confinement parameter and the scaling with density and temperature. Both the magnitude and the scaling are important: the magnitude determines if a power balance can be attained at all; the scaling largely determines the plasma operational dynamics. The previous dynamic analyses have been based on the assumption that the confinement was

15.2 times better than TIM, which is sufficient for the circular plasma designs with $B_{\max}^{\text{TFC}} = 70$ kG to be an ignition device at $T_e = 10$ keV. If the confinement turns out to be as poor as predicted by TIM theory, then the transport power loss would be 15.2 times too large for an equilibrium to obtain at $T_e = 10$ keV with the same plasma density that was used in the base case of the previous sections. A plasma condition for the burn phase which minimizes the transport power loss, P_L , must be found. For trapped ion mode scaling, $P_L \propto T_e^{4.5}$. Thus, to reduce P_L , the plasma should operate at the minimum T_e tolerable. This is about $T_e = 6$ keV in order to still have significant fusion. Since the alpha heating power $P_\alpha \propto n_1^2$, n_1 can be increased up to the limit determined by $\beta_\theta = 2.2$. A search for the best operating point yielded $T_e = 6.3$ keV, $T_i = 6.4$ keV, $n_1 = 0.78 \times 10^{20}/\text{m}^3$. Even with these changes P_L is too large. The power terms are $P_\alpha = 14.6$ MW, $P_\Omega = 0.1$ MW, $P_L = 33.0$ MW, and $P_R = 6.2$ MW, so that the power equation is unbalanced, $P_\alpha + P_\Omega - P_L - P_R = -24.5$ MW. To attain a power balance the plasma must be driven. 23 MW of supplemental beam power produces ~ 1.5 MW of suprathreshold alpha fusion power, and will therefore make up the deficit. The power performance of a driven system with TIM scaling is summarized in Fig. III-29. A substantial cycle-average electrical power, $\bar{P}_E$, can be obtained, but $\bar{P}_{\text{net}} < 0$ if TIM scaling obtains. This power performance would be better for $B_{\max}^{\text{TFC}} > 70$ kG or for a noncircular plasma.

The confinement may scale with temperature and plasma density differently than is predicted by the trapped particle instability theory. If confinement scales as predicted by the pseudoclassical relations, the entire burn cycle dynamics is altered. Although a detailed analysis has not been performed with pseudoclassical (PC) confinement scaling, several cases were analyzed using pseudoclassical scaling for particle and electron energy confinement and neoclassical scaling for ion energy confinement — an extension into the reactor regime of the confinement scaling which seems to agree with present experiments. These cases correspond to the first entry in Table III-3 in that the same particle density was used and the same burn phase equilibrium was sought.

Fig. III-29. Cycle-Average Electrical Power as a Function of Confinement

The startup period (current rise and beam heating phases) was similar to that discussed for the base case in previous sections, because the trapped particle modes are only dominant towards the end of the beam heating phase. Therefore, previous conclusions about the plasma heating and driving system requirements and operating modes are largely unaffected.

However, towards the end of the beam heating phase and in the burn phase, the burn cycle dynamics is considerably different for the PC scaling. The PC confinement is greater than is needed for a power balance at $T_e = 10$ keV. Thus, the plasma temperature continues to increase in the burn phase until radiation power losses balance the alpha heating, with the β_θ constraint being violated. Increasing the background oxygen impurity to 2.5% increases the radiation power loss sufficiently that a power balance equilibrium obtains at $T_e \approx 10$ keV. However, this equilibrium is thermally unstable and cannot be controlled by holding the density constant, as was the case for TIM scaling, because the unstable mode is a pure temperature instability.⁽²⁰⁾ Thus, some form of control or feedback stabilization is required in order to maintain an equilibrium during the burn phase, if the confinement scaling turns out to be pseudo-classical. With stabilization, the power performance with PC scaling is essentially identical to the power performance with 15.2 times TIM scaling (the base case of the previous two sections).

With $^{56}_{26}\text{Fe}$ sputtering from a stainless steel first-wall the PC confinement is still greater than is required for a power balance at $T_e \approx 10$ keV, and it is necessary to increase the background oxygen impurity to 1.3% to obtain a power balance during the burn cycle. Thus, if the confinement scaling turns out to be pseudoclassical, wall-sputtered impurity contamination of the plasma is not expected to be limiting. This conclusion is subject to the same qualifications that were discussed at the end of Section III-B-2.

With PC confinement scaling, the possibility of ohmically heating the plasma to the burn phase operating conditions exists. This procedure is based on the following considerations: The ohmic heating power scales as $P_\Omega \propto Z_{\text{eff}}/T_e^{3/2}$; the bremsstrahlung radiation loss scales as

$P_R \propto n_1^2 Z_{\text{eff}}^{1/2} T_e^{1/2}$; and the transport power loss scales as $P_L^{\text{PC}} \propto n_1^2 Z_{\text{eff}}^{1/2} T_e^{1/2}$ for PC scaling. The radiative and transport power losses during startup can be reduced by initiating the heating at a reduced plasma density, thus increasing the ohmic heating per ion. When fusion temperatures (~ 4 keV) are reached, the plasma density is increased because the alpha heating power scales as $P_\alpha \propto n^2 \overline{\sigma v}(T_i)$. [Low density startup does not work with TIM confinement because the dominant transport power loss scales as $P_L^{\text{TIM}} \propto T_e^{9/2} / Z_{\text{eff}}$, i.e. $P_L^{\text{TIM}} \neq f(n_1)$.] The startup phase duration required for low-density ohmic heating to the burn phase conditions is about 80 sec, which is consistent with a previous study.²¹ This duration is too long to achieve satisfactory power performance from a TEPR with burn phase durations in the range 30-60 sec, as have been considered. However, if much longer burn phase durations are achievable and if the confinement scaling turns out to be pseudoclassical, then the TEPR design can be ohmically heated to ignition.

C. Plasma Physics Considerations

The stable confinement of a D-T burning plasma with the parameters postulated for the TEPR will be a landmark accomplishment for the tokamak confinement program. The primary purpose of this section is to briefly outline where the current plasma physics program stands in relation to what is required for the TEPR and to indicate how information from upcoming experiments is expected to contribute to the plasma physics basis for the TEPR design. A secondary purpose of this section is to comment on several effects which have not been treated explicitly in the analysis.

1. Confinement

The best of present experiments achieve τ values on the order of $4-8 \times 10^{17}$ sec/m³ (see Table I-2). The confinement is roughly consistent with pseudoclassical scaling for the particles and electron energy and with neoclassical scaling for the ion energy.⁽³⁾ With these scalings the confinement improves as the temperature is increased. For temperatures above 1 keV, however, trapped particle instabilities are theoretically expected to reduce the confinement. Anomalous confinement attributable to trapped particle instabilities may have been observed in a tokamak-like magnetic field configuration on the FM-1⁽²³⁾ and in the French TFR.⁽²⁴⁾

As described in Section III-A, the TEPR requires an $n\tau$ of about $3-5 \times 10^{20}$ sec/m³ or greater (depending on the impurity level and temperature) to achieve net electrical power production. Sufficient thermal power to satisfy the other TEPR objectives could be produced in a driven device operating as low as $n\tau \approx 10^{19}$ sec/m³.

Most of the difference between the present values and the required value of $n\tau$ is expected to be achieved through increasing size. (Except in the unlikely event that diffusion increases faster than a^2 , building larger devices cannot be harmful.) If TIM scaling, which is the most pessimistic of the commonly predicted scaling laws at reactor temperatures,⁽³⁾ turns out to be the case, a device of roughly 10 m is required for $B_{\text{max}}^{\text{TFC}} = 75$ kG.⁽²⁵⁾ On the other hand, the TEPR described in this report requires confinement about ten times better than predicted by TIM if it is to achieve net electrical power production. The prospects of having $n\tau$ an order of magnitude better than this most pessimistic prediction are probably good.⁽⁴⁾ It should be kept in mind that the TIM prediction is based on a very rough estimate and does not take into account several potentially stabilizing effects, such as Landau damping and enhanced collisionality due to impurities, cross-section elongation, and finite β .⁽²⁶⁾ There is no experimental evidence that TIM transport exists and only sketchy evidence that other anomalous modes exist.

The next generation of experiments is expected to provide the requisite information on the $n\tau$ scaling. PLT and T-10 are expected to begin operation in late 1975. These machines are about twice as large (in linear dimension) as present experiments and are anticipated to reach $n\tau \times 10^{19}$ sec/m³ and temperatures up to ≈ 5 keV. This should put them in the trapped-electron-mode (TEM) regime near the trapped-ion mode (TIM) regime. Doublet III is expected to begin operation in 1978. It is bigger than the preceding devices as well as being noncircular. It could achieve $n\tau \approx 10^{20}$ sec/m³ at $T \approx 5$ keV. The TFTR, which is more than twice as large as PLT, is expected to begin operation in 1980. Values of $n\tau > 10^{19}$ sec/m³ at $T \approx 5-8$ keV are anticipated. The latter two machines should be into the TIM regime. Apart from size, Doublet III and the TFTR are expected to have densities and temperatures very near those of the TEPR. The extrapolation to the TEPR should then be mainly one of size, unless new transport mechanisms are identified.

2. MHD Stability — q and β_θ

Most of the performance parameters in this report assume that the TEPR could operate at $q \approx 2.5$ and $\beta_\theta = 2.2$. Values near these would be necessary for the power objectives of the TEPR to be met with a circular cross section plasma. On the other hand, in present experiments typical values are $q \approx 4-5$ and $\beta_\theta \approx 0.5-1.0$. (See Table I-2.)

The T-6 tokamak has been operated with $q(a)$ as low as ≈ 1.1 , using a closely fitting liner,⁽²⁷⁾ but generally operation below $q(a) \approx 3$ is accompanied by increased fluctuations and a degradation of confinement time.^(3,28) In order to suppress the $m = 1, n = 1$ kink mode, which is usually accompanied by the disruptive instability, it is necessary to have $q(r) > 1$ (Kruskal-Shafranov limit). Recent experiments on the ATC have achieved $q(0) < 1$ without onset of the disruptive instability, but β_θ/n_e was found to decrease linearly below $q(0) \approx 1$.⁽²⁹⁾ The commonly used lower limit, $q(a) = 2.5$,⁽³⁾ is the value of $q(a)$ such that $q(r) > 1$ everywhere in the plasma for a parabolic current distribution. For a flatter distribution, $q(a)$ could be less, and for a more sharply peaked distribution, $q(a)$ must be more. It is possible that in a larger device one could have localized kink modes with $q(0) < 1$ near the center without having overall instability. This would permit a lower q elsewhere in the plasma.

The presently achieved values of β_θ are probably the most that can be obtained with ohmic heating alone,⁽³⁰⁾ and should not necessarily represent an upper limit on β_θ . A commonly accepted upper limit for β_θ is $\beta_\theta \lesssim A$.⁽³⁾ At $\beta_\theta \approx A/2$, depending on the pressure distribution, the separatrix of the combined equilibrium and poloidal fields begins to enter the plasma volume.⁽⁹⁾ The adiabaticity of the magnetic moment is lost near the field null, and the plasma is connected by field lines which leave the volume when this occurs. The confinement should be expected to deteriorate. It should be noted that for noncircular cross sections, the separatrix begins to enter the plasma volume at $\beta_\theta \approx R/2a$,⁽⁹⁾ not the more restrictive $\beta_\theta \approx \bar{A}/2$, where a is the minor dimension of the cross section, and $\bar{A}$ is the average aspect ratio. More restrictively it has been found numerically that equilibria with $\beta_\theta \lesssim 2$ would be unstable to kink modes, have reversed currents, be squashed to one side of the chamber, or have other undesirable features.⁽¹⁰⁾ The preceding are limits for which equilibrium is obtained. Stability

against kink modes would limit β to $\approx 0.2/A$ for circular cross section devices and up to $\approx 0.4/A$ for elliptical cross sections.⁽³¹⁾ These limits ($\beta_\theta \approx 0.2 q^2 A$ and $\beta_\theta \approx 0.4 q^2 A$) are similar to those for equilibrium. Both PLT and TFTR will be provided with sufficient supplementary heating to determine if β_θ can be driven past the ohmic heating limit without deleterious effects.

3. Noncircular Cross Sections

Vertically elongated, noncircular cross section plasmas have a large potential advantage over circular cross section plasmas, as can be seen in Tables III-1 and III-2. This comes about because the power is proportional to $\beta_\theta^2 B_c^4 / (q^4 \bar{A}^4)$, and the noncircular cross sections have a higher B_c (since more of their volume is in the higher field region closer to the toroidal field coils) as well as a lower $\bar{A}$ than a circular machine of the same volume. On the other hand, this advantage would be reduced if the noncircular cross section plasmas had to operate at higher q and lower β_θ , if other instabilities such as vertical modes occurred, or if confinement, for example, were worse. One of the most pertinent questions is whether the field shaping coils produce the optimum cross section shapes when they are far enough away from the plasma to be shielded from radiation.

Experimentally, Doublet II, with an approximately three-to-one elongated cross section formed by a copper liner, has obtained plasma characteristics similar to those of comparable tokamaks (ST, for example), except that the toroidal magnetic field was much lower.⁽¹¹⁾ Doublet II has been modified to Doublet IIA with field shaping coils instead of the liner.⁽³²⁾ This configuration is more pertinent to a reactor and should allow more complete diagnostics. Other tokamaks with noncircular cross section plasmas are Rector at the Massachusetts Institute of Technology and the Finger-Ring Tokamak T-9 at Kurchatov. Most of the effort in these machines has been in producing the desired cross sections. There are still many unresolved questions.

The Princeton PDX, expected to begin operation in 1978, will have a noncircular cross section, as will all tokamaks with divertors. The most relevant device to the TEPR in terms of noncircular cross sections is Doublet III, expected to be operational in 1978. It will have a 3:1 elongated cross section with major radius 1.4 m and plasma half-width 0.45 m. It is expected to operate at densities ($\approx 2 \times 10^{20} \text{ m}^{-3}$) and temperatures ($\approx 5 \text{ keV}$) similar to the TEPR.

4. Impurities

One of the most crucial problems confronting the TEPR is whether the impurity level can be made sufficiently small to allow ignition and to avoid premature quenching of the burn. Current experiments find a Z_{eff} on the order of 3-7 composed of 1-10% oxygen and carbon and of 0.1-1.5% heavy metal ions. (29,33-35) These devices use high Z_I materials such as molybdenum, tungsten, and gold for liners, limiters, and other structural components, accounting for the presence of the heavy metal impurities.

If the TEPR is to achieve ignition at ≈ 10 keV, the maximum allowable concentrations of some typical impurities are about: 8% ^{12}C , 5% ^{16}O , 1% ^{28}Si , 0.1% ^{56}Fe , or 0.01% ^{96}Mo . (See Fig. I-4. If several of these impurities were present, the individual contributions would be less.) These maximum levels correspond to $Z_{\text{eff}} \approx 1-4$, with the higher Z_{eff} being for the lower Z_I . In addition to these requirements for ignition, consideration of D-T sputtering (see Section III-B-2) indicates that materials with $Z_I \gtrsim 20$ may present severe problems if used as structural materials where they are exposed to the plasma. Assuming that the high- Z_I ions could be eliminated from present discharges, the levels of carbon and oxygen are not too far from what could be tolerated for ignition in a TEPR.

Neoclassical theory predicts that impurities diffuse toward the center of the plasma. (36) Measurements on the Russian T-4 and TM-3 indicate a concentration of impurities near the center of the plasma, (37) but no evidence of appreciable buildup is found on ST (33) or ORMAK. (35) The neoclassical prediction follows from momentum conservation in classical collisions. If the diffusion is anomalous, the argument does not apply; however, a prediction of the appropriate impurity diffusion in this case apparently does not exist. It can be shown that impurities do substantially modify the trapped particle instability, usually toward stability. (38-40) The most obvious effect in TIM is the increase in n_i with Z_{eff} . It is quite possible that for these effects to be important, the impurity concentration must be too high to achieve ignition.

Two potential means of controlling impurities are divertors and gas blankets. Each has its attendant problems, and it is early to say which, if either, is appropriate for the TEPR. The Princeton PDX, which should begin operation in 1978, is designed to investigate the divertor problem, and the

Holifield ISX is proposed to investigate impurity problems in general. The PLT, T-10, and the TFTR should also give information on impurity behavior in plasmas approaching reactor grade.

5. Heating

Some form of supplementary heating will be required for the TEPR. Possible means of supplying this heating are neutral beam injection, compression heating, wave heating, turbulent heating, or some combination of these. Experimentally, the highest energy densities to date have been obtained on the compressed ATC plasma with parameters: $n_e \approx 10^{20} \text{ m}^{-3}$, $T_e \approx 2 \text{ keV}$, and $T_i \approx 0.6 \text{ keV}$.⁽²⁹⁾ The TEPR requires $T_i \approx T_e \approx 10 \text{ keV}$ and $n_i \approx 5 \times 10^{19} \text{ m}^{-3}$, but the volume would be about three orders of magnitude larger than in current experiments.

Neutral beam injection, which is discussed more extensively in Chapter V, is probably the most promising heating method at this time. Current experiments, which have 15-25 keV injectors with 100-200 kW total power, obtain heating that is fairly consistent with theoretical predictions, and there appear to be no anomalous losses.^(34,41) The TEPR, on the other hand, would require $\approx 40 \text{ MW}$ of power at $\approx 180 \text{ keV}$ to reach operating temperatures. Depending on the confinement, additional heating might be needed to sustain the burn. It is planned to have $\approx 35 \text{ MW}$ at 80 keV and $\approx 7 \text{ MW}$ at 150 keV neutral beam heating on the TFTR.

A parameter of interest in neutral beam injection is Γ , the ratio of beam energy density to the total plasma energy density. (A related parameter, Γ' , which is the ratio of the beam energy density to the thermal plasma energy density, is often used.) It is generally felt that operation at $\Gamma < 1/2$ (or $\Gamma' < 1$) will not produce any deleterious effects. Above $\Gamma = 1/2$ more of the magnetic field is used to contain the beam than the plasma, and there is a possibility that microinstabilities will occur.⁽⁴²⁾ Present experiments achieve $\Gamma \approx \Gamma' \approx 0.2$.⁽³⁴⁾ It is planned to try to achieve $\Gamma = 1/2$ on the ATC, and the TFTR is intended to operate at this level. The TEPR will need these higher values of Γ only if confinement is as low as $n\tau \approx 10^{19} \text{ sec/m}^3$.

Adiabatic compression in major radius has provided a factor of 2 increase in T_e and a factor of 3 increase in T_i for the ATC.⁽²⁹⁾ This is the most successful method of heating at present, and a combination of neutral beam injection and compression is planned for the TFTR.

Wave heating has the advantage that high power ($\gtrsim 0.5$ MW continuous) sources already exist. The problems are associated with the wave coupling structures, which must be near the plasma. The advantage of turbulent heating is that the heating occurs rapidly, so radiation losses are small. On the other hand, it may be hard to achieve high densities or high β_0 by this means.

6. Alpha Particle Dynamics

High energy alphas do not appear to any significant extent in present experiments, and therefore their distributions, transport, and other properties are unknown. The calculational model used in this report assumes the alpha particles slow down to the ion temperature, then diffuse out with the same n_T as for the particles. (The slowing-down time is typically an order of magnitude shorter than the particle containment time.) In reality several effects can change this simple model. One is that some alpha particles will be born on banana orbits that intersect the walls. These alphas will be lost with their original energy of 3.52 MeV. The loss depends on the source distribution and the machine size. For the TEPR design in this report this loss could amount to as much as 25% for a flat source distribution, but a more reasonable, peaked distribution would give 5-10% loss.⁽¹⁴⁾ Another source of 3.52 MeV alpha loss is from asymmetric trapping in toroidal field ripples.^(43,44) (See next subsection.) The percentage of alphas lost in this way is on the order of the field ripple and might be 1-2% for the ripples caused by the sixteen toroidal field coils in the reference design. (The localized damage to the first wall is probably more important than the loss itself.)

Apart from the obvious loss of heating from the lost alphas, the plasma tends to become negatively charged. The potential buildup could be as much as hundreds of kilovolts per second.⁽¹⁴⁾ The importance of this effect is not clearly understood.

The slowing-down alphas form an equilibrium distribution which has an average energy of ~ 1300 keV. It could be expected that some of these alphas would diffuse out before they slowed down. In addition, for the alphas that do thermalize, the transport is not known. (Confinement is assumed to be the same as for the electrons and ions. See Appendix A.) The neoclassical prediction that impurities diffuse toward the center of the plasma⁽³⁶⁾ could be applied to the alphas as well. However, the alphas

are born with the D-T fusion distribution, and if they are assumed to stay on the same flux surface while slowing down, neoclassical theory predicts that the alphas will diffuse outward, with inward D-T diffusion to maintain ambipolarity.⁽⁴⁵⁾ If the diffusion is anomalous there is apparently no corresponding prediction of the alpha diffusion.

There may well be instabilities associated with the presence of alpha particles. Two examples are instabilities associated with compressional Alfvén modes⁽⁴⁶⁾ and ion Bernstein modes.⁽⁴⁷⁾ These modes are unstable for some ranges of the slowing-down energy, but it is not known if there is net instability when the entire range of slowing-down energies is considered. A comprehensive survey of possible alpha instabilities has apparently not been made.

It should be emphasized that alpha particles will be a major feature of the TEPR plasma, and the general area of alpha particle dynamics is one which requires a great amount of additional study. One helpful advantage is that the alpha dynamics are in many ways similar to the beam dynamics,⁽⁴⁸⁾ an area of intense current interest.

The TFTR is expected to produce an appreciable number of alphas (with a D-T plasma), so that this device should provide some information on alpha particle-related phenomena in plasmas. However, the TFTR is a highly beam-driven device, and beam dynamics are likely to be more important than alpha dynamics; whereas the converse is true for the TEPR.

7. Nonaxisymmetry

At the low collision frequencies associated with a reactor-grade plasma, particles can be trapped in the magnetic wells associated with toroidal perturbations, such as the ripples caused by the spacing between the toroidal coils.^(43,44) This problem is compounded by the fact that a TEPR will need access for experiments and remote maintenance. The importance of this effect is indicated by the fact that electrons trapped in this type of well are thought to be responsible for the hole burned in the liner of the French TFR during its early operation.⁽²⁴⁾

The wells caused by the field ripple are localized as a function of the poloidal angle, θ (as well as the toroidal angle ϕ). The orbits of particles trapped in these wells are not closed as for bananas but cause a drift

$v_D \sin \theta$ toward (or away from) the walls, where v_D is the grad-B drift velocity. Particles with a large v_D hit the wall before making a collision that would detract them. This is the case for the 3.52-MeV alphas (and for the electrons in TFR, which were accelerated to ≈ 50 keV by an instability initiated by a plasma-runaway electron interaction⁽²⁴⁾).

Even if the particles do not drift all the way out before colliding, this asymmetric trapping gives rise to "ripple" diffusion.^(49,50) For parameters typical of a TEPR, this diffusion is predicted to be larger than the comparable neoclassical diffusion for ripples larger than $\approx 2.5\%$.⁽⁴⁹⁾ (The reference design in this report has a $\approx 2\%$ ripple.) The loss (from either the drift or the diffusion) is localized and may give rise to high wall loadings in localized areas. In addition, velocity-space instabilities can be driven from the anisotropy produced by the lost particles.

A possible beneficial result of field ripple is that the toroidal momentum imparted to the plasma by the injection of neutral beams may be dissipated by friction with the particles trapped in the field ripples.⁽⁵¹⁾

8. Refueling and Edge Effects

In the calculational model it is assumed that cold ions can be supplied in some unspecified manner to refuel the plasma. This fuel replenishment is typically on the order of 10^{19} particles/m³-sec, which corresponds to replacing the plasma once every few seconds. In present devices, which operate for several confinement times, many of the ions which escape are recycled back into the plasma as neutrals. A larger, thermonuclear plasma is, however, predicted to be impermeable to these neutrals,⁽⁵²⁾ so refueling by this means may not be possible.

The major proposed schemes for refueling are neutral beam injection, pellet injection, and gas blankets. Neutral beam injection provides energy, but possibly too few particles for refueling. [The 180-keV neutral beam considered in the analyses of Section A provided a maximum of only 20-30% of the refueling current needed for steady-state operation, and except for the highly driven ($Q_p \approx 1$) cases, the fraction of the current provided was negligible.] In addition, the energy required to produce the beam is large. Pellet injection has the advantage that penetration into the plasma can be achieved with less energy than for neutral beams, but the accelerating

potentials required (because of the small e/m) may be difficult to obtain.⁽⁵³⁾ In a gas blanket, fueling and ash removal take place via diffusion through the blanket, and the reaction energy is lost by heat conduction in addition to neutron transport and radiation. Solutions of such equilibria have been found,⁽⁵⁴⁾ and appear to be stable.⁽⁵⁵⁾ They are not necessarily optimum for reactor operation.

All of these methods require further study, and the problem of refueling a steady-state reactor is very much an open question.

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CHAPTER IV

MAGNET SYSTEM

This chapter covers the design studies of the TEPR magnet system. The TEPR magnets treated here consist of the toroidal-field coils, the equilibrium-field coils and the ohmic-heating coils. The objective of the study is to develop a completely integrated design and to determine the necessary research and development requirements for a TEPR magnet system. The approach here is to develop an understanding for the complete TEPR and to design magnets that are an integrated part of the system, rather than to develop an isolated magnet design that does not fully account for all the problems of a TEPR. Consequently, at the beginning of each magnet system, a detailed account is given of the interaction between each magnet and the rest of the system. Although this is a design-scoping study, the design of each magnet system was sufficiently developed so as to provide enough information about the problem areas to indicate the necessary research and development program. The required R & D program is described in Appendix G.

The toroidal-field coil is described in Section IV-A. It is clear that the toroidal-field level must be as high as possible to get the most power out of the TEPR. Also, the toroidal-field coils must be superconducting in order to achieve net power out. NbTi is the best superconductor to use because of its ductility. The specification of a peak field of 75 kG (at 4.2°K) at the magnet winding is based on a practical judgement concerning some degree of margin required in a real system subjected to the superimposed rapidly-pulsed field of the order of 5 kG from the equilibrium coil, the ohmic-heating coil and the plasma current. Furthermore, the local temperature fluctuation during operation due to the pulsed-field dissipation or neutronic heating cannot be anticipated with good certainty at this time. In this design study, it was found that, for a TEPR, fully-stabilized coils can be designed and a high current density is neither necessary nor desirable. One of the big problems with toroidal magnets of this size is the support of the enormous forces generated by the coils. For this reason, a pure-tension coil shape, with no bending moments, is necessary. A new analytical approach has been developed to determine a pure-tension profile for the coils, with a full account for the effect of coil bumpiness and the effect of the actual coil cross-section. The 3-dimensional stress analysis verified that the coils do indeed approach the pure-tension criterion. The

pure-tension coil computation model is presented in Appendix E. A detailed conductor design is given. Taking into account the radiation-induced resistivity of the copper stabilizer, the conductor stability was fully evaluated to fulfill the cryostatic-stabilization criterion. The coil structure, the winding scheme and the cryostat design are also described. A sound coil protection scheme is proposed. The series-connection scheme allows the energy to be removed sufficiently rapidly to prevent coil damage under any malfunction condition. In addition, it assures that each coil carries equal current at all times so that there is no additional large force generated when the magnet quenches. The coil heating due to the superimposed field, the nuclear heating, and the cryostat heat leak are evaluated in detail. Finally, the interaction between the toroidal coil and the vertical field is also evaluated and is described in Appendix E.

The equilibrium-field coil is described in Section IV-B. Detailed design considerations on the equilibrium-field requirements are given. The required equilibrium field for the Argonne TEPR reference design is 3 kG. The equilibrium coils are carefully arranged so that the vertical-field lines satisfy the plasma-stability requirements. Furthermore, they are so arranged that they are decoupled from the ohmic-heating coil and yet produce a bonus of 37 volt-seconds for the plasma ring, thus reducing the volt-second requirements for the ohmic-heating coil. The preferred design is the superconducting coil. A 15,000 A fast-pulsed ac superconducting cable is proposed. The ac pulsing loss of the cable is fully evaluated. The cable is designed so that the fully-stabilized criterion can be fulfilled, even when it is pulsed at 3 kG/sec. A water-cooled copper coil is a design option. The I^2R loss of such a coil is shown to be around 92 MW.

The ohmic-heating coil is described in Section IV-C. The required volt-seconds is 73, the flux-core radius is 1.9 m, and the required central field is 32 kG, if the coil is superconducting. The stored energy is around 883 MJ. The air-core transformer has the advantage of not requiring a massive iron core and, above all, leads to a smaller core-size requirement. The air-core superconducting transformer is the preferred design over the air-core water-cooled copper coil because it has a smaller power loss. The superconducting ohmic-heating coil is designed to be fully stabilized because of its large size and stored energy. A design of a fully-stabilized 40,000 A ac cable, suitable for pulsing at 64 kG/sec, is presented. The ac pulsing loss of the cable is

evaluated. In addition, a water-cooled copper coil is also designed as an option. The I^2R loss of such a coil is shown to be around 4 MW with a special charging scheme proposed.

A. Toroidal-Field Coils

Tokamaks require a strong toroidal field (TF) for MHD stability. Other confinement and MHD-stability parameters are related to the magnitude of the toroidal field (B_t) in such a way that the output of the reactor is proportional to B_t^4 (See Section I-B). The toroidal-field coils must be superconducting for a reactor such as the TEPR in order to realize net power out. Ordinary water-cooled copper coils would require more power than the TEPR will generate.

It is desirable to have the toroidal field as high as possible to get the most power out of the TEPR. Unfortunately, the field of large superconducting coils cannot be increased without limit. The superconductor must be ductile to tolerate the large strains imposed by the high stresses in the coil. The only superconductor that has this amount of ductility is NbTi. At 80 kG, the critical current of NbTi is only about one-half the critical current at 60 kG. Further, at 80 kG the critical current, (i.e., the maximum current a superconductor will carry at a given field) decreases by 33% with a field increase of only 10 kG. For this study, approximately 80 kG was considered as a reasonable field level.

1. Design Considerations of TF-Coil System

The TF coils for the TEPR must be large enough to contain the required plasma and also allow room for an adequate blanket and shield around the plasma. The coils must generate as high a field as practical within the limits of NbTi superconductor. In order to keep the total peak field on the TF coils within 80 kG, the peak field due to the TF coils alone cannot exceed 75 kG. As shown in Fig. IV-1 and IV-2, an additional field is imposed on the TF coils by the ohmic-heating coils, the vertical-field coils, and by the plasma current itself. Although most of the imposed fields are not in the same direction as the toroidal field, they still affect the superconductor somewhat because as the critical field is approached, the field direction ceases to be a factor in determining the current-carrying capacity.

The ANL-TEPR design, as shown in Fig. IV-3a, allows adequate space for the plasma and shield. It would be advantageous if the shield could be made thinner in the inner region to allow the plasma to move in and thus effectively

Figure IV-1. Field Resulting From Plasma Loop Alone

Figure IV-2. Resultant Fields Due to OH Coils, Equilibrium Coils, and Plasma Poloidal Field

Figure IV-3a. Schematic of Individual TF Coil - Vertical Cross-Section

increase the toroidal field and hence the power output of the reactor. However, a shield thinner than the one shown would cause two problems with the superconducting coils: (1) The refrigeration load on the magnet, due to nuclear heating, would increase to an intolerably high level, and (2) the resistance of the stabilizing copper would rise too much with radiation damage. The 1 m inner-blanket and shield thickness provides an ample safety margin. Some reduction in thickness is possible, particularly if a highly attenuating tungsten (or tantalum)/boron-carbide shield is used. This point is analyzed in detail in Section VI-B and in Appendix D.

The 16-coil design was chosen as a reasonable compromise between field ripple and access. It is undesirable to have to disassemble such large superconducting coils in order to gain access to the blanket/shield, first wall and many other large components within the torus. In this design, more than 3 meters has been allowed between adjacent coils to provide for each access to the large inner components as shown in Fig. 1V-3b. It is not clear at this time whether the resulting field ripple of 2% is tolerable or not. If a smaller field ripple is required, the coils can be made somewhat larger and/or the number increased, still maintaining the 3-meter gap. Such an increase in size will increase the cost of the coils approximately in proportion to the increase in the coil bore.

Since the TEPR TF-coil system has a huge amount of stored energy ($\sim 10^{10}$ J), these magnets should be designed so that they will not quench. The cost of these magnets will be a large fraction of the total plant cost, therefore, they must be designed so that their performance is predictable. Hence, the cryostatically-stable (i.e., fully-stabilized) magnet design is the only viable design. The technology for superconducting magnets has come a long way. The experiences⁽¹⁾ of the past decade clearly substantiate this viewpoint. On the other hand, the experiences⁽²⁾ of the past ten years in the worldwide effort to build magnets of higher current density, based on either intrinsic stability or dynamic stability or both, has shown that these magnets, even the small solenoids, are not predictable. Therefore, the TF-coil design has been based on cryostatic stabilization criteria. This is achieved by having enough copper and cooling within the windings to prevent the possibility of a magnet quench or damage. In addition, large superconducting coils must be protected against overheating and voltage breakdown. Analysis, based on

NUMBER OF COILS = 16

PEAK FIELD = 75 KG

$B_{\phi_0} = 33.4 \text{ KG.}$

$J_{\text{AVG.}} = 1280 \text{ AMP./cm}^2$

EACH COIL CROSS SECTION = 0.5110 m^2

EACH COIL AMP.-TURNS = $6.54 \times 10^6 \text{ A.-T.}$

TOTAL AMP.-TURNS = $104.64 \times 10^6 \text{ A.-T.}$

Figure IV-3b. Schematic of TF Coils - Plan Cross-Section

protection and stability reasons, shows that the larger the stored energy, the lower the conductor current density must be in order to assure complete coil protection.

The ideal winding cross section is the trapezoidal-coil cross section, which for a given peak field, allows for the maximum toroidal field to be obtained. However, the trapezoidal coil winding presents some winding complications. For the ANL-TEPR TF coils, a rectangular coil cross section has been selected. In the rectangular cross section, the conductor cross section must be distributed so as to minimize the flux leakage. This leads to a higher toroidal field in the plasma with given peak field. These points are illustrated in Fig. IV-4.

Finally, because of the configuration of the TF-coil system, large nonuniform Lorentz forces are generated in the coils, so that the coil is subjected to large bending moments in addition to a large effective internal pressure. The bending moments produce a large peak stress. The minimum stress is obtained when there are no bending moments so that the coil is in pure tension.

2. Description of Toroidal Field-Coil System

a. Characteristics of the TF Coils

The design of the TF-coil (TFC) system for the TEPR was the result of an iterative process involving the plasma physics and power performance, the blanket and shield design, requirements on the ohmic-heating coils, and access requirements. The power objectives of the TEPR can be satisfied by a circular cross-section plasma with a radius of $a = 2.1$ m. An additional 0.3 m is allowed to reduce the number of fast alpha particles striking the wall, so that the toroidal vacuum chamber has a radius $r_w = 2.4$ m (See Section III-A and Appendix B). A blanket-plus-shield thickness of 1.0 m on the inside provides adequate protection for the superconducting coils, and a 1.3 m thickness on the outside allows added margin and accommodates breeding blankets in Stage II. (See Section VI-B and Appendix D.) Access requirements for the neutral-beam injectors and for getting maintenance equipment between the TF coil and the blanket on the outside, together with the space requirements for the toroidal vacuum vessel and the blanket and shield, led to a choice of $R_{\text{bore}} = 7.7$ m for the minor bore of the TF coil. The radial location, R_{10} in Fig. IV-3, was determined by the requirements that the central coil radius

Figure IV-4. Field Distribution in Plasma Column for Rectangular and Trapezoidal Coil Cross-Sections

be sufficiently large so that the ohmic-heating (OH) coils could provide the necessary magnetic flux to induce and drive the plasma current without requiring excessive fields in the OH coils (See Section IV-C), and that the plasma volume be sufficiently large so that the power objectives of the TEPR are satisfied. An additional requirement upon the design was that approximately 3.0 m access, at the horizontal mid-plane, be available between adjacent TF coils. This access requirement can be satisfied with 16 TF coils with $R_{\text{bore}} = 7.7$ m, and with a maximum field ripple at the outer part of the plasma of approximately 2%. With these requirements, R_{bore} , R_{10} , 16 coil set, a pure-tension shape was determined by the procedure described in the next subsection.

Table IV-1 lists the specifications and characteristics of the ANL-TEPR TF coils, and Figs. IV-3a and IV-3b illustrate the TFC design. The design analysis is discussed in subsequent subsections.

b. Pure-Tension TF-Coil Determination

A theory and a computer code for designing practical pure-tension TF-coil systems have been developed. The description of the analytical methods used to obtain the Argonne pure-tension coil shape is given in Appendix E.

For a continuous toroidal magnet, the product $B_t r$ is constant within the coil winding. Therefore, the transverse force, F_t , is given by

$$F_t = B_t I = \alpha r^{-1} \quad (\text{IV-1})$$

where r is the toroidal radius, B_t is toroidal field in the winding, α is a constant and I is the conductor current. When the coil is in pure tension, the natural conductor path must be such that:

$$\rho = \frac{\text{Tension}}{F_t} = Kr = \pm \left[1 + \left(\frac{dz}{dr} \right)^2 \right]^{3/2} / \frac{d^2 z}{dr^2} \quad (\text{IV-2})$$

where ρ is the radius of curvature, K is a constant, and z is the vertical coordinate. The coil shape, prescribed by Eq. (IV-2), is called the Princeton "simple-D" coil.⁽³⁾

In reality, toroidal-coil systems are not continuously wound, but are

Table IV-1. ANL-TEPR Toroidal Field-Coil System

Number of coils	16
Max. gap size	3.4 m
Field ripple	2%
Peak field	75 kG plus superimposed fields
Superconductor	NbTi plus copper stabilizer
R_{10}	2.566 m
R_{20}	10.866 m
R_{00}	5.47 m
B_t at plasma center	33.4 kG
Z_{max}	6.227 m
Radial thickness of coil plus bobbin	0.568 m
Axial length of coil plus bobbin	0.90 m
Ampere turns per coil	6.54×10^6
Total ampere turns	104.64×10^6
Cross section of coil plus bobbin	0.511 m^2
Average current density over coil plus bobbin	1280 A/cm^2
Winding cross section	0.44 m^2
Average current density over coil winding	1486 A/cm^2
Average current density over conductor	2378 A/cm^2
Straight section length	7.28 m
Stored energy	15600 MJ
Operational current	10,000 A
Total inductance	312 H
Inductance per coil	19.5 H
Turns per coil	654
Mean turn length	34.4 m
Total conductor length per coil	22,500 m
Approximate weight per coil	175 tons
Total coil weight	2,800 tons

bumpy, with access gaps between two adjacent coils. Flux leakage results in a B_t of

$$B_t \approx (\alpha_1 + \alpha_2 r + \alpha_3 r^2)^{-1} \quad (\text{IV-3})$$

where α_1 , α_2 , and α_3 are constant coefficients.

The transverse force per unit length must be obtained by integration over the coil volume. This transverse force depends on the coil shape, the coil cross section, the number of coils, and the gap size between adjacent coils. For a given pure-tension coil system, the radius of the curvature ρ can be written as

$$\rho = (\beta_1 + \beta_2 r + \beta_3 r^2) = \pm \left[1 + \left(\frac{dz}{dr} \right)^2 \right]^{3/2} / \frac{d^2 z}{dr^2} \quad (\text{IV-4})$$

The constant coefficients β_1 , β_2 , and β_3 are obtained through a least-squares fit. Starting with the Princeton "simple-D" coil shape, a set of β 's is obtained. Equation (IV-4) is then solved. A new coil shape is obtained, and a new set of β coefficients is determined. Equation (IV-4) is then solved. A new coil shape is obtained and a new set of β 's is determined. Equation (IV-4) is solved again, and so on. This iterative process continues until a set of β coefficients generates a nearly identical set of β 's. Figure IV-5 shows the evolution of the Argonne pure-tension coil shape. Note that the iterative process rapidly converged in 4 or 5 iterations. This calculation is performed by the computer code, MARIA. (4)

c. Stress Analysis

The computer code MARIA, after generation of the pure-tension coil shape, divides the coil structure into many finite curvilinear elements. The computer code SOLID SAP⁽⁵⁾ was then used to perform a 3-dimensional stress analysis of the coil structure. A fairly uniform tension, with an average stress of about 16,000 psi, is obtained in the TF coil, as shown in Fig. IV-6. Stresses that would exist in a circular TF-coil system and in a 16-coil, Princeton "simple-D" TF-coil system are also shown in Fig. IV-6. Note the sharp reduction of peak stress in the Argonne pure-tension coil. It is to be noted also that the position of the ends of the support cylinder, as well as the exact shape of the TF coils, is important in reducing the peak stress. The supporting system used in the stress analysis is shown in Fig. IV-7. The stress data indicate that the average stress is 16,000 psi. A reasonable stress limit

Figure IV-5. Evolution of Pure Tension Coil

Figure IV-7. Schematic of Coil Support System

for the copper is about 12,000 psi. To maintain this stress level in copper with an average stress of 16,000 psi, two parts of copper to one part of stainless steel are adequate, since the tensile modulus of stainless steel is about twice that of copper. The resulting stress of 24,000 psi on the stainless steel is well within its working limits at liquid-helium temperature.

d. Nuclear Heating and Induced Resistivity of Stabilizer

In the TEPR blanket and shield design (see Chapter VI), it is found that, for a neutron wall loading of 0.2 MW/m^2 , the maximum nuclear heating in the magnet conductor is $5 \times 10^{-6} \text{ W/cm}^3$. This generates a temperature rise of only a few millidegrees Kelvin. The nuclear-heating effect decreases by an order of magnitude in a coil radial thickness of 56.8 cm. The total nuclear-energy deposition in the coil volume per cm of coil width is 0.14 W. Assume the coil width is 114 cm, the total nuclear-energy disposition is 16 W per magnet and 256 W for all 16 magnets. Coil cooling will be by a liquid-helium bath and natural convection. The 16 W additional heating will pose no problem for this type of cooling.

Radiation produces atomic displacements which increase the electrical resistivity of copper. This is a more serious problem than nuclear heating. The maximum induced resistivity in the copper is $2.5 \times 10^{-8} \Omega\text{-cm}$ for an integral wall loading of 10 MW-yr/m^2 . This requires an additional 25% of copper for full stabilization, which poses no serious difficulty in a practical conductor design. The induced resistivity decreases to $5 \times 10^{-9} \Omega\text{-cm}$ at a radial depth of 56.8 cm in the magnet. We do not expect a TEPR to operate continuously for 10 MW-yr/m^2 . Allowing a factor of 10 for safety and uncertainty, 10 MW-yr/m^2 corresponds to 10 years of operation at 0.2 MW/m^2 and with a 50% duty factor. Furthermore, the radiation-induced resistivity in copper can be recovered by annealing at approximately 300°K .

The radiation damage to the Mylar insulation seems more serious than that to copper. The analysis concluded that Mylar can sustain damage up to an integral wall loading of 3 MW-yr/m^2 . The use of epoxy will prolong the insulation life time up to 20 times longer. However, 3 MW-yr/m^2 corresponds to 30 years operation at a 0.2 MW/m^2 neutron wall loading and a 50% duty factor, which is adequate for the TEPR. Thus, Mylar can be used as insulation between layers.

e. Conductor Design and Coil Structure

Since the toroidal field decreases rapidly across the coil winding, the conductor will be made in 3 grades, with a field range of 76 kG - 60 kG, 60 kG - 45 kG and 45 kG - 0 kG, respectively. Therefore, the inner turns will have the largest amount of copper and NbTi per turn. Grading the conductors in this manner will result in a substantial savings. The conductor specifications are listed in Table IV-2 and illustrated in Fig. IV-8.

As shown in Fig. IV-9, the basic coil unit will be a pancake module. The bobbin is a stainless steel casting of 2.54 cm thickness. The vertical sagging due to a coil weight of 175 tons is estimated to be of order of 5 cm. The bobbin will be used to wind each pancake directly. There are 21 pancakes per coil. Between two pancake coils, a spacer of 0.4 cm in thickness is provided. Each pancake has 70% of both edges cooled. The edge cooling alone is not enough to fulfill the cryostatic stabilization criteria, so 50% of one-face cooling is provided. The aspect ratio of the conductor is chosen to be a compromise between winding cost and cooling efficiency. As both the edge-cooling channel and the face-cooling channel are large liquid channels, a peak heat-transfer flux of 0.5 W/cm^2 is chosen in the stability calculation.

The coil volume is roughly divided into one part copper, 0.5 part stainless steel and 0.1 part coolant passage. At an average stress of 16,000 psi, the resulting stress of copper is 12,000 psi and that of stainless steel 24,000 psi. The average current density over the winding is 1486 A/cm^2 . The current density over the conductor has an overall average value of 2378 A/cm^2 .

The stainless steel support material will be distributed uniformly throughout the coil: i.e., a strip 0.5 cm x 3.6 cm will be wound along with the conductor. This method of reinforcing provides the best stress distribution throughout the winding and does not allow large bearing forces to accumulate on the copper. Distributing the stainless steel within the winding also improves the local heat capacity of a small volume allowing more time before a normal region becomes dangerously hot.

The big advantage of a liquid-helium bath is its large heat capacity. This enables the coil to handle large transient heat loads without quenching. The current carrying capacity of NbTi is based on 5.2°K and peak fields of 76 kG, 60 kG, or 45 kG, according to the grade of conductor. This means that the magnet will operate with a temperature disturbance of 1°K without going to current sharing.

Table IV-2. Conductor Specifications of TF Coils

	<u>Grade 1</u>	<u>Grade 2</u>	<u>Grade 3</u>
Peak field	76 kG	60 kG	45 kG
Magnetoresistivity	$4.4 \times 10^{-8} \Omega\text{-cm}$	$3.73 \times 10^{-8} \Omega\text{-cm}$	$2.05 \times 10^{-8} \Omega\text{-cm}$
Radiation Induced resistivity	$2.5 \times 10^{-8} \Omega\text{-cm}$	$2.1 \times 10^{-8} \Omega\text{-cm}$	$1.7 \times 10^{-8} \Omega\text{-cm}$
Total resistivity	$6.9 \times 10^{-8} \Omega\text{-cm}$	$5.83 \times 10^{-8} \Omega\text{-cm}$	$3.75 \times 10^{-8} \Omega\text{-cm}$
Turns/coil	5	6	20
Radial thickness	10 cm	10.6 cm	31.12 cm
Conductor cross section	4.4 cm^2	4.0 cm^2	3.17 cm^2
Superconductor current density at 5.2°K	$3 \times 10^4 \text{ A/cm}^2$	$9 \times 10^4 \text{ A/cm}^2$	$15 \times 10^4 \text{ A/cm}^2$
Superconductor cross section	0.334 cm^2	0.112 cm^2	0.067 cm^2
Copper cross section	4.066 cm^2	3.888 cm^2	3.103 cm^2
Copper current density	2459 A/cm^2	2572 A/cm^2	3223 A/cm^2
Conductor thickness	1.350 cm	1.217 cm	1.0 cm
Conductor width	3.6 cm	3.6 cm	3.6 cm
Thickness of stainless steel	0.6 cm	0.5 cm	0.5 cm
Thickness of mylar insulation	0.05 cm	0.05 cm	0.05 cm
Wetted conductor perimeter	$3.69 \text{ cm}^2/\text{cm}$	$3.5 \text{ cm}^2/\text{cm}$	$3.2 \text{ cm}^2/\text{cm}$
Required wetted perimeter	$3.4 \text{ cm}^2/\text{cm}$	$3.0 \text{ cm}^2/\text{cm}$	$2.4 \text{ cm}^2/\text{cm}$

Figure IV-8. Coil Composition for the Various Conductor Grades

Figure IV-9. TF Coil Cross-Section

3. Coil Protection

With a total stored energy of 15,600 MJ in 16 coils, the energy per coil is 975 MJ. At a design current of 10,000 A, the inductance will be 19.5 H. By bringing a pair of leads out for each coil and allowing an energy-dump voltage of 1,000 volts per coil, the time constant of the TF system will be 195 sec or 3.25 min. The coils will be connected in series with a dump resistor between each coil as shown in Fig. IV-10. This method of connecting allows the energy to be removed rapidly enough to prevent damage to the coils under any malfunction condition and without accumulating a large voltage with respect to ground. It is important to keep equal currents in the coils at all times in order to prevent large induced-magnetic forces, which would damage the coils.

4. AC-Pulsing Loss by Superimposing Fields

The equilibrium coil, the plasma current and the ohmic-heating coil superimpose ac fields on the TF-coil system, as shown in Fig. IV-2. These ac fields, averaged over the TF coil, have an average magnitude of approximately 3 kG. The ac loss mainly consists of the filament and matrix losses of the TF coil conductor and the eddy-current loss of the TF-coil structure. Only the filament loss and the matrix loss are estimated here. The eddy-current loss in the TF-coil structure and the proposed methods for shielding are discussed in Appendix E. In addition to the ac loss, the superimposed fields also exert bending and twisting torque on the TF coils. These are also discussed in Appendix E.

Referring to Section IV B-4, the filament loss per m^3 of NbTi over a field cycle $0 \rightarrow B \rightarrow 0$ is given by

$$Q_f = \frac{4}{3\pi} J_o B_o d_f \ln \frac{B+B_o}{B_o} J/m^3$$

where J_o is the critical-current density at zero field, B_o is defined by the Kim-Anderson formula $J_c = J_o B_o / (B + B_o)$, and d_f is the filament diameter. The averaged cross section of NbTi used per turn (see Table IV-2) is about 0.119 cm^2 . The average length per turn is about 34.4 m, so the total amount of NbTi per coil is 0.27 m^3 . For a field swing $0 \rightarrow 3 \text{ kG} \rightarrow 0$, we have the filament loss per coil given by

$$\begin{aligned} E_f &= d_f (0.27) \left(\frac{4}{3\pi} \right) (9 \times 10^9) \ln 1.3 \\ &= 27 \times 10^7 d_f J \end{aligned}$$

For 10μ filament size, we have $E_f = 2700$ J. Averaging over a complete fusion cycle, we have a dissipation power of 77 watts/coil at 4.2°K . For 16 coils, the total dissipation at 4.2°K is 1232 watts. If the filament diameter is 100μ , however, the loss will be 770 watts/coil and a total of 12.32 kW at 4.2°K , or 3.7 MW at 300°K .

To estimate the matrix loss per coil, it is assumed that the filament is evenly distributed in the matrix and that the conductor is approximated by a cylindrical composite of equal cross section as proposed in Table IV-2. The matrix loss per m^3 composite for a field cycle consisting of a linear rise and then followed by a linear fall is given by

$$Q_m = \left(\frac{4}{3\pi}\right) \frac{B \dot{B} \ell^2}{\rho_m} \quad \text{J/m}^3$$

where 4ℓ is the twisting pitch of filaments and ρ_m is the matrix resistivity across the conductor. The total composite volume per coil is about 10 m^3 . If the matrix is copper, including the magneto-resistance and the induced-electrical resistivity by radiation, the average value of ρ_m is about $5 \times 10^{-8} \Omega\text{-cm}$. Assume a short twisting pitch $\ell_p = 4\ell = 5 \text{ cm}$. Without the field shield, $\dot{B} = 3 \text{ kG/sec}$ and the matrix loss per coil over a complete fusion cycle, given by

$$\begin{aligned} E_m &= (10) \left(\frac{4}{3\pi}\right) \frac{(0.3)^2}{5 \cdot 10^{-10}} (1.25 \times 10^{-2})^2 \\ &= 119,400 \text{ J} \end{aligned}$$

This gives an average power dissipation of 3.411 kW at 4.2°K , which means nearly 5000 liters of liquid-helium evaporation per hour per coil. Since the conductor length per TF coil is about 22,500 m, the heat generation per cm of conductor only amounts of 1.5 milliwatt. The associated temperature rise is negligible, but the consumption of liquid helium for the entire 16-coil system will be 80,000 liters per hour.

If a field shield is used to slow down the flux diffusion by a factor of 10, the matrix loss will be 11,940 J/coil; the dissipation power will be 341 W, and the helium consumption will be 500 liters per coil. So, the consumption of liquid helium would be reduced to a more manageable level.

Figure IV-10. TF Coil Quench Protection System

5. Support System

The straight sections of the TF-coil conductor experience an inward centering force by Lorentz's law. Using an average field of 37.5 kG, a straight conductor length of 7.28 m, and a total ampere-turns of 104.64×10^6 , the total centering force is given by

$$\begin{aligned} F &= NI \cdot l \cdot B \\ &= (104.64 \times 10^6) \times 3.75 \times 7.28 \\ &= 2857 \times 10^6 \text{ newtons} \\ &= 642 \times 10^6 \text{ pounds.} \end{aligned}$$

The compressive pressure, in units of psi, is given by

$$P = \frac{642 \times 10^6}{2 (2.282)(7.28)(1550)} = 3970 \text{ psi}$$

where the outer radius is 2.282 m and $1 \text{ m}^2 = 1550 \text{ in}^2$. This compressive stress presents no problem for the conductor insulation.

As shown in Fig. IV-3a, the inner radius of the support cylinder is 2 m and the outer radius is 2.282 m, so the maximum circumferential stress is given by

$$S_{\max} = -P \frac{2b^2}{b^2 - a^2}$$

where P is the external radial pressure, b is the outer radius and a is the inner radius. For $P = 3970$ psi, we have

$$S_{\max} = -(3970) \frac{2 (2.282)^2}{(2.282)^2 - (2)^2} = -34,242 \text{ psi}$$

To examine the elastic stability of the support cylinder, the cylinder is identified as a short tube.⁽⁶⁾ For a short tube, the critical load at which elastic buckling occurs is given by⁽⁷⁾

$$P' \approx 0.807 \frac{Et^2}{LR} \sqrt{4 \left(\frac{1}{1-\nu} \right)^3 \left(\frac{t}{R} \right)^2}$$

where t is the cylinder thickness, R is the inner radius of the cylinder, E is the Young's modulus, ν is the Poisson ratio, and L is the cylinder

length. If the cylinder is stainless steel, $E = 30 \times 10^6$ psi and $\nu = 0.3$, we have

$$P' = (0.807) \left(\frac{3 \times 10^7}{728 \times 200} \right) (28.2)^2 \sqrt{\left(\frac{1}{0.7} \right)^3 \left(\frac{28.2}{200} \right)^2} = 64,800 \text{ psi}$$

Thus, buckling would not occur at a pressure of 3970 psi. A metallic cylinder will be segmented to prevent induced currents from the ohmic-heating coils.

If the cylinder is made of glass-filled epoxy, E will range between 2 and 6×10^6 psi. The buckling load will be 4325 psi if $E = 2 \times 10^6$ psi is used. Therefore, G-10 fiberglass is a possible candidate for the support cylinder; however, the low modulus means large strain and so it is inferior to stainless steel for this purpose.

Each TF coil will be connected to its neighbors on either side by several cold supports on the outer portion of the coil. As shown in Appendix E, the equilibrium field interacts with the TF coils, exerting a twisting torque on the latter. A cross-bracing torque frame will be tied to the coils to resist the twisting forces.

6. Cryostat Design

The general cryostat arrangement is illustrated in Fig. IV-11. The individual TF coils will be supported by the support cylinder on the inside, and by individual supports on the outside. The support cylinder rests on a cylindrical compression member of low thermal-conductivity material. This support transmits the load to the outer wall of the vacuum jacket, which rests on the foundation.

The outer supports for the TF coils will be of the same material, again loaded primarily in compression and transmitting the load thru the vacuum-jacket wall. The outer supports will be fixed to the TF coils through a hinged or sliding joint to allow for thermal contraction.

Each coil will have a separate helium container of stainless steel to minimize the helium volume. The container will conform closely to the coil geometry. The resulting flat-sided pressure vessel will require relatively thick walls, on the order of 2.5 cm to keep stresses and deflections at an acceptable level.

Multi-layer insulation and a nitrogen-cooled radiation shield will be used for insulation. The central part of the assembly, consisting of the support tube, the inner part of the TF coils and the ohmic-heating coil will

Figure IV-11. General Cryostat Arrangement

have a common vacuum jacket. There will be a transition to individual vacuum jackets for the outer portions of the TF coils to provide the required access between coils. The outer portions of the vacuum jackets will be parallel to the helium containers, resulting in a flat-sided configuration to give maximum access room.

For the configuration shown, the total helium-temperature surface exposed to the radiation shields is 2400 m^2 . This area includes the TF coils, the ohmic-heating coils and the equilibrium coils. A conservative figure for the heat flux between a nitrogen-cooled radiation shield and a helium-temperature surface with aluminized-mylar insulation is $6.5 \times 10^{-2} \text{ W/m}^2$. This gives a total heat flux of 156 watts.

The main support system must support 16 coils weighing 175 tons each, and the support cylinder, weighing 80 tons, for a total of $5.76 \times 10^6 \text{ lbs}$. If the low-temperature supports are of epoxy-fiberglass laminate with an allowable stress of 30,000 psi, then the total area required is:

$$A = \frac{P}{S} = \frac{5.76 \times 10^6}{3 \times 10^4}$$

$$= 192 \text{ in}^2, \text{ or } 1238 \text{ cm}^2.$$

If we arbitrarily select a length of 25 cm as the heat-leak path, the support heat leak is:

$$Q = \frac{A}{L} \int k dt$$

$$\int_4^{300} k dt = 0.9 \text{ w/cm}$$

$$Q = \left(\frac{1238}{25}\right) (0.9) = 44 \text{ watts}$$

where A is the support cross section, L is the support length and k is the thermal conductivity. The total heat flux to the cryostat assembly is then $156 + 44 = 200 \text{ watts}$.

The foregoing is a simplified analysis of the heat flux to the cryostat but clearly indicates that a total cryostat heat leak of a few hundred watts is readily attainable.

7. Summation of Dissipation Loss and the Refrigeration Requirements

a. Nuclear Heating

The nuclear heating is 16 W per coil or a total of 256 W for 16 coils.

b. AC Loss by Superimposed Fields

The filament loss for 10 μ filaments is 77 W per coil and 1232 W for the 16 coils. The matrix loss per coil under a shield with flux-diffusion rate 0.3 kG/sec, will be 341 W per coil and 5456 W total for the 16 coils. Excluding the eddy-current loss of the coil structure, we have 6688 W for the ac loss of the TF-coil conductors.

c. Cryostat Loss

The radiation to the helium vessel from the nitrogen-temperature shield is 156 W. The heat-conduction loss through the support system is 44 W. Therefore, the cryostat loss is about 200 W.

Based on the above value, the ac loss due to external fields is the most serious problem to be solved in the future design study. The grand sum of the dissipation will be at least 7144 W in this preliminary design calculation. This means the helium consumption will be at least 10,000 liters per hour for the TF-coil system. The required refrigeration at 4.2° K will be at least 10 kW.

The helium reservoir and the transfer-line losses have not been estimated. The loss from the 10,000 A leads will be negligible because it is clear that there is more than 20 W of cold helium gas available for each TF coil.

B. Equilibrium-Field Coil System

1. Equilibrium-Field Requirements

The toroidal plasma-current ring, like an electric current flowing in a conductor loop, exerts an outward hoop force on itself. The hoop force tends to expand the plasma ring. That is, the poloidal magnetic pressure, $B_p^2/2\mu_0$, being larger on the inside, tends to increase the major radius of the plasma. Therefore, a weak field pointed in the vertical direction (called z direction) is needed so that the interaction of the external vertical field and the toroidal-plasma current provides a $\vec{J} \times \vec{B}_v$ force inward toward the toroidal axis. The required vertical field $\vec{B}_v$ must have such a magnitude and curvature that the Tokamak equilibrium condition $\vec{J} \times \vec{B}_v = \nabla p$ is satisfied. According to

the analysis⁽⁸⁾ of Greene, Johnson, and Weimer, the required equilibrium holding field is given by

$$B_{eq} = \frac{I_p}{R} \left(\ln 8 \frac{R}{a} - 1.25 + \beta_0 \right) \times 10^{-6} \text{ (kG)} \quad (\text{IV-5})$$

where I_p is the plasma current in amperes, R is the toroidal radius in meters, a is the plasma radius in meters and β_0 is the ratio of poloidal plasma-to-magnetic pressure. Note that B_{eq} is the neutral-equilibrium field while B_v is the stable-equilibrium field.

For stability against radial displacement, the same analysis⁽⁸⁾ concluded that the vertical-field component, B_{vz} , must be smaller than the holding field B_{eq} for $R < R_0$, and that B_{vz} must be greater than B_{eq} for $R > R_0$, where R_0 is the major radius of the plasma. That is, the condition

$$\left| \frac{\partial B_{vz}}{\partial R} \right| < \left| \frac{\partial B_{eq}}{\partial R} \right| \quad (\text{IV-6})$$

is needed for stability against radial displacement.

For stability against vertical displacement, Yoshikawa⁽⁹⁾ has shown that the required condition is that the vertical-field lines must be concave toward the toroidal axis. That is the condition

$$\frac{\partial B_{vR}}{\partial R} / B_{vz} < 0 \quad (\text{IV-7})$$

must be satisfied so that the resulting force tends to center the plasma on the axis $z = 0$. (B_{vR} is the radial component of the vertical field.)

In the ANL-TEPR design, we have $I_p = 4.5 \times 10^6$ A, $R_0 = 6.25$ meters, $a = 2.1$ meters, and $\beta_0 = 2.2$. Therefore, the required vertical field at the center of the plasma ring is 2,967 Gauss. Equation (IV-6) says that $\partial B_{vz} / \partial R$ must be less than 3.6 Gauss/cm for radial equilibrium (note that $\partial B_{vz} / \partial R$ must be negative). Equation (IV-7) simply states that B_{vR} is negative in the $+z$ plane and positive in the $-z$ plane, so that the field lines are concave toward the toroidal axis.

* For a circular plasma cross section and $B_{TFC} = 70$ kG (see Table III-3). The results would scale according to Eq. (IV-5) for other design points.

2. Design Considerations of Equilibrium-Field Coils

Ideally, one wishes to place the equilibrium-field coil outside the first wall but inside the blanket, so that it requires minimum ampere-turns, has minimum flux-diffusion time, and has minimum pulsing-field interaction with the TF and OH coils. However, because of the high-temperature environment and the high neutron flux, it is difficult to design even a conventional copper coil here. Nevertheless, the location is so attractive that, in the future, it would be worthwhile to study the design of a possible liquid-metal, equilibrium-field coil, or to design a normal-metal coil with resistivity less sensitive to neutron flux.

The second alternative is to place the equilibrium-field coil outside the blanket and the shield but inside the toroidal D coils. The time needed for the equilibrium field to diffuse through the metallic blanket and shield of a thickness of one meter may be so long that fast pulsing is not possible. The coils placed here could be copper coils or superconducting coils, if the temperature at the outer surface of the shield is cooled to nearly room temperature. Placement of the equilibrium-field coils between the shield and TF coil has the following disadvantages: (1) The space between the D coil and the shield is a narrow corridor. The flexibility for arranging the coils is limited. Furthermore, the required access for neutral-beam injection does not allow the placement of coils closer to the outer edge of the corridor. As a result, B_{vz} falls off too rapidly near the outer edge of the plasma column. (2) The equilibrium-field coil bundles have large radii and small axial thicknesses, and thus inherent high-peak fields. The proximity of these coils to the inside surface of TF coils results in a large additional pulsed field being superimposed on the TF-coil system, which severely limits the peak field of the TF coils and generates a large eddy-current heating loss. (3) There are large interaction forces from the TF coil, the OH coil, and the plasma column, and among the equilibrium coils themselves. It is a very difficult task to design a sound support structure for these coils if they are located inside the TF coils. (4) Assembly and disassembly of the equilibrium coils is not easy. (5) If the coils are superconducting, they are subjected to high toroidal fields, in addition to self fields.

The third alternative is to place these coils outside the TF-coil system. In comparison with the second alternative, all the disadvantages of the second

alternative are ameliorated in the third alternative. The disadvantages are slightly larger ampere-turns required and possible uneven and unsymmetric flux diffusion because of the gaps between the toroidal coils. The third alternative was adopted for this design study.

The ohmic-heating coils induce large voltages in the equilibrium coils. With a reasonable power supply, the equilibrium coils must be arranged so that they are decoupled from the OH coils. The decoupling ampere-turns should be positioned so that they produce small antivertical fields and produce small superimposed fields on the TF-coil system.

The final consideration was that the equilibrium coils were designed to produce additional volt-sec for the plasma ring, thus reducing the volt-sec requirement for the OH-coil system.

3. Equilibrium Field-Coil Design

Based on the above considerations and the equilibrium field requirements, the equilibrium-coil configuration was designed as shown in Fig. IV-12. The detailed dimensions and magnet characteristics are listed in Table IV-3.

In Table IV-3, R_1 , R_2 , Z_1 , and Z_2 represent the inner radius, the outer radius, the initial axial coordinates and the final axial coordinates, respectively. The total conductor length is 727×10^6 ampere-meters. The total ampere-turns is 10×10^6 , with half of these ampere-turns serving to magnetically decouple the equilibrium coils from the OH coils. These anti-mutual inductance coils are coils No. 1, No. 2, and No. 3. They are positioned to generate negligible vertical fields in the negative Z direction everywhere on the $Z = 0$ axis, thus enabling coil No. 4 and coil No. 5 to produce a useful vertical field. Furthermore, this arrangement increases the volt-sec generated by the equilibrium coils. Finally, these coils produce only small amounts of superimposed field on the TF-coil system. The superpositioned fields, generated by the equilibrium coils, are indicated in Fig. IV-13. All field calculations were done accurately, using full coil sections rather than filament approximations.

A moderate average current density of 2300 A/cm^2 was chosen for the design calculations. The operational current of 15,000 amperes, resulting in a self inductance of 0.8 H, was chosen with fast pulsing in mind. To drive the equilibrium coils to the operational field strength in one second, the power-supply voltage required is 37.7 kV, neglecting the IR drop if the coils are made of normal conductors. The total stored energy is 90 MJ.

Table IV-3. Equilibrium-Field Coil System

Coil No.	R_1 (cm)	R_2 (cm)	Z_1 (cm)	Z_2 (cm)	Central Field (Gauss)	Conductor Peak Field (kG)	Coil-Pair Conductor Length (Amp - m)
1	200	230	± 630	± 650	- 261	37	37×10^6
2	230	250	± 630	± 680	- 492	32.5	69×10^6
3	250	270	± 680	± 710	- 287	18	45×10^6
4	720	745	± 640	± 695	2192	32	291×10^6
5	1180	1210	± 362.5	± 390	1732	25	285×10^6

Table IV-4. Characteristics of Copper Equilibrium-Coil Design

	<u>Coil Number</u>				
	5	4	3	2	1
No. of layer/coil	13	22	10	15	10
Turns/layer/coil	10	10	9	10	10
Conductor outside dimension (cm)	6.063	5.075	3.617	4.031	3.498
Conductor inside dimension (cm)	4.483	4.079	2.456	2.126	2.413
dc voltage (V)	2600	4494	1000	1000	1000
Power (MW)	24	41	9	9	9
Q (gallon/min)	2022	3500	778	778	778
Copper Weight (tons)	400	254	25	57	21

Figure IV-12. Equilibrium Coil Configuration

Figure IV-13. Field Due to Equilibrium Coil Alone

The vertical-field pattern generated by the equilibrium coils is plotted in Fig. IV-14. Also shown in this figure is B_{eq} defined by Eq. (IV-5). Note that the achieved B_{vz} also satisfies Eq. (IV-6) with $\partial B_{vz} / \partial R$ negative in the plasma region. Therefore, the stability against radial displacement is assured. The concave vertical-field lines are plotted in Fig. IV-15. Clearly, the condition for stability against vertical displacement is met.

The volt-sec contributed by the equilibrium coils was graphically determined from the data in Fig. IV-14. The equilibrium coils contribute 37 volt-sec, thus reducing the volt-sec requirements for OH coils by 33.6%.

Both superconducting and copper equilibrium-coil designs were developed in order that the problems and relative advantages of both options could be examined within the context of the TEPR-design requirements.

4. Superconducting Equilibrium-Coil Design

a. AC Pulsing Losses of Superconductors

Type II superconductors exhibit losses under the conditions of changing transport current or changing external-magnetic fields. Loss analyses show that superconductors must be in a fine-filamentary form in order to reduce hysteresis losses, that the filaments must be twisted with a short pitch length in order to reduce the shielding eddy-current losses across the matrix material, and that there is self-field loss because the filaments are not transposed. The self-field loss is small unless the composite diameter is larger than ~ 1 mm and the number of filaments is greater than $\sim 10^4$.

Consider a cylindrical composite of unit volume with a λ fraction of superconductor. The hysteresis losses per cubic meter of composite, Q_f , over a cycle $0 \rightarrow B \rightarrow 0$, is given by⁽¹⁰⁾

$$Q_f = \lambda \frac{4}{3\pi} J_0 B_0 d_f \ln \left(\frac{B+B_0}{B_0} \right) J/m^3 \quad (IV-8)$$

where J_0 is the critical-current density at zero field, B_0 is defined by Kim-Anderson formula^(11,12) $J_c = J_0 B_0 / (B+B_0)$ and d_f is the filament diameter in meters. For NbTi, J_0 is equal to $\sim 9 \times 10^5$ A/cm² and B_0 is roughly equal to 10 kG. Note that the hysteresis loss is the filament loss and is independent of $\dot{B}$.

Figure IV-14. The Neutral Equilibrium Field B_{EQ} and the Stable Vertical Field B_{VZ}

Figure IV-15. Equilibrium Field Pattern

The matrix eddy-current losses per cubic meter of composite, Q_m , for a cycle consisting of a linear field rise, followed by a linear field fall, is given by⁽¹⁰⁾

$$Q_m = \frac{4}{3\pi} \frac{B}{\rho_m} \dot{B} \ell^2 J/m^3 \quad (IV-9)$$

where 4ℓ is equal to twisting pitch length of filaments, and ρ_m is the transverse matrix resistivity. For a fast pulsing rate, matrix loss is significant. A small twist pitch and a large matrix resistivity will help reduce the matrix losses.

For a practical composite, it is not possible to transpose the filaments fully. Therefore, there is a self-flux enclosed between the outer and inner filaments. This self-flux couples the inner and the outer filaments together and thus increases the filament hysteresis losses. The self-field hysteresis losses per unit filament volume Q_s for a current increase from 0 to I is given by⁽¹⁰⁾

$$Q_s = \eta \mu_0 I^2 / 2\pi^2 a^2 J/m^3 \quad (IV-10)$$

where a is the composite diameter in meters and η is a constant depending upon the sample current history. For unidirectional pulsing, $\eta \approx 0.05$. For alternate positive and negative pulsing, $\eta \approx 0.29$.

There are two other dissipation mechanisms that are generally very small and are usually ignored. The first arises through the generation of eddy currents in the matrix alone. The second is called the dc composite dissipation. Because cross sections of individual filaments are not equal, there is a tendency for currents to commute between one filament and another in a high-field region in order to minimize flux-flow resistivity.

b. Design of 15,000-Ampere AC Superconducting Cables

The equilibrium-field coil operational current is 15,000 A, with a charging time of 1 second and a field excursion of 0 $\rightarrow$ 37 kG. The fast pulsing suggests that the filaments must be twisted in a short pitch. The requirements for fine filaments suggest that the conductor cannot be made of one strand. Cables of a few strands often result in a poor packing factor, poor mechanical rigidity, and possible shorts between strands. Hence, a

composite with a large number of filaments was chosen. Such a composite will have a large diameter. Consequently, a 3-component composite with high-resistive barriers of cupro-nickel between filaments is preferred. On the other hand, the OH-coil system and equilibrium-coil system have a large stored energy. Full stabilization is necessary. This means that the composite diameter cannot be too large in order to ensure a large surface-to-volume ratio for cooling.

After careful design calculations to fulfill the cryogenic-stability criterion, a specific superconducting composite, suitable for equilibrium-coil cables was developed as follows:

NbTi critical-current density	$1.9 \times 10^5 \text{ A/cm}^2$ at 4.2°K, 37 kG
Composite operational current	61.7 amp
Composite critical current	123.4 amp
Composite diameter	0.6 mm
Composite NbTi packing factor	0.23
Composite composition NbTi:Cu:Cupro-nickel	0.23 : 0.57 : 0.20
Filament diameter	5 μm
Twist Pitch	6 mm
No. of filaments in composite	3307
Composite operational current density	$2.185 \times 10^4 \text{ A/cm}^2$
Composite critical current density	$4.37 \times 10^4 \text{ A/cm}^2$
Composite matrix effective resistivity	$10^{-6} \Omega\text{-cm}$

This composite can be fabricated with today's existing technology at a price of \$75/pound.

(1) Cables

The complete conductor will have 243 composites fully transposed in order to have good current sharing. Two basic cable arrangements can be used, as shown in Figures IV-16a and IV-16b.

Group-of-three transposed cable (Fig. IV-16a) - If n is the number of groupings, then the number of strands in the 15,000 A cable is 3^n . For 243 strands, n is equal to 5. The cable packing factor is 0.6462^n so for $n = 5$, the cable packing factor is 11.27%. The diameter of the cable is given by $(2.1557n) \times (\text{Strand diameter})$. Thus the 15,000 A cable diameter is 2.793 cm. The average current density of the cable is equal to 2462 A/cm^2 . The cable could be compressed slightly so that it is roughly square in cross section,

a. Group-of-Three Cable (15,000 Amp.)

b. Calendered-Group Cable (15,000 Amp.)

Figure IV-16 AC Superconducting Cables

enabling the cables to be packed in the designed winding cross section to give an average overall current density of 2300 A/cm^2 . The twisted groupings of 243 x 61.7 amp wires will be packed into a tube through which two-phase⁽¹³⁾ helium will be forced to circulate.

Calendered Cables (Fig. IV-16b) - In this arrangement, a basic group of 8 wires is twisted and rolled flat into a wide-aspect-ratio strip, two wires high and four wires wide. This strip is then used to form additional transposed groups until 256 composites are bundled into a fully-transposed cable. A packing factor of ~40% could be achieved, resulting in a cable having an average current density of 8900 A/cm^2 . This value is much higher than that in the group-of-three transpositions. The calendered cable is not easily encased in a tube but is more easily supported.

(2) Cryostatic Stability

To examine the stability, the longitudinal resistivity of copper must be used. For 37 kG, $\rho = 2.7 \times 10^{-8} \Omega\text{-cm}$. Since copper occupies a fraction of 0.570 in the composite, the heat flux h at 61.7 amp is given by

$$h = \frac{61.7^2 (2.7 \times 10^{-10})}{0.57 \cdot \frac{\pi}{4} \cdot (0.6 \times 10^{-3})^2 \pi (0.6 \times 10^{-3})} = 0.34 \text{ W/cm}^2$$

The group-of-three transposed cables clearly have ample cooling. For the calendered cable, if the basic group of eight strands were wrapped with a porous binding material, such as Nomex paper, sufficient cooling to achieve cryostatic stability could be obtained.

(3) Stressing and Reinforcement

The maximum hoop force on a wire in coil No. 5 is given by

$$F_w = BIR = 2.5 \times 61.7 \times 12.1 = 1866 \text{ Newtons}$$

The yield strength of the wire is roughly 100 Newtons, so reinforcement is necessary. The reinforcing material could be highly-resistive, stainless steel wires packed in the voids between twisted groups. The hoop force in the cable is 453 kN so the cable must be strengthened by impregnation within each of the five pairs of winding bundles.

c. AC Loss Calculations

The total cable length for the equilibrium-coil system is 48,480 meters. The total length of the 61.7 amp composite is 11.78×10^6 meters. Therefore, the total composite volume is 3.33 m^3 .

With a filament diameter 5μ and a maximum-field excursion of 37 kG, the total hysteresis loss over a complete cycle $0 \rightarrow 37 \text{ kG} \rightarrow 0$ is equal to

$$E_f = (3.33) (0.23) \left(\frac{4}{3\pi}\right) (9 \times 10^9) (5 \times 10^{-6}) \ln 4.7 = 22,637 \text{ J.}$$

For a complete fusion cycle, the matrix loss for a linear field rise and fall, namely $0 \rightarrow 37 \text{ kG} \rightarrow 0$ with $\dot{B} = 37 \text{ kG/sec}$, is given by

$$E_m = (3.33) \left(\frac{4}{3\pi}\right) \frac{(3.7)^2}{10^{-8}} (1.5 \times 10^{-3})^2$$

$$= 4,353 \text{ J}$$

The self-field loss for the composite current to rise from 0 to 61.7 amp and then back to zero again is given by

$$E_s = (3.33) (2) (0.05) (4 \times 10^{-7}) (61.7)^2 / 2\pi^2 \cdot (6 \times 10^{-4})^2$$

$$= 224 \text{ J}$$

So the total conductor losses over a complete-fusion cycle are 27,214 J. The complete fusion cycle takes 10 to 60 seconds. For a 35-second cycle time the average power dissipation at 4.2°K is 777 watts. The required time-average refrigeration at 300°K is approximately equal to 233 kW.

If the cable tube is metallic, the eddy-current loss associated with flux penetration into it could be estimated as follows:

Assume that the flux-penetration depth is greater than or comparable to the radius of the cable tube ($R = 1.4 \text{ cm}$). Then the eddy-current dissipation in a thin-wall tube during the field swing $0 \rightarrow B$ is given approximately by

$$Q_t = \frac{B^2 R^2}{\tau \rho} \quad J/m^3 \quad (IV-11)$$

where R is the tube radius, τ is the duration of field change, and ρ is the resistivity of the tubing material. For stainless steel tubing with a tube

wall thickness of 1.4 mm, the energy dissipation in the cable tube E_t over a complete-fusion cycle is given by

$$E_t = (2.985) (2) \frac{(3.7)^2 (1.4 \times 10^{-2})^2}{(1) (5 \times 10^{-7})} = 32,038 \text{ J}$$

and the equivalent average power dissipation at 4.2°K is 915 watt. This clearly shows that a nonmetallic cable-tube design must be pursued.

d. Temperature Rise

Although the heat generated in the wire will eventually be dissipated in the liquid helium, it will cause a temperature rise that must not exceed the critical temperature of the superconductor. The critical temperature of the superconductor, at 37 kG with $j_s = 2.185 \times 10^4 \text{ A/cm}^2$, is about 6.6°K.

The worst case assumes adiabatic-heat generation during the field swing of 0 → 37 kG in 1 second. It is assumed that filament and self-field losses are confined to the superconductors and the matrix losses are confined to the copper matrix. The dissipation and temperature rise associated with each is then

	<u>Dissipation During Field-Rise Cycle</u>	<u>Temperature Rise (From 4.2°K)</u>
Filament Loss	11430 joules	4.8 °K
Matrix Loss	2177 joules	0.8 °K

In these calculations, data for the enthalpy change of copper between 4°K and 10°K were used. The mass specific heat of NbTi was assumed to be equal to the mass specific heat of copper. Therefore, the enthalpy change for NbTi⁽¹⁴⁾ between 4°K and 10°K was assumed to be roughly equal to that of copper. It is clear that, without cooling, the magnet will quench during the field swing. This is true even if the filament diameter of NbTi is reduced to 2.5 μ. In this case, the temperature of NbTi will be 8°K.

If each wire is freely cooled, the total surface area is 22,205 m². Therefore, the heat transfer rate is

$$h = \frac{11430 + 2177}{22205} = 0.6 \text{ W/m}^2 = 0.06 \text{ mW/cm}^2$$

Hence under extremely-weak cooling conditions, the heat-transfer rate is sufficient to contain the temperature rise. However, the inventory of liquid helium surrounding and associated with each wire must be sufficient to absorb the heat generated. In the group-of-three cables, however, the inventory of liquid helium is too large to be practical.

5. Copper Equilibrium-Coil Design

The following criteria were used in the copper-coil design: (1) The equilibrium coil is charged from zero to 15,000 A in one second; it remains at 15,000 A for 20 seconds; then it is discharged in one second back to zero. After this, it remains at zero for 13 seconds during the pump-down cycle. So for a complete cycle time of 35 seconds, the time-averaged current is 9,254 A. (2) A practical inlet-water temperature of 40°F was selected. This requires a water cooling facility. (3) A water-temperature gradient of 130°F was chosen to keep the maximum coil temperature in a range where a conventional epoxy-impregnated structure is useable. (4) A pressure gradient of 200 psi was considered to be the highest value that can be used based on pump availability, maintenance, minimizing the leak potentials, and strength of insulating water connections. (5) Each water circuit was selected to contain 10 electrical turns, which is a convenient number to use to represent one layer in the coil structure. (6) The conductor used in the calculation was square with a circular water hole in the center. Using these conditions and limitations, an iterative computer code⁽¹⁵⁾ for water-cooled, copper-coil design was used and, maximizing the average current density over the conductor cross-section by varying the dc voltage of each coil, it was found that coil No. 5, the largest coil of all, can have only an average current density of 357 A/cm². The current density increases with shorter coils, with the maximum j_{avg} of 1104 A/cm² in coil No. 2. As a result, the copper-coil winding cross section is generally larger than those listed in Table IV-4. The computed conductor dimensions with the dc voltage chosen are given in Table IV-4. The total copper weight is 760 tons.

The power dissipation of each coil is given by 9254 times the dc voltage. The total power dissipation of the equilibrium coil is 92 MW. Total circulating water is 8,000 gallons per minute. To cool this water from 170°F back to 40°F requires 20,000 tons refrigeration (a ton is equivalent to 12,000 Btu/hr).

The total dc resistance is $0.686 \, \Omega$. The total coil impedance is given by

$$Z = \sqrt{0.686^2 + \pi^2 (0.8)^2} = 2.6 \, \Omega$$

Therefore, the peak voltage required to drive the equilibrium coil is 40 kV. The total dc voltage is 10,100 volts.

The conductor dimensions listed in Table IV-4 cannot be used for an ac conductor. A simple ac cable made of transposed strands is shown in Figure IV-17. There is insulation between strands, and the strands are twisted. The central strand is an insulating tube that serves as a cooling channel as well as structural support. The total conductor cross section must be equal to those listed in Table IV-4.

C. Ohmic Heating Coil System

The OH coil functions as the primary and the plasma functions as the secondary of a transformer. When the OH coil is energized, it induces an electric field in the plasma. The electric field breaks down the plasma gas and ionizes it (if this has not previously been accomplished otherwise) and drives the plasma current up to the burn-phase value. During the burn phase, if the bootstrap current does not exist, maintenance of the plasma current will require an additional induced electric field to compensate for the IR drop. If the OH coils are located outside the TF-coil system, the winding configurations can be arranged so that the pulsing fields and the torque they imposed on the TF coils are minimized. It is also necessary that the OH-coil fields in the plasma column region be nearly zero so that the plasma can be initially ionized.

1. Basic Requirements

a. Plasma Inductance

The plasma inductance affects the energy, power, and volt-second requirements of the ohmic-heating transformer system for a tokamak reactor.

In the case of a circular plasma, with radius "a" and major radius " R_o ", the self inductance is given by⁽¹⁶⁾

$$L = \mu_o R_o \left[\left(\frac{2}{k} - k \right) K - \frac{2}{k} E \right] \quad (IV-12)$$

Figure IV-17. AC . Copper Cable

where $k^2 = 4 R_o (R_o - a) / (2R_o - a)^2$ and K and E are the elliptic integrals

$$K = \int_0^{\pi/2} \frac{d\alpha}{(1 - k^2 \sin^2 \alpha)^{1/2}}, \quad E = \int_0^{\pi/2} (1 - k^2 \sin^2 \alpha)^{1/2} d\alpha,$$

In the limit $R_o > a$, the equation is reduced to

$$L = \mu_o R_o \left(\ln \frac{8 R_o}{a} - 1.75 \right) \quad (\text{IV-13})$$

In the case of a noncircular plasma, there is no exact formula for plasma inductance. A computer program was developed to calculate the self and mutual inductance of a set of circular and coaxial coils of rectangular cross section. For any noncircular plasma, one can divide the plasma cross section into many circular coils each having a rectangular cross section. The self inductance of the plasma is then equal to the sum of the self inductance of individual coils and the mutual inductance between them. A series of computer calculations were made to give the exact inductance of several noncircular cross sections likely to be encountered in reactors now under consideration. The plasma shapes investigated were an elliptical toroid, a rectangular toroid, and a peanut-shaped toroid. It was found that a good analytical fit to the exact inductance can be obtained (within 10%) by using the formula for the equivalent circular cross section having the same area and major radius. For example, for an elliptical cross section having a height-to-width ratio of "K", a major radius of " R_o ", and a minor radius "a":

$$\text{Cross-section area of ellipse} = \pi K a^2$$

$$\text{Radius of equivalent circle having same area} = a \sqrt{K} \equiv R_c$$

Inductance of elliptical toroid $\approx \mu_o R_o \left[\ln \frac{8 R_o}{R_c} - 1.75 \right]$ with an error $\leq 10\%$ in the range of an aspect ratio $R_o/R_c \geq 3$.

This formula is now being used where required in the various plasma reactor codes to obtain the noncircular-plasma inductance.

b. The Volt-Sec Requirements of Ohmic-Heating Coils

During the startup period, when the electron temperature is low (< 0.5 keV), the plasma loop is more of a resistive than an inductive load. The plasma resistance decreases as the electron temperature increases. When $T_e \gtrsim 0.5$ keV,

the plasma loop becomes mostly purely inductive, but with a slightly resistive component. During the entire startup-time period, the amount of volt-sec required $\Delta\phi_s$ is given by

$$\Delta\phi_s = I_p L_p + \int_0^{T_s} i_p R_p dt \quad (IV-14)$$

where $I_p L_p$ is the required inductive volt-sec and $\int_0^{T_s} i_p R_p$ is the required resistive volt-sec for starting up the plasma current. (Reduction in the required volt-sec, due to mutual coupling between the OH coils and the plasma, is not accounted for in this estimate.) During the burn cycle, if no bootstrap current exists, an additional core flux, $\Delta\phi_b$, is needed to maintain the plasma current. That is,

$$\Delta\phi_b = \int_{T_s}^{T_b} i_p R_p dt \quad (IV-15)$$

The amount of volt-sec for $\int_0^{T_s} i_p R_p dt$ and for $\Delta\phi_b$ depends upon the plasma resistivity (impurity level and anomalous effects), the mode of startup, the operating temperature and the duration of the burn phase. Based upon the burn-cycle dynamics studies (summarized in Table III-8), it appears

that $\int_0^{T_b} i_p R_p dt < L_p I_p$. Thus, the assumption that the required volt-sec

are $2L_p I_p$ is a conservative one, which should provide some margin. L_p was computed to be 12.273 μH for a circular plasma with plasma radius $a = 2.1$ m and major radius $R_0 = 6.25$ m. For $I_p = 4.5 \times 10^6$ amperes, 110 volt-sec are required.

The OH system is a large fast-pulsed magnet and it is highly desirable to reduce the volt-sec requirements and thus the stored energy. The equilibrium-field coil is designed to produce as much as 37 volt-sec and as a result, the required volt-sec for the OH coil is 73.

2. Evaluation of Various Ohmic-Heating Transformer Schemes

Consider an OH coil winding configuration such that it produces zero field (or nearly zero field) inside its interior by proper distribution of conductor outside the TF coils, as indicated in Fig. IV-18. In this winding arrangement,

Figure IV-18. Ideal OH Coil Configuration

all the flux produced by the OH coils will pass through the central core and will be linked to the plasma loop. That is,

$$\Delta\phi = L_{OH} (NI_{OH}) = L_p I_p + \int_0^{T_b} i_p R_p dt \quad (IV-16)$$

where L_{OH} is the self-inductance of OH coils, NI_{OH} is the required ampere-turns for the OH coil, and L_p is the self-inductance of the plasma loop. For a given OH coil configuration and plasma dimensions, both L_{OH} and L_p can be computed accurately. For first-order estimates, L_p is given for circular-plasma cross section by Eq. (IV-13).

The OH coils could be water-cooled or cryogen-cooled normal conductors, or they could be superconducting coils. The central core could be an air core or an iron core. If the coils are wound with normal conductor, the I^2R loss will be large. To minimize the time interval during which the primary carries a large current, the best charging scheme for normal coils is shown in Fig. IV-19a. This charging scheme is particularly attractive when the burn cycle is much longer than the startup cycle. The instantaneous I^2R loss is given by

$$P_{ins} = J_c^2 (t) \rho V$$

where J_c is the conductor current density, ρ is the resistivity of conductor and V is the conductor volume. The I^2R energy dissipation over a complete cycle time T_c is given by

$$E = \rho V \int_0^{T_c} J_c^2 (t) dt$$

In addition, there are eddy-current losses, which are small in comparison and can be minimized by proper cable design with full transposition and multiple strands. The required ampere-turns is given by Eq. (IV-16).

The maximum field for normal coils is limited by either conductor stress or by volumic specific-power density, W_v . In the conductor stress limitation, a reasonable B_{max}^{OHC} is given by

$$\frac{(B_{max}^{OHC})^2}{2 \mu_0} \approx 10,000 \text{ psi.}$$

That is, the $B_{\max}^{\text{OHC}}$ is about 130 kG to 140 kG. The required core size is given by

$$\pi r_v^2 B_{\max}^{\text{OHC}} \approx \Delta \phi \quad (\text{IV-17})$$

where r_v is the flux core radius. In the specific power-density limit, a reasonable W_v is about 2×10^4 watts/cm³. Therefore, the minimum core size is given by

$$W_v = j_{\text{avg}}^2 \rho \lambda \approx (\text{geometrical factor}) \cdot \frac{P_{\text{ins}}}{r_v^3}$$

where j_{avg} is the average current density over the winding cross-section and λ is the packing factor.

If the OH coils are superconducting, the $I^2 R$ loss is negligible, but the useful maximum field for NbTi superconductor is limited to around 80 kG. The best charging scheme for a superconducting OH coil is shown in Fig. IV-19b. This charging scheme requires only half as many ampere-turns and permits the maximum central field to be reduced by 50%, relative to the charging scheme shown in Fig. IV-19a, for the same volt-sec requirements. If the required volt-sec is $2 L_p I_p$, then the maximum central field is given by

$$\pi r_v^2 B_{\max}^{\text{OHC}} = \frac{1}{2} (2 L_p I_p). \quad (\text{IV-18})$$

Recall that

$$L_p \approx \mu_o R_o \left(\ln 8 \frac{R_o}{a} - 1.75 \right),$$

$$I_p = \frac{2\pi a}{\mu_o} \cdot \frac{B_t}{qA},$$

$$B_t = B_{\max}^{\text{TFC}} \left[\frac{R_o - (r_w + \Delta_B + \Delta_s)}{R_o} \right],$$

where Δ_B and Δ_s are the blanket and shield thickness, r_w is the toroidal-vacuum chamber radius, q is the safety factor for MHD stability ($q \geq 2.5$), and $A \equiv R_o/a$ (see Fig. I-1). Substituting the above three expressions into Eq. (IV-18), one obtains the required core size as

$$r_v = \sqrt{\frac{B_{\max}^{\text{TFC}}}{B_{\max}^{\text{OHC}}} \frac{2a}{qA} \left[R_o - (r_w + \Delta_B + \Delta_s) \right] \left[\ln 8 \frac{R_o}{a} - 1.75 \right]} \quad (\text{IV-19})$$

a. Charging Scheme for Copper Coil

b. Charging Scheme for Superconducting Coil

c. Charging Scheme for Iron Core Transformer

Figure IV-19 Charging Schemes of Various Transformers

In comparing the air-core, normal-coil primary and the air-core superconducting primary, the superconducting system requires half the amount of ampere-turns, has a much smaller power loss, and a simpler power supply. If the maximum field for a normal coil could be designed so as to be twice as high as the maximum field for a superconducting coil, the core size requirements are equal. On the other hand, industry has more experience in normal-coil design than in superconducting-coil design.

If an iron core is used, the charging schemes of Fig. IV-19a and Fig. IV-19b are invalid because an iron core transformer with an open secondary will saturate with only a small primary excitation. The OH coils must be energized according to Fig. IV-19c. In this charging scheme, the energy loss is very large if the coil is a normal conductor. The ampere-turns could not be reduced by half, if the coil is superconducting. Furthermore, the maximum central field is severely limited by the saturation level of iron (≈ 18 kG). As a result, the required core size will be larger than with an air-core primary. Although L_p for the iron core is reduced slightly, and a small dc bias current may be applied to drive the iron core toward reverse saturation prior to charging the OH coil, these advantages may not offset the disadvantages. A large iron-core cost is another consideration. For the iron-core transformer, an additional power loss due to iron losses must also be taken into consideration.

In conclusion, an air core is superior to an iron core for small-aspect-ratio, high-power-density* tokamak reactors. Superconducting air-core OH coils have less power loss and reduce somewhat the very difficult task of designing the power supply. In this study, the air-core superconducting OH coil was chosen as the primary design option, with the air-core copper coil as a secondary design option.

3. Air-Core Superconducting OH-Coil Design

a. OH-Coil Characteristics

The OH coils are located outside the TF coil system. The coil windings are distributed so that the field produced by the OH coils is nearly zero in

* As shown in Section II-B, the power $P \propto B_t^4$, and, as shown above,

$$r_v^2 B_{\max}^{OH} \propto \Delta \phi \propto I_p \propto B_t \propto P^{1/4}$$

the plasma region, and the fields superimposed on the TF coils are minimized. Ideally, one would prefer to distribute the winding evenly in a configuration as depicted in Fig. IV-18, so that there is no winding concentration anywhere around TF coils. This idealized distribution will produce only small superimposed fields even if the OH windings are located close to the TF coils. However, to provide access for beam injectors, plumbing, vacuum pumps, primary coolant, remote maintenance equipment, etc., the OH-coil configuration was designed as shown in Fig. IV-20. The detailed winding dimensions and magnet characteristics are listed in Table IV-5.

In Table IV-5, R_1 , R_2 , Z_1 , and Z_2 represent the inner radius, the outer radius, the initial axial coordinates and the final axial coordinates, respectively. The flux requirement for the OH coil is 73 volt-seconds. The radius of the flux-linkage core r_v is 1.9 meters, so the required central field is 32 kG. The total conductor length is 627.8×10^6 ampere-meters. The total ampere-turns is 45.5×10^6 . Coil Nos. 2, 3, 4, 5 and 6 are needed to reduce the magnetic field in the TF coils and in the plasma column. These coils are positioned at a distance away from the TF coils so that the maximum field superimposed on the TF coils is reduced. All field calculations were computed accurately with full coil cross section and without filamentary approximations. The magnitude and the direction of the OH-coil field is indicated in Fig. IV-20.

A fairly conservative current density of 1280 A/cm^2 was chosen in the design calculations. The operational current is chosen to be 40,000 amperes so that the self-inductance is 1.1 H. The energy stored in the OH coil self-field is 883 MJ. If the plasma startup time is 1 second, the power supply voltage will be 140 kV.

The energy stored in the entire coupling field system of OH coils and plasma is given by

$$W_m = \frac{1}{2} L_{OH} I_{OH}^2 + \frac{1}{2} L_p I_p^2 - M_{OH-p} I_{OH} I_p \quad (IV-20)$$

where M_{OH-p} is the mutual inductance between OH coils and plasma. L_p is the self-inductance of the plasma and is computed to be 12.273 μH . L_{OH} is the self-inductance of the OH coils and is 1.1 H. M_{OH-p} was calculated to be 958 μH . The total energy the power supply must provide to start up

Table IV-5. Ohmic-Heating Coil System

Coil No.	R ₁ (cm)	R ₂ (cm)	Z ₁ (cm)	Z ₂ (cm)	Central Field (Gauss)	Conductor Peak Field (kG)	Coil-Pair Conductor Length (amp-m)
1	180	200	0	± 650	30877	32	39.7 x 10 ⁶
2	190	230	± 650	± 680	251	28	40.5 x 10 ⁶
3	210	250	± 680	± 710	260	27.5	44.4 x 10 ⁶
4	230	270	± 710	± 740	267	25	48.2 x 10 ⁶
5	370	400	± 740	± 770	352	15	55.7 x 10 ⁶
6	1152.5	1167.5	± 402.45	± 417.45	261	4	42.0 x 10 ⁶

Table IV-6. Copper Ohmic-Heating Coil System

	Coil Pair Section Number					
	1	2	3	4	5	6
No. of layers/coil	140	7	7	7	6	3
Turns/layer	3	6	6	6	6	3
Turns/water circuit	12	12	12	12	12	3
Conductor outside dimension (cm)	3.63	4.37	4.57	4.58	4.60	4.58
Conductor inside dimension (cm)	0.86	0.78	0.79	0.83	1.07	0.91
V dc at 4454 A	680	50	50	54	72	54
Power (MW)	3.0	0.22	0.22	0.24	0.4	0.24
Q (gallon/min)	160	12	12	13	17	13
Copper weight (tons)	121	20	24	26	34	26

Figure IV-2C. Field Resulting From OH Coil Alone

the plasma is W_m plus the energy dissipated in resistance losses:

$$\begin{aligned}
 U_{ps} &= W_m + \int_0^{T_s} \left[R_p I_p^2 + R_{OH} I_{OH}^2 \right] dt \\
 &= 883 \text{ MJ} + 124 \text{ MJ} - 172 \text{ MJ} + \int_0^{T_s} \left[R_p I_p^2 + R_{OH} I_{OH}^2 \right] dt \\
 &= 835 \text{ MJ} + \int_0^{T_s} \left[R_p I_p^2 + R_{OH} I_{OH}^2 \right] dt \quad (IV-21)
 \end{aligned}$$

The instantaneous power demand is given by

$$\begin{aligned}
 p = \frac{dU_{ps}}{dt} &= L_{OH} I_{OH} \frac{dI_{OH}}{dt} + L_p I_p \frac{dI_p}{dt} - M_{OH-p} \frac{d(I_{OH} I_p)}{dt} \\
 &\quad + \left[R_p I_p^2 + R_{OH} I_{OH}^2 \right] \quad (IV-22)
 \end{aligned}$$

A representative fusion cycle for the superconductive OH coil is shown in Fig. IV-21.

b. Design of 40,000-Ampere AC Superconducting Cable

The same considerations discussed for the equilibrium-coil ac cable in Section IV-B-4 also apply here. The suitable conductor strand is a 3-component composite. A suitable composite was calculated with the following characteristics:

NbTi critical current density	$2.14 \times 10^5 \text{ A/cm}^2$ at 4.2°K, 32 kG
Composite operational current	54.9 A
Composite critical current	109.8 A
Composite diameter	0.50 mm
Composite NbTi packing factor λ	0.26
Composite composition NbTi:cu:cupro-nickel	0.26:0.54:0.20
Filament diameter	5 μm
Twist pitch	6 mm
Number of filaments in composite	2613
Composite operational current density	$2.796 \times 10^4 \text{ A/cm}^2$
Composite critical current density	$5.592 \times 10^4 \text{ A/cm}^2$
Composite matrix effective resistivity	$10^{-6} \Omega\text{-cm}$

The composite can be fabricated easily with existing technology.

OH COIL POWER SUPPLY

Figure IV-21. A Representative Power Transfer Cycle for OH Coil

(1) Cable

The complete conductor will have 729 composites, fully transposed to have good current sharing. The same two basic cable arrangements discussed in the equilibrium-coil design could be used here.

If the cable is the group-of-three, the 40,000 A cable will consist of 729 composites with n equal to 6. Thus, the cable-packing factor is 7.3%, and the cable average current density will be $2,041 \text{ A/cm}^2$. Such a cable could be packed into the OH-coil winding to form a current density of 1280 A/cm^2 . The diameter of the cable is 5 cm. The twisted groupings of $729 \times 54.9 \text{ A}$ wires will be packed into a tube through which two-phase helium will be forced to circulate.

In the calendered group cable, the basic group is five-wires wide by two-wires high. This basic strip is then used to form a 2×3 transposed group, and then another 2×3 transposed group and finally 2×1 group with a total of 720 composite bundles in the fully-transposed cable as shown in Fig. IV-22a. The 10-wire basic group is wrapped with porous Nomex paper. A packing factor of ~40% can be obtained with sufficient porous space for liquid helium. The cable can be packed into a rectangular tubing. The ideal tubing material may be the G-10 fiberglass. If vacuum sealing is a problem, a very thin-walled stainless steel tubing could be used together with the G-10 fiberglass. A resultant cable average current density of more than $11,000 \text{ A/cm}^2$ could be achieved. Calendered cable has better mechanical rigidity, higher current density, less conductor movement and less helium inventory, and is preferred in comparison with the group-of-three cable shown in Fig. IV-22b.

This OH coil system design is capable of providing plasma driving flux in excess of the present design requirement. For example, if the volt-sec requirement is increased to 200 volt-sec, the 1.9 m core radius and the OH-coil winding cross section of Table IV-5 could still be used to produce 75 kG central field with $J_{\text{avg}} = 3,000 \text{ A/cm}^2$. In this case, calendered-group cable is the only possible cabling scheme to produce the 200 volt-sec requirement.

In the group-of-three cable scheme, the required helium inventory would be too large to be realistic. Nevertheless this cable method would provide the best cooling and the simplest transposition scheme.

a) Calendered-Grouped Cable

b) Group-of-Three Transposition Cable

FIGURE IV-22. 40,000 Ampere AC Cable

(2) Cryostatic Stability

To examine the stability of the group-of-three cable, at 32 kG, the heat flux at 54.9 A is given by

$$h = \frac{54.9^2 (2.6 \times 10^{-10})}{0.54 \frac{\pi}{4} (0.5 \times 10^{-3})^2 \pi (0.5 \times 10^{-3})} = 0.47 \text{ watt/cm}^2$$

In the case of calendered cable, because of the high packing factor, the composite operational current could be reduced by as much as a factor of 3. It is clear that the cryostatic-stability condition could be met easily.

(3) Stressing and Reinforcement

The maximum hoop force will occur in coil No. 6, and is given by $F_w = BIR = 0.4 \times 54.9 \times 115.5 = 2536$ Newtons per composite. Even the composite wire in coil No. 1 is subjected to a hoop force of 333 Newtons. As the yield strength of the composite is approximately 100 Newtons, reinforcement is necessary. The reinforcing material could be packed in the voids between the twisting groups. The hoop force of the 40,000 A cable is about 924 kN for coil No. 6, so the cable must be consolidated by epoxy impregnation within bundles. Fortunately, most of the OH windings are adjacent to the TF-coil support cylinder, and use the cylinder to support the hoop force of the OH coils. Clearly the calendered cable, which is rectangular both in outline and structure, is better suited to be supported on its outer surface than is the group-of-three transposed cable.

c. AC-Loss Calculations

The total cable length of the OH-coil system is 15,702 meters. The total length of the 54.9 A composite is 11.447×10^6 meter. Therefore, the total composite volume is 2.25 m^3 .

Using 32 kG as the maximum-field excursion and 5μ as the filament diameter, the hysteresis loss over a complete fusion cycle, - 32 kG $\rightarrow$ + 32 kG $\rightarrow$ - 32 kG is given by

$$E_f = 2 (2.25) (0.26) \left(\frac{4}{3\pi}\right) (9 \times 10^9) (5 \times 10^{-6}) \ln 4.2 = 32,068 \text{ J.}$$

To calculate the matrix loss, the field swing could be approximated as a linear-field rise and fall. Namely, for a complete fusion cycle the

field swing is $-32 \text{ kG} \rightarrow +32 \text{ kG} \rightarrow -32 \text{ kG}$ with constant $\dot{B} = 64 \text{ kG/sec}$. The matrix loss is given by

$$E_m = (2.25) \left(\frac{4}{3\pi} \right) \frac{(6.4) (3.2) \times 2}{10^{-8}} (1.5 \times 10^{-3})^2 = 8800 \text{ J.}$$

The self-field loss for the composite current to rise from -54.9 A to 54.9 A and then back to -54.9 A is given by

$$E_s = (2.25) \cdot (0.29) (4\pi \times 10^{-7}) (54.9)^2 / 2\pi^2 (5 \times 10^{-4})^2 = 500 \text{ J.}$$

Therefore, the total ac loss from the conductor over a complete fusion cycle is $42,369 \text{ J}$. For a representative complete fusion cycle time of 35 sec , the average power dissipation at 4.2°K is $1,182 \text{ W}$. The required average refrigeration at 300°K is about 354 kW .

The $40,000 \text{ A}$ enclosure tube will have a large ac loss comparable to the conductor ac loss, if the tube is metallic. The ideal enclosure-tube material is G-10 fiberglass. If vacuum seals or structural stresses are a problem, a possible construction is a plastic-metal "jelly-roll" in which the metal is a vapor barrier. The dissipation in such a tube would be negligible.

d. Temperature Rise

The critical temperature of NbTi, with current density $2.8 \times 10^4 \text{ A/cm}^2$ at a magnetic field of 32 kG , is about 6.8°K .

During the field swing, $-32 \text{ kG} \rightarrow +32 \text{ kG}$ in one second, if the heat generation is adiabatic, the hysteresis loss and self-field loss will raise only the filament temperature. The matrix loss will raise the copper temperature. Since the mass specific heat of NbTi is about equal to the mass specific heat of copper within the few degrees-Kelvin temperature range, it is assumed that enthalpy data for copper could be used for NbTi. The temperature rise and the dissipation are given as follows:

	<u>Dissipation During Field Rise Cycle</u>	<u>Temperature Rise (from 4.2°K)</u>
Filament Loss	16284 J	6.3°K
Matrix Loss	4400 J	1.7°K

Clearly, without cooling, the OH coils will quench during the startup cycle.

If each composite is freely cooled, the total surface area is 17985 m^2 . Therefore the heat transfer rate is

$$h = \frac{(16284 + 4400)}{17985} = 1.15 \frac{\text{w}}{\text{m}^2} = 0.115 \frac{\text{mW}}{\text{cm}^2}.$$

This clearly demonstrates that there is sufficient surface area to cool the composite in the field-swing cycle even under extremely poor cooling conditions. That is, a small fraction of a percent of the conductor surface exposed to liquid helium is enough to prevent a temperature rise.

4. Copper OH-Coil Design

The following criteria were adopted for the copper-coil design: (1) The OH coil is charged from -80,000 A to zero in one second in the startup cycle and discharged to -80,000 A in 15 seconds at the end of the burn cycle. For a total period of 35 seconds, this results in a time-averaged current of -4454 A. (2) A practical inlet-water temperature of 40°F was used along with a water-temperature gradient of 130°F and a pressure gradient of 200 psi. The reasons for these values are the same as those for the equilibrium coil. (3) The choice of the number of electrical turns per water circuit was determined by the required coil dimensions, the number of turns making up each coil layer, and the fact that the conductor will be formed in maximum lengths of 500 to 700 feet. The water circuits were limited to lengths of 500 to 700 feet. (4) The conductor that resulted from these calculations was solid copper with a square-outside shape and a round hole in the center.

Using these conditions and limitations as the input, a computer code⁽¹⁵⁾ is used to maximize the average current densities in each coil. The resulting coil cross sections were much smaller than the design listed in Table IV-6 allowed. The copper area was subsequently increased to provide the originally-proposed coil area. This increases the copper but reduces power consumption. The computed conductor dimensions and operating parameters are given in Table IV-6. The total weight of the copper in this coil is 251 tons.

The power dissipation listed in Table IV-6 for each coil pair is given by 4454 times the dc voltage. The total power dissipation of the OH coil is 4.3 MW. The total cooling water required is 454 gallons per minute. To cool this water from 170°F back to 40°F will require about 1100 tons of refrigeration.

The total dc resistance of the coil is $0.216 \, \Omega$. The total impedance is given by $Z = \sqrt{(0.216)^2 + \pi^2 (1.1)^2} = 3.5 \, \Omega$. Therefore the peak voltage needed to drive the OH coil is 140 kV. The total dc voltage required is 960 V.

The type of ac conductor used would be a design similar to that described in Fig. IV-17 for the equilibrium coil.

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CHAPTER V

PLASMA SUPPORT SYSTEMS

A. Neutral Beam Injection

Neutral beam injector designs for TEPR will be based on significant extrapolations beyond the present state of the art. The designs will thus have to anticipate or wait on technologies that do not presently exist and studies which have not yet been made. The present discussion will be concerned with injector requirements, with various possible approaches to neutral injection, with computational models for these approaches, with present neutral beam technology and with required extrapolations beyond the present state of the art. Values for many of the parameters occurring in the computational models can only be estimated; an important part of the future effort on neutral-beam injectors for TEPR will be concerned with establishing their values.

Neutral beam heating requirements for TEPR were determined in Chapter III on the basis of burn-cycle dynamics calculations. In this chapter, the corresponding ion-source beam current and power requirements will be determined. The source requirements will depend on which of four different methods of beam production is chosen, on the amount of deuteron energy spread which can be tolerated, on the ion beam-composition and on beam production, transport and capture efficiencies.

The method of production chosen for the reference design is based on the one presently used in neutral-beam heating experiments. In this method, a positive ion beam is extracted from a deuterium plasma and accelerated to full energy before optional removal of unwanted charged components prior to injection into a neutralizer cell. Source requirements have been determined for three cases: in the first, all three components (D^+ , D_2^+ and D_3^+) of the ion beam are used to produce a neutral beam; in the second, only the D^+ component is used to produce a neutral beam; in the third case, all the components are used but source requirements are calculated by assuming that a neutral beam derived from D_3^+ does not contribute significantly to plasma heating. The efficiencies that enter into the calculation of source requirements are geometry-dependent and also depend on the method of

neutral beam production. Some reasonable estimates have had to be made about their values.

For start-up heating, 40 MW of neutral-beam power will be required for three seconds. Beam penetration of the plasma requires that the ion beam energy be in the range of 150-180 keV. For a 180 keV beam and estimated values of transport and capture efficiencies, the total ion-beam current will range from 890 to 1600 A, depending on whether three, two or one neutral-beam components deliver useful power to the plasma.

If ignition conditions are not achieved, it will be necessary to maintain the plasma balance by supplemental beam heating during the burn phase. This would nominally require 23 MW of neutral beam power for 20 to 50 seconds.

Nominal neutral-beam and ion-beam requirements for heating to ignition and for the beam-driven mode are shown in Table V-1. Ion-Beam currents and powers are given for the three- and one-component cases.

1. Neutral-Beam Production Methods

Energetic D^0 beams can be produced by neutralizing atomic ions (D^+ , D^-) and/or molecular ions (D_2^+ , D_3^+), and/or the charged atomic fragments from the dissociation of molecular ions, in a suitable target gas or vapor; some of the fragments of dissociation are uncharged to begin with. If positive or negative ions are extracted directly from a deuterium plasma and injected into a D_2 gas neutralizer after acceleration to energy $eV_{\pm}$, where $V_{\pm}$ is the beam voltage, the maximum power efficiencies for the processes $D^- \rightarrow D^0$ and $D_j^+ \rightarrow D^0$, $j = 1, 2, 3$, are given as functions of $eV_{\pm}/j$ by the curves of Berkner, Pyle and Stearns,⁽¹⁾ shown in Figure V-1.

Obviously, for energies of interest for TEPR, neutralization of D^- obtained by direct extraction is by far the most efficient process. Unfortunately, a direct extraction D^- ion source suitable for use in a neutral beam injector does not presently exist. Surface-plasma, negative-ion sources of the type developed at Novosibirsk⁽²⁾ have produced H^- currents of 880 mA at a 1 Hz rate for pulses of 1 msec duration, with a current density of 3.7 A/cm^2 . The energy of the 880 mA beam is not specified; it does not exceed 30 keV. The Russian source shows promise and may eventually prove to be superior to the positive-ion sources (of

Table V-1

Neutral-beam Injection System Requirements (Nominal)

Heating

Injected power (total)	40 MW
Ion beam composition	75% D^+ , 18% D_2^+ , 7% D_3^+
Ion source current	890/1600 A*
Ion source power	160/290 MW*
Ion beam energy	180 keV
Pulse duration	3 sec
Number of injectors	16-32

Supplemental Heating (Beam-driven Mode)

Injected power	23 MW
Ion beam composition	75% D^+ , 18% D_2^+ , 7% D_3^+
Ion source current	510/920 A*
Ion source power	92/170 MW*
Ion beam energy	180 keV
Pulse duration	20-50 sec
Number of injectors	16-32

* D^+ , D_2^+ , D_3^+ injection/ D^+ injection.

Figure V-1. Maximum Power Efficiencies for the Neutralization Processes $D^- \rightarrow D^0$ and $D_j^+ \rightarrow D^0$, $j = 1, 2, 3$ as a Function of the Energy per Deuteron. (Curves are from Reference 1.)

the Oak Ridge and Berkeley types), which are presently used in neutral-beam injectors for CTR devices. If and when it is developed to the point that it can be used as a TEPR source, demands on the injector system will be considerably reduced but, for the present, it will be assumed that suitable sources of this type will not be available for use in TEPR.

At UCLA, up to 10 A of H^- ions have been obtained in a cold, dense plasma produced by laser irradiation of a target of NaH powder held in a crucible in a vacuum of 10^{-6} Torr.⁽³⁾ The duration of the negative-ion pulse (10 μ sec) can be extended by lengthening the laser pulse. A scheme for the resonant detachment of the loosely bound electrons from accelerated H^- ions by illumination with a laser beam of $\approx 1.5 \mu$ wave length has been worked out. A practical TEPR neutral-beam injector using laser irradiation for the production of D^- and energetic D^0 is, at best, only a remote possibility at this time.

The technology for the production of D^0 for CTR devices by processes of the type $D_j^+ \rightarrow D^- \rightarrow D^0$ has not yet been developed and there is no assurance that it will be developed in time to be of use for TEPR. However, this method for the production of D^0 beams, while more complicated and less efficient than the single electron detachment process $D^- \rightarrow D^0$, is, in principle, substantially more efficient than any process leading directly from D_j^+ to D^0 at energies of interest for TEPR; it will, therefore, be given further consideration, primarily for future reference.

2. Neutral-Beam Production Models

The most efficient method for the production of neutral beams with energies of interest for TEPR requires that D^- ions be extracted directly from a deuterium plasma. The second most efficient method requires that positive ions (D^+ , D_2^+ , D_3^+) extracted from a deuterium plasma be converted, at low energy (several keV), into D^- ions, which are then accelerated to the required neutral-beam energy before injection into a neutralizer cell. Neither of these methods has ever been used to produce neutral beams for CTR devices because suitable direct extraction and charge exchange sources do not presently exist. The least efficient method, direct extraction of positive ions from a deuterium plasma and acceleration of the ions to the required energies before injection into a neutralizer, is the method presently used. Since the technology for this method is more advanced than the technologies for the other methods

and will require the smallest extrapolation beyond the present state of the art, it will receive more detailed consideration than the others in the following sections, where neutral-beam production models for each of the methods will be developed and discussed.

3. A Neutral-Beam Production Model for $D_j^+ \rightarrow D^0$

A positively charged ion beam extracted from the plasma of a deuterium ion source contains a mixture of atomic ions (D^+) and molecular ions (D_2^+ and D_3^+). If this monoenergetic beam, of energy eV_+ , is injected directly into a neutralizer, as is presently done at Berkeley and Oak Ridge and the various laboratories where neutral beams are being injected into CTR devices, the neutral beam emerging from the neutralizer cell will have five components. There will be three D^0 components, with the component derived from D_j^+ having energy eV_+/j , a D_2^0 component (derived from D_2^+) with energy eV_+ and a second D_2^0 component (derived from D_3^+) with energy $2/3 eV_+$. The D_2^0 components constitute a negligible fraction ($\sim 1\%$) of the neutral beam; they will be excluded from further deliberations, which will be concerned only with D^0 components having energies of eV_+/j .

If the beam entering the neutralizer cell traverses the cell without loss, the total equivalent current of deuterium atoms emerging from the neutralizer is:

$$I_o = \sum_{j=1}^3 \eta_{jo} I_{D_j^+} \quad (V-1)$$

where η_{jo} is the deuterium-atom yield per entering D_j^+ ion. The total neutral-beam power is:

$$P_B = \sum_{j=1}^3 \eta_{jo} I_{D_j^+} \frac{V_+}{j} \quad (V-2)$$

Let f_{jo} be the power efficiency for the process $D_j^+ \rightarrow D^0$:

$$\begin{aligned} f_{jo} &= \frac{\text{Power in } D^0 \text{ beam derived from } D_j^+}{\text{Power in } D_j^+ \text{ beam entering neutralizer}} \\ &= \frac{\eta_{jo} (I_{D_j^+}) V_+/j}{(I_{D_j^+}) V_+} = \frac{1}{j} \eta_{jo}, \quad j = 1, 2, 3 \end{aligned} \quad (V-3)$$

In terms of power efficiencies,

$$P_B = V_+ \sum_{j=1}^3 f_{j0} I_{D_j}^+ . \quad (V-4)$$

If Γ_j is the D_j^+ fraction of I_+ (the total current of positive ions entering the neutralizer) and impurity ions constitute a negligible fraction of I_+ ,

$$P_B = I_+ V_+ \sum_{j=1}^3 f_{j0} \Gamma_j ,$$

or (V-5)

$$P_B = P_+ \sum_{j=1}^3 f_{j0} \Gamma_j .$$

This equation applies to a neutral beam which contains components with energies eV_+/j , $j = 1, 2, 3$. If a separator is placed ahead of the neutralizer to remove one or more of the positive ion components from the beam entering the neutralizer, the equation can be written in the form

$$P_B = P_+ \sum_{j=1}^3 \xi_{j0} f_{j0} \Gamma_j , \quad (V-6)$$

where ξ_{j0} is an option parameter which can have the value 1 or 0. The total positive-ion current required to produce a given amount of useful neutral-beam power is

$$I_+ = \frac{P_B/V_+}{\tau \sum_{j=1}^3 \epsilon_{Rj} f_{j0} \Gamma_j} \quad (V-7)$$

where τ is the transport efficiency from the source to the reactor and ϵ_{Rj} is the reactor capture efficiency for neutral beam with energy eV_+/j .

Use in the above equation of f_{j0} values taken from the curves of Figure V-1 requires some justification, because each of the curves applies (at each energy) for a target thickness which is different for each species of ion. Where values of target thickness which optimize the values of f_{j0} exist, these optimum values have been plotted by Berkner, Pyle and Stearns; where optimum target thicknesses do not exist, they have plotted values of f_{j0} corresponding to 95% of the equilibrium values.

In one case where results obtained by using their curves can be compared with results obtained by Berkner, Pyle and Stearns for the specific example of a target thickness of 1.5×10^{16} D_2 molecules/cm² and a 160 kV beam,⁽⁴⁾ the value of $\sum_1^3 f_{j0} \Gamma_j$ obtained by use of the curves is 5.4% higher than the value obtained from results given in reference 2. This is not a significant difference in view of the estimated uncertainties of $\pm 10\%$ or more (given in Table I of reference 1) in the cross sections used to calculate the values of f_{j0} .

Source current and power requirements for a 40 MW neutral beam, for several ion beam energies and various values of the option parameters, are given in Table V-2. The Γ_j values used in the Table are those obtained with the Berkeley "10 A" source.⁽⁵⁾ The value of 0.8 assigned to the transport efficiency τ is arbitrary but not too different from values obtained with present-day injectors. ϵ_R , the capture efficiency, may be variously defined as that portion of the neutral beam which is ionized in passing through the reactor plasma, or that fraction of the beam which penetrates far enough into the plasma to minimize premature loss to the first wall or a limiter, or that portion of the beam energy which is transferred to the plasma ions and electrons. In the absence of a precise definition, we have chosen as a default value $\epsilon_R = 0.95$. Factors which affect the capture efficiency are discussed in Section V-A-7.

Source requirements are given for three sets of option parameters in Table V-2. The first set gives the case where all of the ion beam passes through the neutralizer. Source requirements are calculated on the assumption that all of the neutral beam is fully effective in heating the reactor plasma. The third set of option parameters gives the case where molecular ions are separated from the beam before it enters the neutralizer. The second set of option parameters gives the case where all of the ion beam passes through the neutralizer, but source requirements are calculated on the assumption that the $V_+/3$ energy component of the neutral beam, derived from D_3^+ , does not contribute useful power to the plasma.

A neutral-beam production model for $D_j^+ \rightarrow D^0$, with energy recovery and optional removal of unwanted charged components, is shown in Figure V-2. The first separator, if it is used, must be a magnetic separator of charged particles having the same energy but different momenta. The second separator

Table V-2. Source Requirements for a 40 MW Neutral Beam^a

V_+ (kV)	eV_+/j				$\xi_j = 1; j = 1, 2, 3$		$\xi_1 = \xi_2 = 1; \xi_3 = 0$		$\xi_1 = 1; \xi_2 = \xi_3 = 0$	
	j	(keV)	f_{jo}	$f_{jo} \Gamma_j$	I_+ (A)	P_+ (MW)	I_+ (A)	P_+ (MW)	I_+ (A)	P_+ (MW)
120	1	120	0.43	0.32	830	100	1000	120	1400	165
	2	60	0.73	0.13						
	3	40	0.82	0.058						
160	1	160	0.29	0.22	880	140	1000	160	1500	240
	2	80	0.62	0.11						
	3	53.3	0.76	0.05						
180	1	180	0.24	0.18	890	160	1100	190	1600	290
	2	90	0.58	0.10						
	3	60	0.73	0.05						
200	1	200	0.20	0.15	900	180	1100	220	1800	350
	2	100	0.53	0.095						
	3	66.7	0.70	0.049						

^a Ion beam accelerated to full energy before optional removal of unwanted components. Requirements are given by Eq. V-7. ($\tau = 0.8$; $\epsilon_{Rj} = 0.95$; $j = 1, 2, 3$; $\Gamma_1 = 0.75$; $\Gamma_2 = 0.18$, $\Gamma_3 = 0.07$)

I_+ = Total D_1^+ , D_2^+ , D_3^+ Current

V_+ = Beam Voltage

Γ_j = D_j^+ fraction of I_+

$$P_+ \sum \Gamma_j = P_+ = I_+ V_+$$

$$k = \frac{P_{\text{source}} + P_{\text{accelerator}}}{P_{\text{ion beam}}} \quad (k \approx 1.3)$$

τ = Transport Efficiency

ϵ_{Rj} = Reactor Capture Efficiency

f_{j0} = Power Efficiency for $D_j^+ \rightarrow D^0$

ξ_j = Option Parameter = 1 or 0

Figure V-2 Neutral Beam Production Model for $D_j^+ + D^0$, $j = 1, 2, 3$, with the Ion Beam Accelerated to Full Energy Before Optional Removal of Unwanted Components

may be a magnetic separator or an electrostatic separator, which will, in either case, remove charged particles from the neutral beam going to the reactor; use of the reactor magnetic field as a separator is not ruled out, but in this case thermal rather than electrical energy recovery will probably have to be used. A shortcoming of the model of Figure V-2 is that the option of thermal recovery is not provided for in the second separator; this option can be exercised for the first separator by letting $\tau_1 = 0$.

Electrostatic separators for the removal of charged components from the neutral beam may be of the deflection or the retarding field type. The latter can also be used as in-line direct converters of the types described by Moir⁽⁶⁾ and by Fumelli and Valckx.⁽⁷⁾ As noted by Moir, if lower energy ions resulting from the dissociation of molecular ions constitute an appreciable fraction of the un-neutralized beam, the efficiency of an in-line direct converter is seriously reduced.

In reference 7, Fumelli and Valckx describe a neutral beam injector in which the ion source, an annular duoplasmatron, is operated at ground potential, eliminating the need for isolation transformers for the source filament, arc and magnet supplies. The extractor grid, the decelerating grid, the neutralizer and the suppressor grids are all at high negative potential, as shown in Figure V-3.

The bias voltage on the neutralizer cell prevents the escape of electrons from the cell. D^+ ions which lose energy traversing the cell and fractional-energy charged fragments from the dissociation of molecular ions do not have sufficient energy to reach the grounded screen grid; they are trapped in a potential well between the extractor and screen grids and can only go to the neutralizer, the suppressor grid or the decel grid. On the other hand, D^- ions emerging from the neutralizer with energies eV_+/j are accelerated to energies $e(V_+ + V_+/j)$. Thus, the neutral beam source of Figure V-3 can also be regarded as a tandem-acceleration negative-ion source, with electrode arrays and grid potentials similar to those used in the H^- source for the Zero Gradient Synchrotron booster at Argonne.⁽⁸⁾

In reference 7, Fumelli and Valckx describe energy-recovery experiments in which the grounded screen grid of Figure V-3 was replaced by a parallel-ribbon grid connected to a 500 V a.c. supply. When the injector was operated with helium, good recovery efficiency was obtained; with hydrogen gas and a mostly molecular hydrogen ion beam, recovery efficiency was poor.

Figure V-3 Schematic Showing the Grid Arrays and Grid Potentials in the Neutral-Beam Source of Reference 5

When positive ions are injected into a neutralizer, most of the beam emerging from the neutralizer is charged. If the molecular components were removed from the ion beam before it entered the neutralizer, the charged beam emerging from the cell, with energy eV_+ , could be recirculated through the neutralizer several times by placing the neutralizer in one of the straight sections of a storage-ring.^(9,10) In principle, recirculation could raise the fraction of usable D^+ beam to 95% or more and thus reduce the required D^+ beam current for a given neutral-beam power by the factor $0.95/f_{10}$, approximately. The ring would be rather large, with a major diameter of about 3m, and it would require a vacuum of 1×10^{-5} Torr or less everywhere except in the neutralizer, where the pressure would be of the order of 10^{-2} Torr.

To take a specific example, we note from Table V-2 that for a 180 keV beam with the molecular ions removed from the beam entering the neutralizer, the required source current is 1600 A while the accelerated beam power is 290 MW, for a 40 MW neutral beam. If the fraction of usable D^+ beam could be raised to 95% by recirculation, the required source current for a 40 MW D^0 beam would be reduced to $(0.24/0.95) \times 1600 = 400$ A. This would be more than a factor of two lower than the current of 890 A for the case shown in Table V-2 where all of the ion-beam components are injected into the neutralizer. The 400 A ion current could be provided by 8 injectors, with a total ion current of 50 A per injector, or by 16 injectors, with a 25 A current per injector.

At this point it is necessary to note that development of a beam recirculator capable of handling tens of amperes of ion beam will require a major technological advance. In the absence of a strong experimental program and demonstrated progress toward the achievement of the objectives of the program, we have chosen not to include recirculation in the beam production models we are considering here.

The overall efficiency of the neutral injector, for the model of Fig. V-2, is given by the expression

$$\epsilon_{NI} = P_B / (kP_+ - P_R) \quad (V-8)$$

where

$$P_B = \tau_2 P_+ \sum_{j=1}^3 \epsilon_{Rj} \xi_j f_{jo} \Gamma_j \quad (V-9)$$

is the neutral-beam power, kP_+ is the input power and P_R is the total power recovered and returned to the source and accelerator by the electrical and thermal energy recovery systems. If $\xi_j = 1$, $j = 1, 2, 3$, the neutral beam is the sum of three components derived from the three ion-beam components. The way in which the beam power is distributed among the three components is shown as a function of ion-beam voltage, V_+ , in Fig. V-4. The neutral-component energies are eV_+/j , $j = 1, 2, 3$.

The over-all power efficiency of the neutral injector, ϵ_{NI} , as defined in Eq. (V-8), is plotted as a function of V_+ for one- and three-component neutral beams, with and without energy recovery, in Fig. V-5. The efficiencies with recovery for the one-component case were calculated with $\tau_1 = 1.0$.

An ion source with staged acceleration consists of a plasma followed by a number of gridded electrodes with apertures or slots which are usually aligned, but sometimes deliberately misaligned to cause individual beamlets to be steered. Steering was first used for thrust vectoring in electron-bombardment ion thrusters.^(11,12) It has more recently been used to focus individual beamlets toward a common line⁽⁵⁾ or point.⁽¹³⁾ The first gridded electrode, called the source grid in Fig. V-3, serves as a plasma source electrode at the potential of the positive anode or the negative cathode or, as is usually the case, at some intermediate potential; it also serves as the first, or beam forming, electrode of the extraction system. The next electrode, the extraction electrode, is an ion accelerating electrode. The third electrode is usually a decelerating electrode sufficiently positive with respect to the extraction electrode to prevent the acceleration of cold electrons (secondaries and free electrons from ionization of background gas) up to the potential of the plasma source. The remaining electrodes are used to further accelerate the ion beam to its final energy.

If A_g is the overall area of the gridded portion of one of the extraction system electrodes and τ_g is the transparency of the source grid, the ion-current density is

$$J_+ = I_+ / \tau_g A_g, \quad (V-10)$$

Figure V-4. Neutral-Beam Power Derived from D_j^+ , $j = 1, 2, 3$ vs. Ion-Beam Voltage for the Model of Fig. V-2 with $\Gamma_1 = 0.75$, $\Gamma_2 = 0.18$, $\Gamma_3 = 0.07$, $\tau_2 = 0.8$ and $\epsilon_{Rj} = 0.75$, $j = 1, 2, 3$

Figure V-5. Neutral-Beam Production Efficiency as Defined in Eq. V-8 vs. Ion-Beam Voltage for the Model of Fig. V-2 with $\Gamma_1 = 0.75$, $\Gamma_2 = 0.18$, $\Gamma_3 = 0.07$, $\tau_2 = 0.8$ and $\epsilon_{Rj} = 0.95$, $j = 1, 2, 3$ ($\tau_1 = 1.0$).

while the extracted current density is given by

$$J_E = I_+/A_g. \quad (V-11)$$

Once I_+ , the total positive ion beam current required to produce a given amount of neutral-beam power, has been calculated from Eq. (V-7), the required extraction electrode grid area, A_g , can be determined from Eq. (V-11) if J_E is given. A_g is plotted as a function of neutral-beam power for $J_E = 125 \text{ mA/cm}^2$ in Fig. V-6; curves are drawn for several beam voltages for one- and three-component neutral beams, with and without energy recovery.

It is apparent from the variations with beam voltage of I_+ and A_g shown in Fig. V-6 that there is an optimum beam voltage which minimizes the required source current and extraction grid area. The optimum voltage, subject to uncertainties in the f_{jo} values, is in the neighborhood of 100 kV for a one-component neutral beam and approximately 150 kV for a three-component beam.

In the neutral beam model of Fig. V-2, the first separator, if it is used, is after the staged accelerator which raises the ion-beam energy to its final value. This scheme was chosen in preference to one, shown in Fig. V-7, in which separation takes place between the source and the accelerator because it can reduce beam losses due to premature neutralization. As shown in Table V-3, a one meter flight path through a separator vacuum of 10^{-4} Torr will cause a beam loss of 23% due to premature neutralization at 20 keV but only 1% at 180 keV. This loss is in addition to the loss due to premature neutralization in the extraction region of the source; by proper design, this latter loss can be made independent of which of the two separator schemes is used.

TABLE V-3. D^+ and D^0 Fractions (F_1 and F_0) for Several D_2 Target Thicknesses and Beam Energies of 20 keV and 180 keV.^a

$P l^b$ (Torr cm)	$eV_+ = 20 \text{ keV}$			$eV_+ = 180 \text{ keV}$		
	F_1	F_0	$P l / 10^{-4} \text{ Torr}^c$ (cm)	F_1	F_0	$P l / 10^{-4} \text{ Torr}^c$ (cm)
0.001	0.97	0.03	10	0.999	0.001	10
0.01	0.77	0.23	100	0.99	0.01	100
0.10	0.14	0.86	1000	0.90	0.10	1000

^aRoom temperature target.

^bTarget thickness in Torr-cm

^cPath length for pressure of 10^{-4} Torr.

Figure V-6. Extraction Grid Area vs. Neutral-Beam Power for an Extracted Current Density of 125 mA/cm^2 , for the Model of Fig. V-2 with $\Gamma_1 = 0.75$, $\Gamma_2 = 0.18$, $\Gamma_3 = 0.07$, $\tau_2 = 0.8$ and $\epsilon_{Rj} = 0.95$, $j = 1, 2, 3$

V_s = Source Voltage

V_A = Accelerator Voltage

$V_s = V_s + V_A$

$\Gamma_j = D_j$ Fraction of I_s

$k_1 = P_{source} / I_s V_s$

$(k_F - 1) T_2 \Gamma_1 I_s V_A$ = Loading due to Currents Other Than $\Gamma_1 I_s$

T_2 = Transport Efficiency to and Through 1st Accelerator Grid

T_3 = Transport Efficiency from Accelerator to Reactor

ϵ_R = Reactor Capture Efficiency

f_{10} = Power Efficiency for $D^+ \rightarrow D^0$

Figure V-7 Neutral-Beam Production Model for $D_j^+ \rightarrow D^0$, $j = 1, 2, 3$, with Unwanted Ion-Beam Components Removed before Acceleration to Full Energy

4. Alternate Neutral-Beam Production Model for $D_j^+ \rightarrow D^0$

In the scheme shown in Fig. V-7, molecular ions are removed at low energy and only the D^+ beam is accelerated to full energy. Thus, beam loading on the accelerator is less than in the model of Fig. V-2. On the other hand, as noted above, premature neutralization in the separator region can result in a significant loss of beam, which puts an additional burden on the ion source and the neutral-injection vacuum system.

The loss term $(k_2 - 1) \Gamma_1 I_+ V_A$ in the model of Fig. V-7 results from loading due to currents other than the D^+ beam current. The other currents, as noted by Julian,⁽¹⁴⁾ include electron currents due to ionization of background gas and from secondary emission, gas currents due to ionization by x-rays of air or a pressurizing gas outside of the vacuum enclosure, and the current flowing through the potential divider.

This model has not been studied in as much detail as that in Fig. V-2, which has been chosen as the reference design model, but some calculations have been made and they show that the neutral-injection efficiencies ϵ_{NI} for this model fall in the region between the top two curves of Fig. V-5 if $\tau_2 = \tau_3 = 0.8$, $k_1 = 1.2$ and $k_2 = 1.1$ or 1.2 .

5. Neutral-Beam Production Model for $D^- \rightarrow D^0$

A model for the production of D^0 by stripping the electron from D^- obtained by direct extraction from a deuterium plasma is shown in Fig. V-8. The simplicity of this model results from the fact that molecular negative ions do not exist. The total D^- current required to produce a given amount of useful neutral beam power is

$$I_- = \frac{P_B / V_-}{\tau \epsilon_R f_{10}} \quad (V-12)$$

Over the ion beam voltage-range from 120 to 200 kV, $f_{10} = 0.62$. If $\tau = 0.8$ and $\epsilon_R = 0.95$, as in Table V-2, the D^- current required to obtain a 40 MW neutral beam is given by $I_- V_- = 84.9$ MW (for $P_B = 40$ MW) for the beam energy range from 120 keV to well beyond 200 keV. Over this range, the neutral-injector efficiency is $\epsilon_{NI} = 0.47 / (k - 0.26)$ and it goes from 0.60 to 0.50 as k varies from 1.05 to 1.2.

Figure V-8. Neutral-Beam Production Model for $D^- \rightarrow D^0$

6. Neutral Beam Production Model for $D_j^+ \rightarrow D^- \rightarrow D^0$

A simplified and compressed neutral beam production model for this case is shown in Fig. V-9. The D^- source consists of a positive deuterium ion source followed by a D_2 gas cell which serves as a space charge neutralizer and, also, as a buffer between the positive ion source and an alkali-metal vapor-cell which receives the mixture of positive ions, negative ions and neutrals emerging from the gas cell. The alkali metal will most likely be cesium, for which the power efficiency f_{11} is ~ 0.2 at a D^+ energy of ~ 1.5 keV, or sodium, for which the efficiency is ~ 0.12 at an energy of ~ 5 keV, according to Gruebler et al.⁽¹⁵⁾ For the model of Fig. V-9, the accelerated D^- power is $\tau I_- V_-$ and the total positive-ion current is given by $I_+ = I_- / f_{11} \Gamma_1$. For $\Gamma_1 = 0.75$ and for a cesium cell, with $f_{11} = 0.2$, $I_+ = 6.67 I_-$.

Source requirements for a 40 MW neutral beam for the four models which have been considered are summarized in Table V-4. The most efficient process, $D^- \rightarrow D^0$ is ruled out for the time being because a suitable direct extraction D^- source does not presently exist. The process $D^+ \rightarrow D^- \rightarrow D^0$ may be ruled out for TEPR not only because a suitable charge exchange source does not presently exist but also because of the very large positive-ion current requirements. Of the two remaining models, that of Fig. V-2, with all components of the ion beam used to produce neutrals, emerges as the one which makes the most modest demands on the ion source and the neutral-injector vacuum system (see Section V-A-8).

7. Plasma Penetration

For the beam energies considered in this report ($U_B \geq 80$ keV), charge exchange accounts for less than 5% of the fast ion production; the remaining 95% of the fast ions being produced by ion impact and, to a minor extent, electron impact phenomena.^(16,17) The ion impact cross sections are functions of the nuclear charge of the ion with which the neutral particle collides, the cross-section scaling as Z^2 with respect to the proton-impact cross sections.⁽¹⁶⁾ Over the range of neutral beam energies of interest, the total "ionization" cross section is essentially independent of plasma temperature.

V_s = Source Voltage
 V_A = Accelerator Voltage
 $V_- = V_s + V_A$
 Γ_1 = D^+ Fraction of I_+
 $k_1 = P_{\text{source}} / I_+ V_s$

$(k_2 - 1) T_1 I_- V_A$ = Loading due
 to Currents Other Than I_-
 f_{IT} = Power Efficiency for $D_j^+ \rightarrow D^-$
 f_{T0} = Power Efficiency for $D^- \rightarrow D^0$

T_1 = Transport Efficiency to and Through 1st Accelerator Grid
 T_2 = Transport Efficiency from Accelerator to Reactor
 ϵ_R = Reactor Capture Efficiency

Figure V-9. Neutral-Beam Production Model for $D_j^+ \rightarrow D^- \rightarrow D^0$

Table V-4. Ion Source Requirements for a 40 MW D^0 Beam for the Models Listed in the Footnotes

$D^- \rightarrow D^0$ (a)			$D_j^+ \rightarrow D^- \rightarrow D^0$ (b)			$D_j^+ \rightarrow D^0$ (c)		$D_j^+ \rightarrow D^0$ (d)	
V_+	I_-	P_-	I_-	$\tau_1 I_- V_-$	I_+	I_+	P_+	I_+	$\tau_2 \Gamma_1 I_+ V_+$
(kV)	(A)	(MW)	(A)	(MW)	(A)	(A)	(MW)	(A)	(MW)
120	708	84.9	884	84.9	5900	830	100	1700	122
160	531	84.9	663	84.9	4420	880	140	1890	182
180	472	84.9	590	84.9	3930	890	160	2030	219
200	425	84.9	531	84.9	3540	900	180	2190	263

^a Model of Fig. V-8 with $\tau = 0.8$, $\epsilon_R = 0.95$, $f_{10}^- = 0.62$

^b Model of Fig. V-9 with $\tau_1 = \tau_2 = 0.8$, $\epsilon_R = 0.95$, $f_{11}^- = 0.2$, $f_{10}^- = 0.62$, $\Gamma_1 = 0.75$

^c Model of Fig. V-2 with $\xi_j = 1$, $j = 1, 2, 3$ and other parameters as listed in Table V-2.

^d Model of Fig. V-7 with $\tau_2 = \tau_3 = 0.8$, $\epsilon_R = 0.95$, $\Gamma_1 = 0.75$, $\Gamma_2 = 0.18$ and $\Gamma_3 = 0.07$

The mean free path, λ (in meters), for the $1/e$ attenuation of a neutral beam is given by $\lambda = 1/(\sum_j n_j \sigma_j)$ where n_j is the ion density of species in the plasma and σ_j is the total cross section for the production of fast ions by charge exchange, ion and electron impact collisions. Since almost all the fast ions are produced by ion impact, the resulting expression for the mean free path for fast ion production with a neutral deuteron beam is given by $\lambda = (2.75 \times 10^{17} U_B)/n_e Z_{\text{eff}}$ where Sweetman's expression for the total "ionization" cross section has been used,⁽¹⁷⁾ n_e is the electron density (in m^{-3}) and $Z_{\text{eff}} = \sum_j n_j Z_j^2/n_e$ is the mean effective charge of the plasma ions and U_B is the deuteron energy in keV. This expression is valid for deuteron beams in the range $40 \leq U_B \leq 200$ keV.^(17,18)

The fast-ion density distribution will have its maximum in the center of the plasma for a plasma of radius a when $\lambda \geq a/4$.^(18,19) This criterion is probably valid for perpendicular as well as tangential injection.^(18,19) Thus, in the upper ranges of neutral-beam energies considered, $U_B \geq 150$ keV, the first two components of a three-component neutral beam will satisfy this criterion for the relatively clean ($Z_{\text{eff}} = 1.3$) plasmas considered in Chapter III. The third component of the beam will tend to deposit its energy towards the outer region of the plasma in this case.⁽¹⁹⁾ However, as seen from Fig. V-4, this component carries less than 15% of the total neutral-beam power, so that the net effect should be a deposition profile peaked towards the center of the plasma. For the remainder of the range of beam energies considered, $U_B < 150$ keV, only the highest energy beam component satisfies this criterion. The two lower energy beam components will have deposition profiles flattened and/or peaked towards the outer plasma regions. Since, from Fig. V-4, it is seen that these two components carry from 30 to 37% of the total beam power for this energy range, the net effect should still be a deposition profile peaked towards the center of the plasma or one which is fairly uniform as a function of plasma radius.

A uniform buildup of impurity concentration in the plasma would necessitate higher beam energies to maintain an energy deposition profile peaked at the center of the plasma. If the impurity ion concentration is largest near the plasma edge, the higher Z_{eff} in these regions would tend to attenuate the beam more quickly and thus flatten or peak the deposition profile towards the outer edge of the plasma.

Deposition of a large fraction of the beam towards the outer edges of the plasma, as in the case of the lower energy components of the three-component beams or in the case of impurity buildup, will effectively result in decreased beam heating of the plasma. In addition, in order to avoid transfer of a significant amount of toroidal momentum to the plasma, the neutral beam will probably have to be counterinjected as well as coinjected. In such a case, more of the counterinjected beam will be lost to the limiter than the coinjected beam, due to pitch-angle scattering of the fast ions produced into unconfined orbits.^(19,20) In ORMAK, with $\lambda = a/4$, 99% of the coinjected fast ions are retained in the plasma, while 74% of the counterinjected fast ions are retained.⁽¹⁹⁾ However, increasing the plasma current greatly improves the fast ion retention.^(20,21) The banana widths of the orbits which result in fast-ion loss are proportional to the square root of the particle energy; and, since a large fraction of 3.5 MeV alpha particles are retained in the plasma by a plasma current of 3MA or more,⁽²²⁾ a capture efficiency, ϵ_{Rj} , of 95% is a reasonable estimate for the injected ions.

8. Neutral Beam Injector Gas Load

Since there are $6.24 \times 10^{18} D_j^+$ ions/A sec, $j/2$ molecules per D_j^+ ion and $1/3.29 \times 10^{19}$ Torr-liter/molecule at 20°C, the D_2 gas equivalent of the D_j^+ fraction of a deuterium-ion beam is $0.095 j$ Torr-liter/A sec. The gas equivalent of the entire beam, I , is

$$Q = 0.0950 I \sum_{j=1}^3 j \Gamma_j \quad \text{Torr-l/sec.} \quad (V-13)$$

We shall, for the moment, assume that all of the ion current required to produce the neutral beam for TEPR comes from a single, rather large, ion source. As a rough approximation, it will be assumed that Q_{in} , the gas flow into this source, is equal to the gas equivalent of

$$\begin{aligned} I_{total} &= I_g + I_+ \\ &= I_g + \tau_g I_{total} \end{aligned} \quad (V-14)$$

where I_g is the ion current intercepted by the grid closest to the plasma and I_+ , the current extracted through the grid, is given by Eq. (V-7). τ_g is the previously defined grid transparency.

To the extent that this rough approximation is valid, the gas flow into the source (actually, the sum of all sources) is

$$Q_{in} = 0.095 (I_g + I_+) \sum_{j=1}^3 j \Gamma_j \quad \text{Torr-l/sec.} \quad (V-15)$$

while the gas flow out, i.e., the injector vacuum system gas load, is

$$Q_{out} = 0.095 I_g \sum_{j=1}^3 j \Gamma_j \quad \text{Torr-l/sec.} \quad (V-16)$$

Since $I_{total} = I_+/\tau_g$, these may be written in the forms

$$Q_{in} = 0.095 I_+/\tau_g \sum_{j=1}^3 j \Gamma_j \quad (V-17)$$

and

$$Q_{out} = 0.095 \left(\frac{1-\tau_g}{\tau_g} \right) I_+ \sum_{j=1}^3 j \Gamma_j \quad (V-18)$$

For a steady-state source, τ_g will probably lie between 0.25 and 0.40. For these values, the ranges of the inflows and outflows to be expected for the 180 kV Reference Design of Table V-1 are shown in Table V-5. Since the neutral injection system will be subdivided into $N = 16$ or 32 separate injectors, values of I_+ , Q_{in} and Q_{out} appearing in Table V-5 should be divided by N to obtain the currents and gas flows per source.

Table V-5. Total Gas Inflows and Outflows for the 180 kV Reference Design of Table V-1

τ_g	P_B (MW)	Three-Component Beam			One-Component Beam		
		I_+ (A)	Q_{in} (Torr-l/sec)	Q_{out} (Torr-l/sec)	I_+ (A)	Q_{in} (Torr-l/sec)	Q_{out} (Torr-l/sec)
0.25	40	890	445	334	1600	800	600
	23	510	255	191	920	460	345
0.40	40	890	278	167	1600	500	300
	23	510	159	96	920	280	173

9. Technological Basis for Neutral-Injector Design

The basic elements of a high-energy, neutral-injection system for a fusion reactor are: a gas source; a deuterium plasma source; a set of extraction electrodes; a set of accelerating electrodes; a neutralizing cell; and auxiliaries such as electrical power supplies, a vacuum system and a control system. To these may be added magnetic and/or electrostatic deflecting and/or focusing elements.

The deuterium plasma is characterized by an ion density of the order $5 \times 10^{12} \text{ cm}^{-3}$, an electron temperature of the order of 10 eV and an ion temperature of the order of 1 eV. Ideally, the plasma is quiescent and spatially uniform, with temporal and spatial variations $\delta n/n < \pm 1\%$; in practice the variations may be an order of magnitude higher.

An ion current density of $\sim 0.5 \text{ A/cm}^2$ is currently obtained from the positive ion sources used in neutral injectors for CTR devices. To obtain tens of amperes of beam current, it is necessary to have large-area extraction systems and this has led to the use in CTR sources of large multi-aperture extraction grids similar to those first developed for electron-bombardment ion thrusters.^(23,24) It has also led to the development of plasma sources capable of producing reasonably uniform plasmas to illuminate these grids.

Virtually all of the positive ion sources presently in use in neutral-beam injectors for CTR devices are positive ion sources of the types which have been developed at Oak Ridge National Laboratory, Lawrence Livermore Laboratory and Lawrence Berkeley Laboratory. The Livermore MATS (Multiple Aperture Test Source) sources⁽²⁵⁾ and the Oak Ridge DuoPIGatron⁽²⁶⁾ are modifications of a Russian source⁽²⁷⁾ in which a fourth electrode was added to von Ardenne's duoplasmatron⁽²⁸⁾ to convert the discharge from a drifting-arc type, similar to that in the Lamb-Lofgren source,⁽²⁹⁾ to a PIG (Penning Ion Gauge) type. In a magnetically-confined, drifting-arc discharge, thermionically-emitted primary electrons make a single transit of the discharge; in a PIG discharge, energetic primary electrons are trapped in a potential well and oscillate back and forth a number of times along a magnetic field which may or may not be uniform before reaching the anode by diffusion across the magnetic field. They can, thus, give up more of their energy in multiple-ionizing collisions with gas molecules and are more efficiently used.

Unlike the Oak Ridge and Livermore sources, the Russian 4-electrode source did not use multiaperture grids to obtain the high electric field gradients required to extract high density beams but used, instead, a low-gradient gap between two cylinders. There has been a tremendous scale-up in size in going from an initial beam diameter of ~ 4 mm in von Ardenne's source to the 10 cm diameter of the latest version of the Oak Ridge source.

The Berkeley source uses a plasma generated by a "diffuse low-pressure, high-current discharge from distributed thermionically emitting cathodes;" there are no externally applied magnetic fields.⁽³⁰⁾ The Berkeley source is normally operated with deuterium gas while the Livermore and Oak Ridge sources usually run on hydrogen.

The neutral injector design for TEPR will be based on the assumption that the ion sources will be of the Oak Ridge and/or Berkeley types, with comparatively conservative, but not trivial, extrapolations to high energy and essentially steady state operation.

For TEPR, pulse lengths ranging from several seconds to about one minute and ion beam voltages of 150 to 200 kV will be required. These requirements may be compared with the present capabilities of the Berkeley and Oak Ridge ion sources.

The Berkeley "50 Ampere" source, with 105 0.2 cm x 7 cm slots in a 7 cm x 35 cm extraction electrode array produces ~ 75 A of 20 keV deuterium ions in a 10 msec pulse, once per minute.⁽³¹⁾ The total current (ion plus neutral) to a 20 cm x 40 cm calorimeter at 2.9 m ($\pm 2^\circ$ x $\pm 4^\circ$) is 67 A, while that to a 10 cm x 20 cm target ($\pm 1^\circ$ x $\pm 2^\circ$) is 47 A. J_+ , the beam current density (total current/total slot area) is 510 mA/cm². J_E , the extracted current density (total current / total extractor area) is 306 mA/cm². Gas flow is 21 Torr-l/sec; filament power is 25 kW; arc power, 165 kW; accel power 1.6 MW; and decel power is 13 kW. The total source power, 1.803 MW, is 1.2 times the total beam power of 1.5 MW; 90 percent of the extracted beam is collected on the $\pm 2^\circ$ x $\pm 4^\circ$ target.

The Berkeley source is not cooled; if cooling is added to permit steady-state operation, the extracted current density will be reduced by perhaps a factor of 2 or 3.

The Oak Ridge 7-cm diameter source with an extraction array of 209 3.8-mm diameter holes can produce a 6A, 40 keV hydrogen ion beam for 300 m sec on a 15% duty cycle.⁽³²⁾ About 70 percent of the beam (ion plus neutral) is deposited on a 10-cm diameter target 125 cm from the source ($\pm 2.3^\circ$). The beam current density (total beam current/total aperture area) is 250 mA/cm². The extracted current density (total current/total extractor area) is 160 mA/cm². Filament power is ≤ 90 watts; arc power ≈ 7 kW; magnet power ≤ 300 watts (estimated); accel power is 240 kw; and decel power ≈ 1 kW. The total source power of 248 kW is ~ 1.1 times the total beam power of ~ 230 kW.

The Oak Ridge source is water cooled and is believed to be capable of essentially dc operation, a premise to be tested when suitable dc supplies become available. A direct scale-up of the 7-cm diameter source with somewhat better cooling is expected to result in a 50 kV, 50 A, essentially dc source for Ormak II. The extractor is expected to have a 21-cm diameter, with a total area of 346 cm². The extracted current density of such a source would be ~ 144 mA/cm².

At the present time, the Berkeley and Oak Ridge sources are in the forefront of the advance toward the type of source required for TEPR. Thus they, along with relatively well established vacuum, power and beam handling (deflecting, focusing) technologies form the technological basis for a neutral injector design.

The neutral-beam development program is currently focused upon the needs of the TFTR⁽¹⁰⁾. If the TFTR requirements can be met on schedule, then the additional requirements of the TEPR should be achievable.

10. Preliminary Design Considerations

For the neutral-beam production model of Fig. V-2, once the values of τ_2 , ϵ_{Rj} , ξ_j and Γ_j are specified, the total ion-beam current and power required to produce a given amount of neutral-beam power, P_B , are given by Eq. (V-9), which can be put in the form

$$I_+ = \frac{P_B/V_+}{3 \sum_1 \epsilon_{Rj} \xi_j f_{jo} \Gamma_j} \quad A \quad (V-19)$$

where V_+ is expressed in MV if P_B is given in MW. If k and τ_1 are known, the

injector efficiency can be calculated from

$$\epsilon_{NI} = P_B / (k P_+ - P_R) \quad (V-20)$$

where the terms entering into P_R , the total power recovered, can be read off of Fig. V-2. The required extraction grid area (total) can be calculated if the extracted-current-density capability of the source is known.

τ_2 , the transport efficiency from the exit grid of the accelerator to the entrance window in the 1st wall of the reactor, is the only geometry-dependent parameter in Eq. (V-19) for I_+ and P_+ . The design initially selected for the neutral-beam injection ports is described in the next section. It calls for an entrance window of 0.85 m x 0.85 m.

This window subtends an angle $\theta_{max} = 0.425/L$ at a distance of L meters. If all of the ion-beam components are injected into the neutralizer, and the beam emerging from the exit grid of the accelerator is diverging, L is the distance from the exit grid of the accelerator to the entrance window in the reactor first wall; if the beam approaching the window is initially converging, goes through a waist and then diverges again, L is the distance from the waist.

Stewart and his colleagues at Oak Ridge⁽¹³⁾ have used programmed aperture displacement to steer the individual beamlets emerging from their source toward a common focal point. By examining the beam profiles obtained with this source and by studying the single-aperture profiles which comprise the total profile, they have found that the total profile can be represented accurately by a gaussian with a half-width given by an equation of the form

$$\theta_{1/2} = L (\theta_S + \theta_A + \sqrt{T_i/E}) \quad (V-21)$$

where L is the distance from the source exit to the target, θ_A is the single-aperture gaussian half-angle divergence remaining after account is taken of the finite source-plasma ion temperature, θ_S is an additional gaussian half-angle divergence due to imperfect focussing or superposition of the single-aperture beamlets, T_i is the ion temperature and E is the energy of the beam ions emerging from the source. For the Ormak injection units, $T_i = 0.5$ eV, $\theta_A = 0.4^\circ$ and $\theta_S = 0.6^\circ$. If I_T is the total current, ion and neutral, in the beam approaching the target, the total current within radius r on the target is⁽¹³⁾

$$I = I_T \{1 - \exp [-\frac{1}{2} (r/r_{\frac{1}{2}})^2]\}$$

(V-22)

or

$$I = I_T \{1 - \exp [-\frac{1}{2} (\theta/\theta_{\frac{1}{2}})^2]\}$$

where

$$\theta_{\frac{1}{2}} = \theta_S + \theta_A + \sqrt{T_1/E} .$$

If

$$\theta = \frac{r}{L} = 2 \theta_{\frac{1}{2}} ,$$

(V-23)

where r is the radius of a circular target, 86.5% of the beam reaches the target. If r is the radius of a circle inscribed in the square entrance window, the fraction of the beam entering the plasma will be greater than 86.5%. $\theta_{\frac{1}{2}}$ for a TEPR source will be determined primarily by θ_S , the half-angle divergence due to imperfect focus, i.e., lens aberrations, which cannot be predicted without detailed knowledge of the extraction and accelerating electrode geometries.

Some limits can be placed on the values of $\theta_{\frac{1}{2}}$ which will allow a specified fraction of the beam to reach the reactor. These are given in Table V-6 as functions of L for the case given in Eq. (V-23), with $r = 0.425$ m.

Table V-6. $\theta = \frac{r}{L}$ and $\theta_{\frac{1}{2}} = \frac{r}{2L}$, the Value of the Gaussian Angular Half-Width Which Gives > 86.5% of the Beam on Target, for $r = 0.425$ m

L (m)	θ (deg)	$\theta_{\frac{1}{2}}$ (deg)
3	8.12	4.06
4	6.09	3.04
5	4.87	2.44
6	4.06	2.03
7	3.48	1.74

11. Engineering Description

Sixteen or thirty-two injectors are currently being considered. An engineering design with thirty-two neutral beam injectors arranged on the periphery of the reactor is shown in Figs. II-4, II-5 and V-10. Eight clusters of four injectors are positioned such that the beams focus on the plasma horizontal mid plane and slightly inward of the plasma vertical mid-plane. Each aperture to the vacuum chamber accommodates two injector-beam ducts entering into a transition fitting from opposite (120°) directions forming a V around a toroidal field magnet. A total of sixteen apertures to the vacuum chamber wall are provided.

The design initially selected meets the major criteria of 32 beams of 85×85 cm cross section to the plasma and counter directional injection at near to plasma centerline tangency. The ducting is square in cross section and contains a quick acting flap-type valve for isolation and vacuum seal and extends approximately 2.2 meters from the toroidal chamber into an open area between the toroidal field magnets. Space provided for the neutral-beam injection equipment from this point is a volume 5.5 m long by approximately 3 m square.

Consideration has been given in the design for magnet shielding to compensate for duct voids in the blanket shield, minimum duct length and remote maintenance and repair.

The ducting is made of stainless steel and assembled and disassembled remotely. The design selected here has a great deal of flexibility to vary several geometric parameters as the basic needs may require. The ports themselves offer access to the plasma chamber for inspection and repair. Alternate injector port designs are under study currently.

B. Vacuum Systems

1. Toroidal Vacuum System

The torus or plasma containment vessel has 711 m^3 of volume (897 m^3 with the volume of the exposed pumping and injector duct) and a surface area of 592 m^2 (1598 m^2 with the addition of the duct surface area). The pumping system for the torus and the included ducts must be able to reduce the pressure after the "burn" or fusion cycle from 10^{-3} Torr to 10^{-5} Torr in ~ 10 to 15 sec. Furthermore, before each experimental period, the pumping system must be

Figure V-10. Multiple-Beam Port Engineering Design

capable of evacuating the entire volume down into the 10^{-9} Torr region to assure a reasonably contamination-free plasma. Sizing for the proper pumping capacity is dependent, therefore, upon which of the two conditions will produce the greater gasload per unit of time.

The first condition was determined by the equation:

$$S = \frac{2.303 (V) \left(\log_{10} \frac{P_1}{P_2} \right)}{t} \quad (V-24)$$

where V = volume of torus (including duct volume) = 897,000 liters

$$P_1 = 10^{-3} \text{ Torr}$$

$$P_2 = 10^{-5} \text{ Torr}$$

$$t = 10 \text{ sec}$$

The equation is considered valid, since the pressures P_1 and P_2 are well above 10^{-8} to 10^{-9} Torr, the base pressure of the system. Further assumptions include no leaks and a pump of constant capacity. Under these conditions, the pumping speed required to reduce the pressure from 10^{-3} Torr to 10^{-5} Torr in 10 sec is 413,000 liters/sec.

To assure effective symmetrical pumping, ports are being considered at both the top and bottom of the torus and between each magnet (See Figs. II-4, II-5). This makes available 32 pumping ducts, each approximately 6 m long. Designing with the ability to house the pump directly within the duct and giving consideration to both the entrance impedance and the impedance of the duct length itself, it appears that thirty-two (32) 25,000 liters/sec pumps will give the required effective pumping speed to evacuate the torus from 10^{-3} Torr to 10^{-5} Torr in less than 15 sec.

The second condition for determining the proper pump capacity is the speed required to reach the desired base pressure of 10^{-9} Torr before each experimental run. The necessary speed is based upon the material exposed to the vacuum within the torus and its outgassing rate. The outgassing rate is very dependent upon any preconditioning and upon its temperature at the time of evacuation. Table V-7 shows the outgassing rates for stainless steel.⁽³³⁾ It is not clear that stainless steel will be utilized as the liner in the torus, but the table points out that a bake-out cycle does have a big advantage. After a bake-out cycle, the above pumping

system will have no problem in reaching a base pressure of 1×10^{-9} Torr at room temperature. Operating at elevated temperatures and with energetic ions can make a significant difference. At 750°C , steel has a vapor pressure of 1×10^{-8} mmHg⁽³⁴⁾ and at 1000°C , the vapor pressure is 3×10^{-1} mmHg.⁽³⁴⁾ CERN,⁽³⁵⁾ with their experience on the intersecting storage rings, has observed that in the absence of beams, the residual gas pressure is very low ($\approx 1 \times 10^{-10}$ Torr). Also, the gas composition is that of a clean, well-baked and leak-tight ultrahigh vacuum system that is 90 to 95% of hydrogen with traces of water vapor, carbon monoxide, and hydrocarbons. This observation is from the surfaces of stainless steel that had been annealed in a vacuum oven at about 900°C for two hours, before fabrication into chamber form, to reduce the hydrogen content in the steel. Subsequently, the chambers made of this steel were later baked at 200°C for about five hours to remove surface contamination. When beam was injected into the ring, it induced a pressure rise. An analysis of the gas composition during a pressure rise found typically 40% hydrogen, 40% CO, and 20% water vapor and hydrocarbons. It was established that ions produced in the beam by collision of the high energy

TABLE V-7. Recommended Values of Outgassing Rates⁽³³⁾
(Torr-liters/cm²·sec)

Material	Outgassing Rates, Unbaked				Outgassing Rate After Bake-Out	Remarks
	1 hour	4 hours	10 hours	40 hours		
Stainless steel only degreased	2×10^{-9}	1×10^{-9}	2.5×10^{-10}	8×10^{-11}	4×10^{-12}	25-hour bake at 300°C

protons with the residual gas molecules are accelerated by the potential of the beam towards the chamber wall. The ions bombard the chamber wall with ≈ 300 eV energy and knock-off molecules which had been adsorbed with binding energies which are sufficiently high that the ultimate pressure without a beam is not affected. CERN's strategy to combat this was to make (1) the gas desorption factor small (24-hour bake at 300°C), (2) the pumping speed big, and (3) the distance between pumps small. We must decide also to make use of this strategy.

Graphite has been suggested as a protective liner or coating over the stainless steel. The problem with ordinary graphite is the large internal-surface area. The gases released upon heating are CO_2 , CO , N_2 , and H_2 , and complete outgassing is obtained only at temperatures above 1700°C .⁽³⁶⁾ Pyrolytic graphite,⁽³⁷⁾ a very flame-resistance material that will withstand temperatures to $\sim 3700^\circ\text{C}$ has been developed by the General Electric Co. Sheets as thin as 0.001 in. are impervious to liquids or gases. A relatively new material is vitreous carbon,⁽³⁶⁾ which is prepared by thermal decomposition of organic polymers. Its density is relatively low because of a pore volume of about 30%. The pores, however, are of spherical shape and not accessible to gases. The material has a very low permeability, even for helium. The purity and ash content (<50 ppm) are superior to ordinary graphite.

There are many factors to consider in the choice of high vacuum pumps for the toroidal vacuum system. Cryosorption pumps are being considered because they reduce the space problem that diffusion pumps present and the more difficult tritium recovery and maintenance necessary with ion pumps and sublimation pumps. Cryosorption pumps have the ability to generate an inherently "clean" vacuum, which is not contaminated by oil vapors or other impurities, and reliable operation not affected by power failures. Moreover, cryosorption pumps can vary in configuration so that they may be attached to vacuum systems at almost any point. According to manufacturer's data,⁽³⁸⁾ special pumps can be quite easily constructed by simple scale-up to almost any imaginable size to achieve desired pumping speeds and mass throughput rates. Another point of consideration is the fact that the necessary cryogen for the cryosorption pumps (i.e., liquid helium and liquid nitrogen) will be readily available, since it must be supplied for the superconducting magnets.

Pumping speeds of the required cryosorption pumps are based on a report by P. J. Gareis and S. A. Stern.⁽³⁹⁾ Table V-8 lists the results of their experience with two cryosorption pumps of different sizes. Although not reflected in the table, the outer shield and two chevron baffles were continuously refrigerated with liquid nitrogen to minimize the liquid helium consumption (See Fig. V-11).

TABLE V-8

PERFORMANCE CHARACTERISTICS OF CRYOSORPTION PUMPS (39)

Factors	SHe-8	SHe-25
Cryosorption Panel area, ft. ² (cm ²)	0.27 (250)	3.4 (3,150)
Pumping Speed, l/sec.		
Hydrogen	1,000	17,000
Helium	600	7,500
Steady State Liquid Helium Boiloff, liquid liters/day	1.0	6.6

One of the major disadvantages to cryosorption pumps is the periodic need for regeneration because of their limited storage capacity. A 25,000 liter/sec pump has a storage capacity of approximately 90,000 Torr-liters. With a torus volume of 897 m³ and 32 pumps, regeneration would be required after ~3200 "burn" cycles. Of course, if downtime is to be avoided during regeneration, the number of pumps could be doubled (See Figs. II-4, II-5). With suitable valving, half the pumps could be regenerated while the other half are in the operating mode. If a suitable valve can be found to isolate the ducts during the "burn" cycle and thus confine it to the torus (see Figs. II-2, II-3) the volume to be exhausted would be reduced by 25% and the surface area by 60%. Such valves would have to exist in a harsh environment, be completely metallic, have reasonable sealing ability and closure time, and be extremely reliable.

The pumping speeds of the cryosorption pumps are based on their ability to pump hydrogen. A question is whether this ability to pump hydrogen necessarily establishes the ability of these pumps to pump deuterium and tritium as well. Tests to establish this ability would be appropriate.

Fig. V-11. Schematic diagram of SHe-25 cryosorption pump (39)

Speeds for our requirements are based on the results from the SHe-25 cryosorption pump which was 25 in. (63 cm) in diameter (see Fig. V-12)

Fig. V-12. Pumping speed of the SHe-25 cryosorption array as a function of pressure for hydrogen and helium. (39)

Paralleling these tests would be other experiments to establish cryosorption pump regeneration techniques, as well as closed-system pumping and handling of tritium and deuterium. It also will be important to know, to some reasonable accuracy, the cryogen requirements to cool down the pumps to operating temperatures and to maintain them at these temperatures. Knowledge of storage capacity will be equally important in order to establish and schedule regeneration periods.

Roughing pumps are necessary to bring chamber pressures down low enough to prevent "loading" down the cryosorption pumps and minimizing cryogen requirements. For fusion reactor operation, tritium containment is a must consideration. The mechanical pumps, sized for the TEPR secondary pumping system, are not available as "canned" pumps; with seals, shafts, and motors canned to avoid leakage. There are, however, smaller sizes available, which means the technology is available and when the demand becomes apparent, required sizes of "canned" pumps will undoubtedly appear on the market.

Figure V-13 is a pumpdown and degassing cycle scheme for the toroidal vacuum chamber. The 16 mechanical pump units, each consisting of 16 rotary piston pumps, 16 Roots blowers, and 16 turbomolecular pumps are selected for flexibility and multipurpose pumping. Since all exhaust must be contained, the same system can be used for pumpdown, bake-out, holding, and backup for regeneration of cryosorption pumps.

Figures II-2, II-4 represent one arrangement for the ports, ducts, cryopumps, and related equipment. Several alternative locations are under study. All components have been designed with consideration for minimum duct length, shielding of both magnets and cryopumps, remote maintenance and replacement. Other aspects of vacuum ports and equipment under consideration are the access they provide for assembly, inspection and repair of the first wall, and the availability to perform experiments such as testing of first wall and blanket arrangements in the area of the butterfly valve.

The major vacuum ducting components are described in detail as follows:

(a) Vacuum Port Ducting. The ducting as shown in Figs. II-2, II-4 is comprised of two Y sections. This geometry exhibits the maximum flexibility for remote handling while meeting all major functional requirements. The inner Y duct supports the inner-shield plug assembly vertically while the Y arm aims obliquely outward through the volume bounded by the equilibrium

Fig. V-13. Tokamak (Torroidal Vacuum Chamber) Pumpdown and Degas Cycle

coils and adjacent toroidal field coils. This duct is made of stainless steel and sealed metalically to the toroidal chamber port outer regions of the duct with bolting vertically accessible from the inside. These joint seals may be reached by removal of the shield plugs.

The outer Y duct is symmetrically shaped carrying the access opening from the plasma chamber to either of the two cryosorption pumps. This component is also bolted and sealed to the inner duct. Assembly of this unit is made through the cryopump ports. Both Y sections are removed vertically.

(b) First-Wall Valving. A quick-operating, thin-member butterfly valve with the inner (plasma) side lined with the first wall material is provided as near as possible to the toroidal chamber. The valve will have a high resistance seal and can shut against a stainless steel seat. Both seat, butterfly valve, and valve pivot bearings are supported in position on a cylindrical sleeve carrier fastened to the inner Y duct. The valve operating linkage extends upward through the shield where it is attached to a pneumatic operator accessible outside the shield. Provisions are included for cooling the valve surface. The entire valve, seat, cooling leads and operating mechanism are easily removable vertically as a unit.

(c) Heat Exchanger. A heat exchanger is provided in the oblique arm of the inner Y duct to cool the gas emanating from the chamber in order to reduce the load on the cryopumps. This exchanger is mounted on supports extending into the outer Y duct and is accessible by removing the cryopumps. Coolant lead lines are projected through the outer duct.

(d) Shielding. Vacuum port shielding consists of an inner shield of irregular shape, which forms a contour passage to the vacuum pumps. This shield is supported within the inner duct from a vertical mounting. This shield is followed by a 1-meter thick shield in the form of a right circular cylinder, which caps the vertical run of radiation streaming from the vertical port. A third shield surrounds the inner "Y" duct preventing excessive heating of the magnets from scattered radiation. All port shielding is cooled and remotely removable.

(e) Cryosorption-Pump Isolation Valves. Gate-type valving to be used for isolating or regenerating the cryopumps has been provided. These valves will be located at the pump inlet and will be integral with the pump support structure. A flexible bellows will be used to join the valve with

the outer Y duct, allowing the valve, cryosorption pump, and regenerative/ ancillary equipment to be mounted on a separate base.

2. Neutral-Beam Vacuum System

The pumping requirement for the neutral-beam injectors will be much more severe than for the toroidal vacuum system. The severity will depend upon the beam power selected (see Table V-5). Figure V-14 is a plot of the beam power versus the pumping surface, in square meters, and pumping speed, in liters per second, required for each injector under various conditions as indicated. Surface area requirements and speeds are based on the experimental data from the SHe-25⁽³⁹⁾ cryosorption pump (see Table V-8). Because of the large gas loads involved in this system, any impedance of gas flow toward the pumps must be avoided. Cryosorption panels appear most suited for this, since they can be installed within the beam housing and along its path, or it is conceivable that the housing itself can be constructed from the panels themselves.

As indicated elsewhere in this report, periodic regeneration of the cryosorption panels will be necessary. Regenerating these panels will disrupt operation until regeneration is completed and the system returned to operating pressure. Doubling the number of beam injectors and pumping systems would be one solution. The intended use, however, of 32 beam injectors already poses the question of adequate space, let alone finding space for 64. A more practical solution may be an ingenious method of providing adequate surface within the beam housing so that part of it can be isolated and regenerated while the other part is exposed and pumping.

The secondary pumping system discussed above for the toroidal vacuum system will be used in the same capacity for the neutral-beam vacuum system. If suitable "canned" mechanical pumps are not available, the necessary backup for the primary pumps will have to be done with large molecular-sieve pumps that will require periodic regeneration, such as will be done with the cryosorption pumps. It is also conceivable that conventional mechanical pumps could be used, if arranged in a separate tunnel system. This tunnel system would confine any leakage from faulty seals and other sources of leaks. A continuous flow of air through the tunnel would be exhausted through the tritium recovery system.

Fig. V-14. Neutral Beam Power versus Pumping Surface

Questions posed and experimental programs suggested for the toroidal pumping system, to gain knowledge regarding the behavior of the cryosorption pumps, tritium containment, and tritium recovery, are identical for the neutral-beam vacuum system.

C. Power Supplies

1. Ohmic-Heating Coil Power Supply

The current in the ohmic-heating coil reverses twice each fusion reactor cycle. As a result, the total stored energy of the magnet is transferred into and out of the power supply twice each cycle. Since the stored energy of the ohmic-heating coil approaches 10^9 J, the energy storage system is a dominant feature of the ohmic-heating coil power supply. Clearly, economy and efficiency of the energy transfer are major design considerations.

The ohmic-heating coil, which is described in Chapter IV, has an inductance of 1.1 H and an operating current that varies sinusoidally between +40,000 and -40,000 A at 1/2 cycle per second before and after the burn cycle (see Fig. IV-21).

Conventional energy-storage systems such as capacitors or motor-generator sets tend to be too costly and inefficient. Less conventional energy-storage systems such as homopolar generators⁽⁴⁰⁾ or superconducting magnetic-energy storage and transfer systems⁽⁴¹⁾ require further investigation into their applicability. Also, since these devices are not commercially available, cost and efficiency estimates are not particularly accurate.

Consider, as an example, the homopolar generator as a rotating capacitor. The simple geometry of the rotor structure allows full acceleration and deceleration of the rotating mass in periods of one second.⁽⁴²⁾ They can be used as rotating capacitors that are required to rotate only during the two energy transfer periods. During the bulk of the reactor cycle, the rotors can remain stationary. Wind and friction losses would be relatively small compared to a conventional machine, which would have to run continuously.

Compatibility between the ohmic-heating coil and the homopolar generator represents some difficulty, and the technique used to provide proper matching will certainly affect the cost. In particular, the ohmic-heating coil

requires a voltage that is approximately equal to:

$$V = L I_m \omega \sin \omega t$$

where L is the inductance of the ohmic heating coil, I_m is the maximum current in the coil, and ω is the discharge frequency (π radians/second). The terminal voltage for a coil of 1.1 H and 40,000 A must be 140,000 V in order to reverse the current in 1 sec.

The homopolar generator is basically a low-voltage, high-current device. Either a voltage matching circuit is required, or a number of generators must be connected in series. If one makes the assumption that even with some form of voltage-matching circuit, several generators will have to be placed in series, then a drum-type generator of the form shown in Figure V-15 is probably more applicable than a disk-type generator, because voltage insulation is technically easier in the former. The magnetic field is radially directed in the region of the cylinder, and the induced voltage between brushes is

$$V = v \int B \, dl \quad (V-25)$$

where v is the surface velocity of the conducting cylinder, B is the magnetic field along the length of the cylinder, and l is the axial distance along the cylinder. The effective voltage per generator depends on the length of the cylinder, the magnetic field, and the surface velocity of the cylinder. The unit size suggested in the Westinghouse Electric Corporation report is a cylinder two-meters long and two meters in diameter. For a maximum surface speed of 277 m/sec and a magnetic field of 30 kG, the voltage per generator is 1660 V. Eighty-four generators would have to be connected in series to gain enough drive voltage for the TEPR.

In addition, the effective capacitance of the generator must resonate with the ohmic-heating coil at a frequency of 1/2 cycle per second. The effective capacitance of a homopolar generator is

$$C = \frac{J}{B^2 l^2 r^2} \quad (V-26)$$

where J is the moment of inertia of the rotor, B is the magnetic field, l is the length of the generator, and r is the radius of the cylinder. The moment of inertia is

$$J = 0.5 \pi \rho l (r_o^4 - r_i^4) \quad (V-27)$$

Figure V-15. Schematic of a Drum-Type Generator.

where ρ is the density of the rotor material, ℓ is the length of the cylinder, and r_o and r_i are the outer and inner radii, respectively, of the cylindrical rotor. The capacitance of each generator must be about 7.5 F in order to have the circuit resonate at 1/2 cycle per second. Roughly, the shell thickness of the rotor cylinder would be about 0.01 m and the maximum stored energy would be about 10.5 MJ.

The shell thickness could be increased and, correspondingly, the total stored energy per unit increased, if the required coil voltage could be decreased or the generator terminal voltage increased. In the latter case, either the rotor velocity must be substantially increased, or the magnetic field increased. The surface velocity of the rotor is limited by material strength. The magnetic field is limited by either the saturation of the magnetic material or the quench limits of superconducting wire.

The extent to which switching is a problem would depend on how the voltage matching is actually accomplished. For instance, if all the voltage could be developed by simply adding the generators in series, then standard high-vacuum power line interrupters could be used to perform this task at relatively minor cost and with good reliability, because switching of the current occurs at nominally zero voltage.

Losses incurred in the cycle must be resupplied during the exhaust- and replenishment-phase of the burn cycle. Losses in a homopolar generator system should be about 2 to 3%. A dc power supply of 75 V and 3 MW is capable of resupplying 5% energy loss by recharging the ohmic-heating coil at the rate of about 65 A per second. Higher losses require a proportionately larger dc power supply.

This example identifies, in a rather superficial way, the advantages and disadvantages of a homopolar generator system. Design and compatibility problems exist in all systems. Relatively detailed designs in collaboration with major equipment manufacturers should be undertaken on those systems that appear most applicable. Undoubtedly, research and development will be needed to insure the feasibility of the technical designs before the final system is chosen.

2. Equilibrium-Coil Power Supply

The equilibrium-coil power supply must charge the equilibrium coil from 0 to 15,000 A in one second at the start of the burn cycle. The coil remains

charged during the burn cycle and then is discharged to 0 A. The current remains zero for the duration of the exhaust and replenishment phase of the burn cycle.

The equilibrium coil has an inductance of 0.8 H, and so the stored energy is 90 MJ. Basically, the requirements of the equilibrium-coil power supply are similar in nature to the ohmic-heating coil power supply and, in all probability, an ohmic-heating coil energy storage unit will be used for the equilibrium coil. There is one major difference; the energy storage unit must store the energy for the total exhaust and replenishment phase, whereas the ohmic-heating coil energy is stored back in the coil by reversing the current. Thus, if a homopolar generator is used, a power supply will have to be switched onto the generator terminals to maintain the energy level. For a superconducting magnetic energy transfer system, the recycle motion would have to be suspended for the duration of the exhaust and replenishment period.

3. Toroidal-Field Coil Power Supply

The design of the toroidal-field power supply presents no special difficulties, even though the operating current is 10,000 A and the coil stores 15,000 MJ of energy, because the coil is operated at a constant current. The tolerable charging time affects the size of the unit, but since four hours is an acceptable charging period, a nominally sized unit of 200 V, 2 MW is adequate.

4. Neutral-Beam Power Supplies

A neutral-beam injector operates at 180 kV with 50% efficiency. Either 16 or 32 individual injectors will be used for the TEPR. Polyphase, phase-controlled rectifier systems are envisioned for generating that much power at 180 kV. Emergency phaseback will be controlled from a feedback element to prevent damage to the accelerating column during sparking. Twelve or twenty-four phase systems are being considered, so that energy storage capacitors for ripple suppression will be sufficiently small to eliminate the need for crowbarring the supply during sparking.

The 80 MW of output power required for the neutral injectors would represent a sizeable bump on the power grid, if some energy storage system was not used to even out the power demand. A conventional motor-generator-flywheel set, operated similarly to those at several major particle

accelerators, can be used for this purpose. The high-power pulse is taken out of the stored energy of the rotors and flywheel. The motor reaccelerates the generator and flywheel before the next pulse. Each neutral-beam injector would have its own step-up transformer and phase controlled rectifier.

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CHAPTER VI

PRIMARY ENERGY CONVERSION SYSTEM

In the TEPR design activity carried out thus far, an attempt has been made to consider collectively all of the systems and components that lie between the plasma and the toroidal-field coils; this collection of hardware is referred to herein as the primary energy conversion system (PECS). The principal components of this system are (1) the first-wall assembly; (2) the blanket region, which comprises the 30 to 40 cm zone immediately backing the first wall (99% of the thermal energy is generated in the first wall and the blanket); (3) the primary coolant that removes all of the sensible heat from the blanket, together with its associated circulation and processing equipment; (4) the magnet shield, which lies between the blanket region and the toroidal-field coils and provides the attenuation of neutron and gamma radiation required to protect the magnet systems; and (5) the penetrations that provide access for the neutral beams, vacuum system, diagnostics, fuel delivery, etc. Briefly, the preliminary PECS design for the TEPR calls for two stages of operation. The first wall for Stage I operation consists of graphite-coated stainless steel sections, appropriately fitted with insulated (flux-breaker) joints, hydraulic-coupling flanges, and the required vacuum and neutral-beam penetrations. The blanket/shield region contains an optimized composition of stainless steel and B_4C which permits generation of sensible heat and provides adequate radiation protection for the toroidal-field coils. The sensible heat developed in the blanket is removed with either a pressurized-water circuit or a pressurized-helium circuit. A drawing of the vertical section of the Stage I PECS, showing its orientation with respect to the toroidal-field coils, is given in Fig. VI-1.

During the second stage of operation (Stage II), sections of the Stage I PECS are replaced with experimental modules containing a tritium-breeding material (e.g., liquid lithium or a solid-lithium compound). Provision is made for processing of the breeding material to remove tritium and impurities. These Stage II modules will be designed to be compatible with any remaining Stage I sections but may employ more advanced concepts for (1) first-wall design, (2) blanket/shield structure, and (3) modular cooling (e.g., with liquid lithium).

An integrated multi-disciplinary treatment of the PECS design is necessary because the concerted operation of the PECS is vital to the accomplishment of

Figure VI-1. Vertical Section of the TEPR Preliminary Design

the design objectives discussed in Chapter I for a TEPR. In addition to defining a preliminary design point, the emphasis of much of the ANL design activity for this system, during FY 1975, was on a broad spectrum of materials evaluations, parametric sensitivity analyses, scoping studies of individual component functions, and determinations of performance objectives. The results of studies performed to date and conclusions drawn from them, that affect the design of the TEPR, are given in the following sections of this chapter. Discussed in order are: the criteria for materials selection, the nuclear performance of the PECS, the results of preliminary thermal-hydraulic analyses of the blanket, and selected aspects of the engineering design for the Stage I PECS. Appendices C, D, F, H, and I contain additional information pertaining to materials considerations, neutronic analyses, and technology assessments that have helped to set the guidelines for the preliminary PECS design.

In the ensuing year, the aim of this TEPR design effort will shift from the broad scoping studies described herein to a much more detailed analysis of the PECS and all the systems that interface directly with the PECS. The ultimate goal of this effort will be to identify a credible and technologically-feasible design for an integrated TEPR primary energy conversion system.

A. Materials for the Primary Energy Conversion System

The proper selection of materials for the primary energy conversion system of the TEPR will have a major influence on the attainment of the design objectives. The major materials considerations for the primary energy conversion system of a fusion power reactor are reviewed in Appendix C. Only those materials considerations that are applicable to the TEPR design, outlined in Chapter II, are included in this section. As stated previously, the primary objectives of the TEPR are the production of suitable amounts of thermonuclear power and the conversion of this power to sensible heat, which may be used to produce electricity. The "minimum risk" approach for enhancing the attainment of these two primary objectives in the suggested time period severely limits the materials alternatives for the TEPR. In particular, the absence of a tritium-breeding blanket in Stage I alters the priorities for materials selection in the primary energy conversion system relative to a demonstration or commercial-scale reactor. The strong incentive to utilize available materials technology in order to meet scheduled design objectives places further restraints on the materials selection. In some cases it will be necessary to utilize

materials in TEPR that are probably not optimal for commercial-power reactor applications, since time and economic restrictions prohibit the satisfactory development of an advanced materials technology for the TEPR. Therefore, materials selection for the Stage I TEPR is based primarily on available materials technology. However, advanced materials should be developed for and tested in Stage II. (One of the principal motivations for the staged approach is to provide extra lead time for the development of advanced materials.)

1. The First-Wall System

Probably the most critical materials problem relating to the achievement of the TEPR design objectives is the proper selection of materials for the first-wall system. The primary function of the first-wall system is to provide a low-pressure region of controlled-impurity level in which the plasma burn can occur. For purposes of this discussion, the first-wall system includes the vacuum vessel that surrounds the plasma region and other associated components such as a low-Z liner, a limiter, a flux-breaker, and the vacuum-wall penetrations.

The principal requirements of the TEPR first wall are: (1) to protect the plasma region from excessive atmospheric contamination, (2) to prevent excessive plasma contamination by products of plasma-wall interactions, and (3) to maintain its structural integrity for sufficient times under the severe irradiation, thermal and stress conditions imposed by an operating fusion reactor environment.

The first-wall system design options that have been considered for the TEPR (see Appendix C) include: (1) a bare-metal first wall, (2) a first wall fabricated from sintered metal-metal oxide product of the SAP type, (3) a composite consisting of a metal vacuum wall with a protective low-Z coating, and (4) a metal vacuum wall protected from the plasma by a separate low-Z liner.

The bare-metal wall is the most attractive of the four options on the basis of fabricability; and, because of the extensive property data available from other programs and the ease of fabrication, austenitic stainless steel is a leading candidate for the vacuum wall of the Stage I TEPR. For the proposed TEPR conditions, viz., short lifetime (~ 10 yr), low wall loading ($\sim 0.2 \text{ MW/m}^2$), low plant duty factor ($< 50\%$), and no breeding requirement, a stainless steel vacuum wall facing the plasma appears to be acceptable for all of the principal requirements except for the problem of plasma contamination by products of plasma-wall interactions.

Stainless steel is considered one of the best materials for high-vacuum applications, since it is nonpermeable (except to hydrogen) and reliable-

leaktight joints are obtainable by well-established welding procedures. The outgassing properties of stainless steel, the proposed vacuum pumping capacity, and the expected vacuum conditions are presented in more detail in Section V-B.

Preliminary assessment indicates that a stainless steel vacuum wall will maintain adequate structural integrity for the limited design conditions and lifetime of the TEPR. Extensive mechanical property data, much of which has been obtained from Liquid Metal Fast Breeder Reactor (LMFBR) programs, are available for AISI Types 304 and 316 stainless steel.⁽¹⁻⁵⁾ In the absence of radiation, stainless steels maintain adequate tensile strength and ductility to a temperature of 600°C. However, two thermal effects that will require careful consideration in the first-wall design are thermal-fatigue problems and thermal-gradient stresses. The burn cycle of the plasma and the wall thickness will influence the magnitude of these problems. At high-radiation levels (1) void swelling, (2) radiation-damage embrittlement, (3) helium and hydrogen embrittlement, and (4) transmutation effects can substantially affect the elevated-temperature mechanical properties of stainless steel.⁽²⁻⁵⁾ The plasma-physics and neutronics calculations indicate a total neutron current of about 4×10^{17} n/m²/sec at the first wall during a burn, one-fourth of which are 14 Mev neutrons. This corresponds to a wall loading of ~ 0.2 MW/m². For a 50% duty factor, helium- and hydrogen-production rates in the first wall are 22 and 53 appm per year respectively, and the bulk damage is 1.1 displacement per atom (dpa) per year. Recent data from LMFBR programs⁽⁶⁾ indicate that an acceptable level of less than 5% swelling of stainless steel is expected for TEPR reference design conditions (1 MW-yr/m²).

Reduced ductility caused by the combined radiation-damage embrittlement and helium embrittlement presents, perhaps, the most limiting criterion to the structural integrity of a stainless steel first wall. Radiation decreases the ductility of stainless steel and at high-damage and helium levels a total elongation of $< 1\%$ has been observed.⁽³⁻⁵⁾ Cold-worked material generally exhibits a lower ductility than annealed material, therefore, annealed stainless steel is probably preferable for the TEPR first wall. The data indicate that annealed material will maintain adequate ductility at the TEPR neutron-damage level. Although no data are available on material containing helium concentrations at levels expected in the TEPR first wall, i.e., a few-hundred appm, prohibitive effects are not expected. Substantial amounts of hydrogen,

which will also be produced in the steel, will likely diffuse out of the steel because of hydrogen's relatively high mobility at the operating temperatures. However, it may be desirable to control the hydrogen pressure in the coolant at low levels in order to enhance the hydrogen removal from the steel, since hydrogen embrittlement is known to occur at moderate levels (> 10 appm) in stainless steel when cooled below 150°C .⁽⁷⁾ Solid transmutation products are not expected to substantially affect the mechanical properties of stainless steel for the anticipated TEPR conditions.

Sputtering and blistering of the surface, caused by high-energy neutrons and ions, will not seriously affect the mechanical integrity of a stainless steel first wall, since wall-erosion rates of less than 0.1 mm/yr are predicted from calculations based on available sputtering and blistering data (see Appendix C). Chemical interactions between the stainless steel first wall and either the plasma (including impurities) or coolant (including impurities) should not be sufficient to significantly affect the mechanical integrity of the first wall. However, erosion by a high-velocity coolant, e.g., pressurized helium, and mechanical fatigue from vibration, caused by the high-velocity coolant, may affect the integrity of the first wall. These effects must be investigated more extensively.

Stainless steel is relatively attractive from a neutronics viewpoint, but it becomes highly radioactive when exposed to a high-neutron flux. Since stainless steel will most likely be used in the blanket, shield, and magnet sections of the TEPR, the high activation in the first wall becomes less critical because remote maintenance is already essential.

Calculations (see Chapter III), based on the sputtering coefficients given in Appendix C, indicate that excessive plasma contamination will occur as a result of sputtering in the TEPR if stainless steel is used as the first wall facing the plasma. The detrimental effect on the plasma performance is due to the characteristic high Z of the sputtered metal atoms. Similar calculations, based on the same sputtering coefficients but assuming a $Z = 6$, i.e., graphite, wall facing the plasma, indicate acceptable plasma performance. Two of the alternate first-wall design options circumvent the problem of sputtering of the high- Z metal atoms into the plasma: one concept utilizes a separate low- Z liner to protect the metal vacuum wall and the other utilizes an adherent low- Z coating on the vacuum wall. Liners, either in the form of a graphite curtain

or a relatively thick graphite or silicon-carbide shield, have been proposed. In addition to the low-Z plasma interface, the materials typically considered for the liner concept also permit a higher first-wall temperature. However, fabrication of a vacuum wall-liner system that will reliably withstand the severe operating conditions of the TEPR for a long period of time poses an extremely difficult problem. Possible cracking of the liner as a result of extensive thermal cycling or severe radiation damage, must be carefully examined. Anisotropic radiation growth, which has been observed in some graphites, may also affect the structural integrity of the liner. Also, the difficulty of repairing a damaged liner is an important consideration. Two other potential problems that relate to plasma performance in a TEPR utilizing the liner concept are high outgassing rates, which are characteristic of most liner materials considered (see Appendix C), and possible chemical interactions between carbonaceous liner materials and the hydrogenous plasma. The additional outgassing from the added wall area, viz., both sides of the liner and the vacuum wall, plus the difficulty in pumping out the adjoining annulus, may impose excessive pumping requirements on the vacuum system.

The second design concept that utilizes a low-Z interface between the plasma and the vacuum wall, i.e., the adherent coating on the vacuum wall, has several attractive features. The major one appears to be much simpler fabrication. Several standard techniques are available for coating metals and, although not essential, it is conceivable that the coating could be applied after the vacuum wall is assembled. Another implication of this concept is the feasibility of remote replacement of the coating after extended reactor operation. The materials considered most applicable are the same low-Z materials suggested for the separated liner, e.g., carbon or silicon carbide. The more desirable coatings for this application are those where adhesion to the metal is greater than its cohesion, since these coatings will tend to crack rather than spall or chip. Because fine cracks in the coating will expose only a very small area of steel to the plasma, the coating should be effective in reducing contamination of the plasma by high-Z metal ions. The coating concept may also be effective in reducing the blistering rate of the first wall, since thin porous coatings approximately 50 μ thick would absorb most of the helium (penetration distance typically a few microns). Thin coatings (1-4 mm) are likely to present less of an outgassing problem than a thick suspended liner. Additional work is necessary to verify the apparent advantages of this first-wall concept.

2. Blanket and Shield Materials

Because tritium breeding is not a design objective for Stage I TEPR operation, the materials considerations for the blanket region are substantially different from those for a commercial power reactor. The primary purpose of the blanket in Stage I is to convert thermonuclear energy into sensible heat. The Stage I blanket is essentially an extension of the shield and the most important materials constraints are the neutronics considerations. The fabrication and heat removal are the other major technical considerations. The candidate materials for the Stage I TEPR blanket/shield system have been selected primarily on the basis of the neutronic analyses, which are presented in Section VI-B and in Appendix D. The thermal-hydraulic analysis for heat removal from the blanket/shield system is contained in Section VI-C.

The materials that are being considered for the blanket/shield region of Stage I are combinations of stainless steel and B_4C with a combination of tungsten or tantalum and B_4C for a small inner region of the torus (see Fig. VI-2). As discussed in Section VI-B, the layered sections are utilized to maximize the neutron attenuation. Since stainless steel is a good structural material, fabrication of the steel portion of the blanket/shield section should not pose major problems. Details of the structural considerations for the B_4C sections must be worked out. Tungsten is difficult to fabricate and is expensive, however, the structure is not expected to be overly complex so fabrication of the tungsten regions should not be a prohibitive problem.

The inner portion of the blanket will be exposed to temperatures and neutron fluxes similar to those experienced by the vacuum wall. It was concluded in the first-wall system analysis that bulk-radiation damage of a stainless steel vacuum wall would not be excessive for the predicted TEPR conditions; therefore, radiation damage to the stainless steel blanket sections should not be excessive. However, boron carbide is subject to considerable helium production during irradiation and, although data are limited, tungsten is known to experience some radiation swelling. The extent of these effects will be examined more thoroughly and considered in the design.

Most of the nuclear heating (> 99%) will be generated in the first 40 cm of the blanket region (see Section VI-B). Since one of the primary objectives of the Stage I TEPR is to produce 100 megawatt quantities of sensible heat, the method of heat removal from the blanket is an important consideration. High

temperatures, i.e., a few-hundred degrees centigrade, in the blanket are required to produce sensible heat; and considerable cooling capacity is required to remove 100 megawatt quantities of heat from the blanket. The proposed coolants, viz., helium and water, and calculations on the cooling requirements are discussed in Section VI-C. Since both of these coolants will be under high pressure, the stress analysis of the coolant channels will be an important aspect of the design. Another important consideration is the compatibility of the proposed coolants with the blanket materials at the appropriate temperatures. Extensive data that exist on the corrosion of stainless steels in pressurized water and steam indicate that water chemistry and fabrication history of the steel are critical parameters in the corrosion studies. For example, small amounts of oxygen or chlorine in the water or steam have significant effect on stress-corrosion cracking of austenitic stainless steels. Sufficient data exist from water reactor programs to specify the appropriate conditions for adequate compatibility of stainless steel with water in the blanket. Available data also indicate that helium can be contained in stainless steel at the temperatures proposed for the TEPR blanket. Probably the most critical corrosion problem for either coolant is the transfer of radioactive corrosion product outside the reactor. This problem will require careful consideration in the final design. The details for cooling the B_4C sections require further analysis. Tungsten is not compatible with either water or helium (with expected impurities) under the proposed TEPR conditions. Therefore, cladding of the tungsten or containment of the coolant in stainless steel will probably be required in the blanket region where tungsten is used.

Insertion of blanket modules with a tritium breeding capability will impose additional constraints on the structural materials for the Stage II TEPR design. Appendix C contains a more detailed discussion of the materials problems relating to the implementation of a tritium breeding blanket in the TEPR. As discussed in Section VI-A-4, lithium or lithium compounds are the candidate materials for the tritium-breeding blanket. The form of the lithium, i.e., liquid metal or solid compound, will have a major influence on the selection of the blanket structural material. For example, a liquid-lithium breeding blanket will likely require the use of refractory metals, viz., vanadium-, niobium-, or molybdenum-base alloys, for containment materials. However, these alloys are probably unacceptable for use with helium or water coolants at the temperatures and impurity levels expected in the TEPR. Concepts

utilizing a solid-lithium compound as a breeding material will likely be limited to a gas (probably helium) coolant with iron-, nickel-, or aluminum-base alloys as containment and structural materials. Because of the complex interrelation of the breeding material, coolant, and structural material in the blanket region, further analysis is required before the most favorable Stage II blanket system can be specified.

3. Primary Coolant

Appendix C-3 contains a summary of coolant selection considerations for a TEPR. Based on this survey, pressurized water (as it is implemented in PWR's) and pressurized helium (as it is implemented in HTGR's) were chosen for early TEPR scoping studies. These choices were predicated largely on the substantial technological base that exists for both water and helium utilization in thermal energy transport and power conversion applications. (Early demonstration that magnetohydrodynamic effects arising in liquid metal primary coolant configurations for Tokamaks will not, in any significant way, compromise reactor performance, would probably place liquid lithium ahead of both helium and water on the list of candidate TEPR coolants.)

The advantages (see Appendix C-3) of (1) a relatively benign medium, (2) good prospects for simplified and effective tritium containment, (3) compatibility with advanced power cycle concepts, and (4) promising long-range applicability to fusion power production should make pressurized helium an optimum choice for the TEPR primary coolant. However, a relatively large electrical input may be required to overcome the cyclic pressure drop in the pressurized-helium heat-transfer circuit for a TEPR (see Section VI-C). If the pumping power exceeds ~20% of the plant electrical power, the capacity of the TEPR to produce net-electrical power could be jeopardized.

Consideration of demonstrated net-electrical power as an objective for a TEPR tends to force coolant selection in the direction of fluids and cooling configurations that require the least pumping-power input per electrical unit produced. In addition, the pressurized water coolant technology is highly developed. For these reasons, the decision was made to carry along a design incorporating pressurized water as an alternative coolant for the preliminary TEPR approach described in Chapter II. Although the coolant configurations for the TEPR have not been optimized in any sense, the results in Section VI-C show quite conclusively that a pressurized water-cooling system requires

significantly less pumping power and channeling volume fraction than a pressurized helium system per unit of thermal energy removed. The principal disadvantages of water as a fusion-reactor primary coolant are that it (1) represents a source of large-scale protium insertion to the mainstream fuel cycle, (2) is generally incompatible with liquid-lithium blankets and with advanced power-cycle concepts and (3) would act as an irreversible sink for any tritium that entered it.

4. Breeding Materials

Appendix C-4 summarizes a survey effort carried out to identify promising tritium-breeding materials that could be implemented in the second stage of operation of the TEPR. Based upon this survey, current Stage II design activities are focusing on two breeding material options. One of the options is a liquid-lithium blanket supported by a vanadium-alloy structure; the second option is a solid-lithium compound contained in a stainless steel structure. Design considerations for each of these options are summarized below.

(a) Liquid Lithium Breeder Blanket

There appear to be several reasonable approaches to the implementation of a liquid-lithium/vanadium-alloy blanket. Natural lithium could be employed in a zone of representative thickness (~10 cm). As was mentioned in Appendix C-4 and shown in Appendix D, this approach would not permit the achievement of a level of tritium breeding that projected to a net breeding gain in the Stage II modules, but all other technological aspects of the operation of a liquid-lithium blanket, including recovery and recycle of whatever tritium is bred, could be verified. Thermal energy removal from the natural-lithium blanket could be achieved by (1) using the lithium as both breeding medium and primary coolant or (2) coupling a semi-stagnant lithium blanket with a non-metallic primary coolant medium such as helium or perhaps pressurized water. From the standpoint of blanket system complexity, there are distinct advantages to using lithium as both blanket and primary coolant; but in order to carry this approach into the final stages of the TEPR design it will be necessary to show conclusively that the magnetohydrodynamic interactions generated by flowing lithium in the magnetic fields do not compromise either plasma stability (by altering field structure) or coolant performance.

For the case of a semi-stagnant liquid blanket, cooled by helium or water, the lithium could still be circulated at a slow rate to permit external blanket

processing. (In-situ processing of liquid-lithium blankets appears to be fraught with many imponderable complications.) Because of conflicting materials compatibility requirements, coupling of helium or water to a liquid-lithium blanket will undoubtedly force the use of some form of multiplex metallic interface. Although the technological implications of interfacing a liquid-alkali metal with water in a heat transfer circuit are not unprecedented, there are strong motivations (from an overall safety and reliability viewpoint) for giving primary consideration to a helium-cooled, liquid-lithium blanket design.

An alternative to the use of natural lithium as a breeding medium is to employ enriched liquid lithium ($\sim 90\%$ ^6Li) in combination with sufficient beryllium to permit demonstration of a net breeding gain in the Stage II modules. This configuration could be employed in a relatively thin zone (<10 cm) and would not be expected to add significant additional blanket complexity over and above that arising from the use of natural-liquid lithium. Blanket processing and coolant considerations would be essentially identical to those discussed above for the natural-lithium blanket.

Approaches to tritium recovery and other blanket processing requirements for the liquid-lithium blanket concepts, that will be pursued in the coming year, are summarized in Section VII-B-7.

(b) Solid-Lithium Compound Breeder Blanket

At least one design approach involving a solid lithium-containing material as the breeder blanket will be carried along during the coming year. The solid material will be selected on the basis that (1) it be compatible with stainless steel insofar as possible so that minimal perturbation of the Stage I blanket design is required, (2) it have a high enough melting point (probably $> 1200^\circ\text{C}$) such that no eminent danger of slumping or melting exists, and (3) it be compatible with reasonably pure inert gases like nitrogen and helium. This lithium-containing compound will be appropriately enriched in ^6Li and interspersed with sufficient amounts of beryllium or a beryllium compound (e.g. Be_2C) to yield a demonstrated net-breeding gain.

The mixture of lithium- and beryllium-containing materials will be prepared in particle form, having a size range that permits acceptable thermal energy and tritium removal. This particle mix will be loaded into a honey-combed cellular structure located at an optimum depth in the blanket. Both packed-bed and fluidized-bed operation of solid-blanket concepts have been

investigated during the past year. While fluidized-bed operation appears to offer marginally better conditions of temperature uniformity for individual particles and over the entire bed, designing for fluidized-bed operation will add considerable complexity to the assembly of the blanket and may result in a significant increase in overall blanket thickness. Furthermore, the investigations of fluidized-bed blanket performance showed that substantial auxiliary cooling (over and above that provided by the fluidized gas) will be required for first wall loadings in the range of interest for a TEPR (0.1 to 0.3 MW/m^2). Consequently, initial solid-blanket conceptual design studies for the TEPR will focus on gas cooled packed-beds in which all the thermal energy deposited in the blanket and all the tritium produced by transmutation of lithium are recovered from the primary cooling gas (probably helium).

B. Nuclear Design of the Primary Energy Conversion System

Since the TEPR will operate on the D-T cycle, 17.6 MeV of energy is released per fusion reaction in the form of an alpha particle and a neutron with average kinetic energies of 3.5 MeV and 14.1 MeV respectively. These average energies and the neutron spectra will be affected to some extent by the energies of the neutrals injected into the plasma as well as the plasma-ion temperatures, but the effects are small as far as the analysis in this chapter is concerned. The 14 MeV neutrons represent the main source of recoverable energy in a D-T fueled fusion reactor. Regardless of whether the reactor is designed to produce electrical power or just demonstrate the feasibility of doing so, sufficient shielding must be provided against these neutrons to protect the magnet and other reactor components. The choice of a superconducting magnet to produce the toroidal magnetic field in TEPR imposes a set of stringent requirements on the design of the blanket/shield system.

The space between the plasma region and the toroidal-field coils is generally considered as two separate regions; the blanket and the magnet shield. The blanket is the region in which most ($\sim 99\%$) of the kinetic energy of the neutrons and the resulting secondary gammas is converted into heat. The blanket is operated at reasonably high temperature in order to obtain an adequate thermodynamic efficiency in the case of an electrical power producing device and in order to keep cooling requirements to a manageable level in the

case of no electric power production. In Stage II where tritium breeding is a design objective, a zone of lithium in one form or another must be provided for in the blanket region. The energy conversion requirements are generally satisfied by blankets which give roughly two orders of magnitude attenuation in nuclear radiation. However, the superconducting magnet, which is cryogenically cooled to about 4°K, requires, as will be shown later, another four orders of magnitude attenuation of the neutrons and photons streaming out of the blanket. This attenuation is provided by the magnet shield.

The primary functions of the magnet shield are:

1. To reduce the nuclear heating in the magnet to the levels allowed by:
(a) total power required to run the refrigerators and (b) the maximum local heat-generation rates imposed by practical limits for coil design (e.g., maximum allowable temperature increase in superconductor and normal conductor, heat transfer area and spacing between coolant channels, etc.).
2. To attenuate the nuclear radiation to the minimum allowed by tolerable radiation damage to the components of the superconducting magnet. These components are: (a) the superconductor, (b) the normal (stabilizing) conductor, (c) structural materials, (d) electrical insulators, and (e) thermal superinsulators. Of particular concern in examining the radiation damage effects in the magnet components is the reduction in the critical current density, J_c , in the superconductor and the increase in the resistivity of the normal conductor.
3. To reduce the level of radioactivity in the magnet to a tolerable level. Reliable performance of the superconducting magnet is of extreme importance for satisfactory operation of TEPR. This stringent requirement of reliability may dictate the construction of a permanent superconducting magnet, i.e., a magnet which would not be disassembled except under extreme circumstances. In this case, magnet maintenance has to be performed in place; it is, therefore, essential to minimize the radiation induced activation in the magnet components so that magnet maintenance and minor repairs can be made in place with a minimal degree of radiation protection after a reasonably short cool-down time. As will be shown later, it is imperative that the reactor be shut down prior to any maintenance or repair work on components within the nuclear island.

Depending on design characteristics such as power level and blanket/shield material composition, satisfying the above requirements may be possible only with a relatively thick blanket/shield. On the other hand, increasing the blanket/shield thickness, Δ_{BS} , reduces the reactor power and increases the reactor cost. The reactor thermal power, P_T , can be written as (see Section I-B)

$$P_T = C \left(\frac{B_t}{A} \right)^4 R a^2 \quad (VI-1)$$

where

a = plasma minor radius

R = plasma major radius

A = aspect ratio = R/a

B_t = toroidal magnetic field at the plasma centerline

$$B_t = \left(1 - \frac{r_w + \Delta_{BS}}{R} \right) B_{\max}^{TFC} \quad (VI-2)$$

$B_{\max}^{TFC}$ = maximum toroidal magnetic field at the inner coil winding

C is a parameter which depends on other variables of no concern here and r_w is the first wall radius. Selection of a particular superconducting material such as NbTi sets a practical upper limit on $B_{\max}^{TFC}$. Equations VI-1 and VI-2 show that, for fixed minor and major radii, B_t and P_T decrease as Δ_{BS} is increased. In addition, increasing the inner radius ($r_w + \Delta_{BS}$) of the magnet increases its cost. Equation VI-2 also shows that for fixed $B_t/B_{\max}^{TFC}$ the maximum allowable Δ_{BS} is smaller for reactors with smaller major radius. Several considerations discussed in Section I-B indicate that TEPR will probably have $R \sim 5-7$ m; the reference designs here use $R = 6.25$ m and $R = 5.45$ m for the circular and non-circular plasma cross sections, respectively. This restricts the allowable space for the TEPR blanket/shield and makes its design more difficult than that for large full-scale devices ($R \sim 10$ to 15 meters).

The biological dose outside a magnet shield, satisfying the magnet protection criteria, is generally a few million mrem/hr or more depending on the particular reactor design. Therefore, an additional biological shield is required to reduce the biological dose to ~ 1 mrem/hr. This can be accomplished by a few feet of concrete or equivalent material. Working areas such as offices and control rooms would be located outside the biological shield.

The rest of this section is devoted to a description and analysis of the nuclear design for the reference blanket/shield system. This reference design was based on the results of a detailed analysis of the major conflicting requirements imposed by other reactor components. The results of the sensitivity and optimization studies for the various trade-off aspects in the nuclear design of the TEPR blanket/shield system are discussed in Appendix D.

1. Description of the Blanket/Shield for the ANL-TEPR Reference Design

This subsection, contains a description of a preliminary reference design for a TEPR blanket and shield. The primary design criteria were (See Appendix D):

- 1) Radiation-induced resistivity increase in the copper stabilizer of the superconducting toroidal field coils $< 3 \times 10^{-8} \Omega\text{-cm}$.
- 2) Maximum decrease (due to irradiation) in the critical-current density in NbTi superconductor in the toroidal-field coils $< 5\%$.
- 3) Maximum power required to run the refrigerators of the superconducting toroidal field coils $\sim 1\%$ of plant electrical-power output.
- 4) Total radiation dose to the mylar insulation of the toroidal-field coils $< 1.2 \times 10^8$ rad for the magnet lifetime.

This reference design does not represent a fully detailed final design of the blanket and shield for a TEPR. However, the analysis of such a design serves three important purposes:

- 1) It demonstrates the feasibility of designing a TEPR blanket/shield system that satisfies the major requirements of such a system.
- 2) A knowledge of the major characteristics and key parameters of such a conceptual design is very helpful in understanding the major problems of the primary energy conversion system and its interface with other reactor components.
- 3) This design provides a reasonable starting point for additional in-depth optimization and detailed design studies.

Figure VI-2 is a schematic of the ANL-TEPR reference design for the blanket and shield. No modular or cellular structure is shown in this figure.

Figure VI-2 Vertical Section of the Blanket/Shield for the Reference Design

However, the figure illustrates a new concept which is proposed for the TEPR design. The inner region between the plasma and the vertical segment of the D-shaped, toroidal-field coils comprises the highest magnetic field zone inside this coil system. As discussed earlier, for a given maximum magnetic field at the magnet, reducing the blanket/shield thickness in this inner high-magnetic-field region increases the reactor power significantly and also makes it possible to reduce the size of the reactor for the same reactor power. In the exterior region of the D-magnet the space problems are greatly reduced by the fact that extending the D in the low-magnetic field region, hence low stress region, does not involve a large cost penalty or an added technological difficulty. Therefore, a blanket/shield system is proposed which consists of two parts. The first part is a specially-designed blanket/shield region in the inner clearance between the plasma and the vertical segment of the D-shaped magnet, and the second part is the blanket/shield in the outer regions. The inner part is designed to minimize the blanket/shield thickness in the high-magnetic field region to a value that just satisfies the radiation-damage design requirements in the magnet. This is accomplished by using materials that are very efficient attenuators -- such as tungsten or tantalum. Although these materials are very expensive, the increase in the blanket/shield cost is not large because the total volume of the inner region in this concept is only 5 to 10% of the total blanket/shield volume. The small increase in the blanket/shield cost is more than offset by the reduction in the total reactor cost, as will be discussed later.

The inner region of the blanket/shield is generally not useful as a test module in any practical design because of the lack of space between the magnets at the inner side of the torus. Therefore, the criterion of reducing blanket/shield activation and biological dose after shutdown can be significantly relaxed and a wider spectrum of materials can be considered for this region. The outer blanket/shield is the major component of the blanket/shield system. Space restriction in the outer region is not severe in a D-shaped magnet and the use of materials less efficient and less expensive than those used in the inner blanket/shield is possible.

2. Neutronics and Photonics Analysis of the Blanket and Magnet Shield

Figure VI-2 shows both the inner and outer blanket and magnet shield for the reference design. The major radius is 6.25 m and the first wall radius

is 2.4 m (see Chapter II). The thickness of the inner blanket/shield (distance between points A and C in the figure) is 100 cm. The thickness of the outer blanket/shield (distance between points F and E in the figure) is 131 cm. Dimensions and material compositions for the inner and outer blanket/shield are shown in Tables VI-1 and VI-2, respectively. The blanket and shield consist mainly of alternating zones of stainless steel and boron carbide. The dimensions and compositions of the various zones are the result of an attempt to maximize the attenuation for an inner blanket/shield thickness of one meter of stainless steel and boron carbide. From results in Appendix D, a mixture of stainless steel and boron carbide was found superior (from the total attenuation standpoint) to all other material compositions investigated in this study except tungsten-boron carbide and tantalum-boron carbide mixtures. The nucleonic results for the reference design are discussed next. The effects of reducing the inner blanket/shield thickness to less than the reference value of one meter will be examined in Section VI-B-5. Alternative designs which bring up the advantages of replacing the SS-B₄C in the inner blanket/shield by W-B₄C and Ta-B₄C mixtures will also be investigated.

The maximum radiation damage and heat-generation rates in the magnet will occur at the intersection of the inner vertical segment of the D-shaped coil with the horizontal midplane (projected as point A in Fig. VI-2). Thus, the magnet design should be based on the values for the important parameters (e.g. radiation-induced resistivity, nuclear heating, and dose to the insulators) along the line A-D in Fig. VI-2. Therefore, in presenting the results for the reference design, great emphasis will be given to the inner region (i.e. line C-A-D). It should be noted that the first 100 cm of the outer blanket/shield is identical in the reference design to the inner blanket and shield. The difference in the neutronics results for the first meter of the outer blanket/shield (e.g. along line F-E or H-G) from those for the inner blanket/shield (line C-A) is very small. The level of nuclear radiation in the outer segment of the magnet is approximately two orders of magnitude lower than that in the inner segment.

The neutron, gamma, and total nuclear heating are given by zone in Table VI-3. The spatial distribution of these heating rates in the blanket and magnet shield are given in Figs. VI-3, VI-4, respectively, for a neutron wall loading, P_w , of 0.2 MW/m^2 . It is noted from these results that about 99% of the nuclear heating is generated in the first 40 cm; therefore,

TABLE VI-1. Dimensions and Material Composition of the ANL-TEPR Inner Blanket/Shield

Zone	Outer Radius cm	Thickness cm	Material Composition	Density ^a Factor
1	210	210	Plasma	0.0
2	240	30	Vacuum	0.0
3	241	1	Stainless Steel	0.5
4	256	15	Stainless Steel	0.9
5	261	5	Boron Carbide ^c	0.9
6	276	15	Stainless Steel	0.9
7	281	5	Boron Carbide	0.9
8	291	10	Stainless Steel	0.95
9	301	10	Boron Carbide	0.95
10	311	10	Stainless Steel	0.95
11	321	10	Boron Carbide	0.95
12	331	10	Stainless Steel	0.95
13	340	9	Boron Carbide	0.95
14 ^b	397	57	Magnet	1.0

^aThis density factor was used in the neutronics calculations to account for space occupied by the helium coolant, thermal expansion, material packing density, etc.

^bThe superconducting magnet was homogenized in the neutronics calculation as 44% copper, 44% stainless steel, 6% NbTi and 6% helium (these are volume percentages).

^cBoron carbide (B_4C) with boron of natural isotopic composition.

TABLE VI-2. Dimensions and Material Composition of the ANL-TEPR Outer Blanket/Shield

Zone	Outer Radius cm	Thickness cm	Material Composition	Density ^a Factor
1	210	210	Plasma	0.0
2	240	30	Vacuum	0.0
3	241	1	Stainless Steel	0.5
4	256	15	Stainless Steel	0.9
5	261	5	Boron Carbide ^d	0.9
6	276	15	Stainless Steel	0.9
7	281	5	Boron Carbide	0.9
8	291	10	Stainless Steel	0.95
9	301	10	Boron Carbide	0.95
10	311	10	Stainless Steel	0.95
11	321	10	Boron Carbide	0.95
12	331	10	Stainless Steel	0.95
13	340	9	Boron Carbide	0.95
14	351	10	Stainless Steel	0.95
15	361	10	Boron Carbide	0.95
16	371	10	Stainless Steel	0.95
17 ^b	430	59	Vacuum	0.0
18 ^c	487	57	Magnet	1.0

^aThis density factor was used in the neutronics calculations to account for space occupied by the helium coolant, thermal expansion, material packing density, etc.

^bThis space allows for assembly and replacement of the various modules. Parts of this region will be occupied by accessory equipment.

^cThe superconducting magnet was homogenized in the neutronics calculation as 44% copper, 44% stainless steel, 6% NbTi and 6% helium (these are percentages on volume basis).

^dBoron carbide (B_4C) with boron of natural isotopic composition.

TABLE VI-3. Total Heating by Zone for the Inner Blanket/
Shield of the ANL-TEPR Reference Design

Zone	Composition	Thickness cm	Density Factor	Neutron Heating	Gamma Heating	Total Heating	Total Heating
				In Units of MeV/DT	Neutron	For $P_w = 0.2 \text{ MW/m}^2$	
1	Plasma	210	0	--	--	--	--
2	Vacuum	30	0	--	--	--	--
3	SS ^a	1	0.5	0.3057	0.4309	0.7366	1.5766 (+3)
4	SS	15	0.9	2.5861	5.8210	8.4071	1.7995 (+4)
5	B ₄ C ^b	5	0.9	2.3906	0.1274	2.5180	5.3895 (+3)
6	SS	15	0.9	0.1662	0.6219	0.7881	1.687 (+3)
7	B ₄ C	5	0.9	0.3367	0.0126	0.3493	7.476 (+2)
8	SS	10	0.95	0.0108	0.0489	0.0597	1.2778 (+2)
9	B ₄ C	10	0.95	0.0576	0.0029	0.0605	1.2949 (+2)
10	SS	10	0.95	0.00065	0.00381	0.00446	9.546
11	B ₄ C	10	0.95	0.0026	0.00002	0.00262	5.607
12	SS	10	0.95	4.60(-5) ^e	2.714(-4)	3.174(-4)	0.6794
13	B ₄ C	9	0.95	1.30(-4)	1.354(-5)	1.435(-4)	0.3071
14	Magnet ^c	11	1	4.009(-6)	2.324(-5)	2.725(-5)	5.8326(-2)
15	Magnet	10	1	2.699(-6)	9.979(-6)	1.268(-5)	2.7140(-2)
16	Magnet	10	1	3.125(-6)	7.223(-6)	1.035(-5)	2.2153(-2)
17	Magnet	2	1	3.413(-7)	1.202(-6)	1.543(-6)	3.3026(-3)
18	Magnet	24	1	2.762(-6)	9.630(-6)	1.239(-5)	2.6520(-2)
Sum	--	--		5.855	7.070	12.925	2.766 (+4)

^a - SS = Stainless Steel

^b - B₄C = Boron Carbide

^c - Magnet - 44% Cu, 44% SS, 6% NbTi, 6% He

^d - total heating by zone in units of watts per centimeter in the toroidal direction for a neutron wall loading of 0.2 MW/m².

^e - 4.60 (-5) = 4.60 x 10⁻⁵, etc.

Figure VI-3. Spatial Distribution of Heating Rates in the ANL-TEPR Blanket

Figure VI-4 Spatial Distribution of Heating Rates in the ANL-TEPR Inner Shield

these 40 cm define the blanket according to the criteria given in the introduction to this section. Table VI-3 shows that the total nuclear heating per DT neutron is 13 MeV. Hence, the total energy per fusion reaction (including the 3.5 MeV alpha) is approximately 16.5 MeV, which is substantially less than a typical value of 20 MeV found^(8,9) for systems utilizing lithium. Using nuclear data from version IV of ENDF/B increases the energy per fusion by approximately 1.5 MeV. Thus, there is uncertainty in the value of the total energy per fusion reaction reported here of about 10%. (This will be examined in greater detail in future studies.) In any event, the reduction in the energy per fusion when lithium is replaced by stainless steel-boron carbide mixture in a fusion reactor blanket is not surprising. Almost all the high-energy reaction in Fe, Cr, and Ni are endothermic causing a relatively large-energy loss (converted to mass). Although each radiative capture in Fe, Cr, and Ni results in an energy gain of approximately 8 MeV compared with 4.8 MeV in ${}^6\text{Li}$ (n,t) reaction, only ~ 20% of the neutrons in the reference design have a chance of reaching the low-energy range compared with 90% in a mostly lithium blanket.

Figure VI-3 shows a maximum nuclear-heating rate of approximately 2.1 watts/cm³ for a neutron wall loading, P_w , of 0.2MW/m² in the first wall. The nuclear heating drops by about an order of magnitude in the first 15 cm of stainless steel in the blanket, then rises again to approximately 1.0 watts/cm³ in the inner part of the next boron-carbide zone. More than 60% of the heat generated in stainless steel comes from absorption of the secondary gamma-rays. Energy deposition by neutron interactions dominates the nuclear heating in boron carbide, primarily because of the large (n, α) cross section in B^{10} with a Q-value of +2.49 MeV. The nuclear heating rate per unit volume in boron carbide is about a factor of 3 higher than the neighboring stainless steel regions. This is not expected to cause any severe difficulties in the design of the heat transfer system as B_4C can be operated at much higher temperatures than stainless steel.

The total energy deposition in the shield, which starts at approximately 40 cm from the first wall, is only 1% of that in the blanket. The heat-generation rate per unit volume in the inner shield varies from a maximum of

approximately 2×10^{-2} to a minimum of 1×10^{-5} watt/cm³. Although these rates may be too low to allow heat removal with a high-thermodynamic efficiency, cooling of the shield is necessary in order to ensure its physical integrity.

Figure VI-5 shows the atomic displacement in stainless steel as a function of position in the blanket/shield in units of displacements per atom (dpa) for an integral wall loading of 1 MW-year/m². The spatial distributions of the hydrogen- and helium-production rates in stainless steel are shown in Fig. VI-6. In the first wall, the rate of atomic displacement is 11 dpa/(MW-year/m²) and the rates of helium- and hydrogen-production are 216 and 531 appm/(MW-year/m²). As discussed in Section IV-A, the first wall is expected to operate satisfactorily under these conditions for approximately 1 MW-year/m². Therefore, the present design, with a neutron wall loading of 0.2 MW/m², could be expected to operate for approximately 10 years with a duty factor as high as 50% before the replacement of the first wall becomes necessary. The gas-production rates drop by approximately an order of magnitude in the first 15 cm while the rate of atomic displacement decreases by only a factor of six. Replacement of the 15 cm stainless steel regions behind the first wall may be required for extended operation as a radiation test facility. Replacement of other stainless steel zones inside the blanket is not likely to be necessary because of the large decrease in radiation damage-related parameters (gas production and dpa) and the fact that most of the stainless steel in the blanket does not serve any structural purposes.

3. Nuclear Heating and Radiation Damage in the Magnet

Figure VI-7 shows the spatial distribution of nuclear heating in the inner segment of the toroidal field magnet (i.e. along line A-D in Fig. VI-2) in units of watts/cm³ for a neutron wall loading of 0.2 MW/m². The maximum nuclear heating in the magnet conductor is 5×10^{-6} watt/cm³. Assuming the spacing between coolant channels is 10 cm, and using the physical properties of copper and Nb-22Ti at 80 kG and 4.2°K, the maximum temperature rise inside the superconductor is less than 0.05°K. Thus, the nuclear heating could be increased by two orders of magnitude and the spacing between coolant channels adjusted to limit the maximum temperature rise in the superconductor to 1°K without any practical difficulty. Note that the total heating rate in the magnet is the sum of the nuclear heating rate shown in Fig. VI-7 plus the contribution from thermal leakage.

Figure VI-5 Spatial Distribution of Atomic Displacements in Stainless Steel in Blanket/Shield of the ANL-TEPR Design for Integral Neutron Wall Loading of 1 MW-year/m²

Figure VI-6 Spatial Distribution of Hydrogen and Helium Production Rates in the Stainless Steel Regions of the ANL-TEPR Blanket (production rates given in Atomic Parts per Million for an Integral Wall Loading of 1 MW-year/m^2)

Figure VI-7 Spatial Distribution of Heating Rates in the Inner Segment of the Magnet

The nuclear heating decreases by only an order of magnitude within 50 cm in the magnet. Thus the attenuation of the nuclear heating in the magnet is slower than that in the blanket and shield. The magnet consists mostly of copper and stainless steel and contains no strong low-energy neutron absorber, such as boron. Therefore, low-energy neutrons are absorbed in radiative capture reactions in the constituents of stainless steel and copper resulting in significant secondary gamma-ray production. Figure VI-7 shows that about 97% of the nuclear heating is generated by the absorption of these secondary gamma-rays.

The total nuclear-energy deposition in the magnet is 0.14 watts per centimeter in the toroidal direction for a neutron wall loading of 0.2 MW/m^2 . The magnet width, as shown in Chapter IV, is 114 cm; therefore, the total nuclear-energy deposition is 16 watts per magnet and 256 watts for all sixteen magnets.

Figure VI-8 shows the atomic displacement and the radiation-induced electrical resistivity, ρ_r , as a function of the copper stabilizer position in the magnet. The maximum ρ_r is $2.5 \times 10^{-8} \Omega\text{-cm}$ for an integral wall loading of 10 MW-year/m^2 . A fully stabilized conductor designed to operate at 80 kG requires a 25% increase in the amount of copper stabilizer to compensate for this increase in the copper resistivity so that the total stability criterion is kept satisfied. This represents no serious difficulty in a practical conductor design. Since the useful working stress for copper at 4.2°K is about half that of stainless steel, the amount of the stainless steel structure can be reduced by about half the increase in copper. The radiation-induced resistivity in copper decreases to a value of approximately $5 \times 10^{-9} \Omega\text{-cm}$ at a depth of 50 cm in the magnet; this requires less than 5% increase in the amount of copper for 10 MW-year/m^2 .

The TEPR is not expected to operate continuously for 10 MW-year/m^2 of integral neutron wall loading. In addition, the radiation-induced resistivity in copper can be recovered by annealing at elevated temperatures. Since it takes about 50 days to cool down the magnet from room temperature to 4.2°K, it is not desirable to warm up the magnet very frequently. However, there will certainly be down periods for maintenance, replacement of experimental blanket modules, etc. during which magnet warm up can be scheduled. The neutron wall loading for the ANL-TEPR design is calculated to be 0.2 MW/m^2 . Due to a multiplicity of uncertainties in the plasma characteristics, the same design may be capable of producing much more (or less) power than expected (see Chapter III). Furthermore, there is uncertainty in deriving radiation damage-related

Figure VI-8 Atomic Displacements and Radiation Induced Resistivity in Copper (stabilizer) as a Function of Depth within the Inner Segment of the Magnet after Reactor Operation for an Integral Wall Loading of 10 MW-year/m^2

parameters in the magnet because of uncertainties in nuclear data and transport calculations, in addition to uncertainties in translating the neutronics parameters into radiation-damage effects. After the superconducting magnet is built, it will not be possible in many instances to modify it in order to accommodate any unexpected changes in the reactor performance. Therefore, it appears that a prudent design should allow a safety factor of 5 to 10 in neutronics parameters pertinent to the magnet design.

The important impurities produced by transmutations in copper are nickel, zinc and cobalt. The maximum impurity concentration in the copper of the ANL-TEPR magnet is 0.003 appm/(MW-year/m²) most of which is ⁶⁴Ni, ⁶⁴Zn, and ⁶⁶Zn produced by radioactive decay of the products from radiative-capture reactions in ⁶³Cu and ⁶⁵Cu. This level of impurity concentration is a few orders of magnitude lower than that normally present in OFHC copper⁽¹¹⁾ commonly used as the stabilizer in superconducting magnets. Therefore, effects of impurity production in copper on the electric resistivity do not present any problem in the ANL-TEPR design. Figures VI-9 and VI-10 show the spatial distribution of hydrogen and helium production rates, respectively, in appm/(MW-year/m²) in the copper stabilizer, in the NbTi superconductor, and in the stainless steel structure of the magnet.

The ANL-TEPR magnet design uses mylar as the thermal and electrical insulation in the toroidal-field coil windings. Figure VI-11 shows the absorbed dose from nuclear irradiation in mylar as a function of its position inside the inner magnet. The maximum dose in mylar is approximately 4×10^7 rads for an integral neutron wall loading of 1 MW-year/m². Investigation of radiation effects in polymers in air indicate⁽¹²⁾ that mylar and epoxy can operate satisfactorily under irradiation for doses up to 1.2×10^8 and 3×10^9 rads, respectively. Thus mylar can sustain damage in the ANL-TEPR magnet up to an integral wall loading of approximately 3 MW-year/m². For a wall loading of 0.2 MW/m², 50% duty cycle, mylar insulation can perform its function properly for ~ 30 years. It should be noted that replacement of interlayer insulation between the conductor and the structure is not practical. Therefore, the design value for the useful lifetime of the interlayer insulation should be about the same as that of the TEPR plant. Mylar appears adequate in this regard according to the present calculations. However, if radiation damage to the polymeric materials proves more serious at cryogenic temperatures than that at room temperatures (see Appendix D), mylar will have to be replaced by other

Figure VI-9 Spatial Distribution of Hydrogen Production Rates (in atomic parts per million for integral wall loading of 1 MW-year/m²) in Structural Material (stainless steel), Superconductor (NbTi), and Stabilizer (copper) as a function of depth in the Inner Segment of the Magnet

Figure VI-10 Spatial Distribution of Helium Production Rates (in atomic parts per million for an integral wall loading of 1 MW-year/m²) in Structural Material (stainless steel), Superconductor (NbTi), and Stabilizer (copper) as Function of Depth in the Inner Segment of the Magnet

Figure VI-11 Dose Absorbed in Mylar as a Function of Mylar Position in the Inner Segment of the ANL-TEPR Magnet for an Integral Neutron Wall Loading of 1 MW-year/m^2

types of insulators, e.g. epoxy. The useful lifetime of epoxy is about 20 times longer than that of mylar under the same irradiation conditions.

4. Biological Shield

Because of cost and other considerations, the magnet shield is kept to the minimum size that satisfies the magnet protection criteria. This shield is not sufficient to reduce the biological dose to the level allowed for occupationally exposed individuals. Therefore, additional biological shielding, surrounding the walls of the reactor building, must be provided to reduce the biological dose during full power operation to ~ 1 mrem/hr. The biological dose referred to here is based on the elemental composition of a STANDARD MAN and includes both neutron and gamma-ray contributions.

During operation of the ANL-TEPR reference design with a neutron wall loading of 0.2 MW/m^2 , the biological dose at a location just outside the shield in the space between magnets (point E in Fig. VI-2) is 3×10^5 mrem/hr. The biological dose at a location just outside the outer part of the magnet is 2×10^4 mrem/hr. Thus the 57 cm magnet provides for only one order of magnitude reduction in the biological dose. This reflects the fact that the magnet constituents (mainly copper and stainless steel) are not very effective in attenuating neutrons in the keV and low MeV energy range, which constitute the largest component of the neutron spectrum in the magnet. Since most of the neutrons and gamma-rays reaching the walls of the reactor building (inner part of the biological shield) come from the gap between magnets, ignoring the attenuation effects of the magnet provides a conservative basis for the design of the biological shield.

Since space has to be provided outside the magnet for necessary equipment, the neutron and gamma-ray fluxes at the inner surface of the biological shield will be lower than those at the magnet shield. Calculation of this geometry effect awaits more detailed information than is currently available for the reference design, but a crude estimate shows roughly a factor of two reduction in the fluxes. Ordinary portland concrete is relatively inexpensive, and a 97-cm-thick biological shield of concrete is sufficient to reduce the biological dose during full-power operation to 1 mrem/hr. The maximum nuclear-heat-generation rate in the concrete is $\sim 10^{-7} \text{ watt/cm}^3$ and no external cooling of the biological shield is required.

5. Alternative Designs for the Inner Blanket/Shield

In Subsection VI-B-1, the concept of two separate segments for the blanket/shield system was introduced. One segment, called the inner blanket/shield, occupies the region of high-magnetic field, is constructed from very efficient attenuators, and its dimensions are determined only by the magnet protection criteria. The other segment, the outer blanket/shield, is constructed in the low magnetic-field region with materials and dimensions subject to all other constraints such as cost and induced activation, in addition to satisfying the magnet-protection criteria and reducing the overall power requirements of the cryogenic system. The purpose of this subsection is to examine in greater detail the merits of the two-segment blanket/shield system and to investigate alternative materials for the inner blanket/shield.

Results in Appendix D show that a SS-B₄C mixture is superior, in terms of attenuation, to all other material compositions investigated except for W-B₄C and Ta-B₄C mixtures. At current prices, the cost per unit volume of W and Ta is roughly two orders of magnitude higher than that for any other material considered in this study. Constructing the entire blanket/shield system from Ta or W can be easily rejected on the basis of the very high cost involved. However, the use of tungsten or tantalum in the relatively small volume of the inner blanket/shield can be very advantageous.

Table VI-4 gives a description of four alternative designs for the inner blanket/shield. Design I is the reference design discussed in the previous subsection and consists of alternating zones of stainless steel and boron carbide with a total blanket/shield thickness, Δ_{BS} , of 100 cm. Design II is the same as design I except stainless steel is replaced by tungsten in zones 4, 6, and 10. Design III uses tungsten and boron carbide as design II does but with a near-optimal arrangement of W and B₄C. Design IV was obtained from design III by replacing W with Ta on a 1-to-1 volume basis. The pure metals W and Ta were used to approximate the easily fabricated non-magnetic tungsten- and tantalum-base alloys discussed in Appendix D. A reasonable amount of stainless steel was retained in all designs to satisfy structural requirements.

Table VI-5 compares the maximum values of the important radiation-damage parameters in the toroidal-field coil for the four designs. The results show that replacing SS by W in 40% of the blanket/shield volume reduces the radiation damage in the magnet by approximately a factor of five. Using the optimal arrangement of W-B₄C in design III results in an order of magnitude reduction

TABLE VI-4. Description of Alternative Designs for the Inner Blanket/Shield System

Zone	Radial Position, cm	Thickness cm	Density Factor	Design I ^a	Design II	Design III	Design IV
1	0-210	210	0	Plasma	Plasma	Plasma	Plasma
2	210-240	30	0	Vacuum	Vacuum	Vacuum	Vacuum
3	240-241	1	.5	SS ^b	SS	SS	SS
4	241-256	15	.9	SS	W ^d	W	Ta ^e
5	256-261	5	.9	B ₄ C ^c	B ₄ C	W	Ta
6	261-276	15	.9	SS	W	W	Ta
7	276-281	5	.9	B ₄ C	B ₄ C	B ₄ C	B ₄ C
8	281-291	10	.95	SS	SS	SS	SS
9	291-301	10	.95	B ₄ C	B ₄ C	W	Ta
10	301-311	10	.95	SS	W	W	Ta
11	311-321	10	.95	B ₄ C	B ₄ C	W	Ta
12	321-331	10	.95	SS	SS	SS	SS
13	331-340	9	.95	B ₄ C	B ₄ C	B ₄ C	B ₄ C
14	340-397	57	1	magnet (44% SS + 44% Cu + 6% NbTi + 6% He)			

^aReference design, ^bStainless steel, ^cBoron carbide, ^dTungsten alloy, ^eTantalum alloy

TABLE VI-5. Effects of Using Tungsten and Tantalum Alloys
in the Inner Blanket/Shield of the Reference Design

Maximum Values in the Inner Magnet	Design I	Design II	Design III	Design IV
Displacements in Copper, dpa per MW-year/m ²	1.5×10^{-5}	3.0×10^{-6}	1.4×10^{-6}	2.3×10^{-6}
Radiation Induced Revistivity in Copper, Ω -cm for 10 MW-year/m ²	2.5×10^{-8}	5.0×10^{-9}	2.4×10^{-9}	3.9×10^{-9}
Neutron Fluence, n/cm ² per MW-year/m ²	4.7×10^{16}	9.3×10^{15}	1.2×10^{16}	1.5×10^{16}
Dose in mylar, rad per MW-year/m ²	3.7×10^7	7.4×10^6	4.0×10^6	6.2×10^6

in radiation damage to the superconducting magnet components, compared with the SS-B₄C reference design. The radiation level at the magnet is a factor of 6 lower for the Ta-B₄C design (design IV) compared with the SS-B₄C design and about 60% higher than that for the W-B₄C design (design III). Note that radiation attenuation for tungsten is better than that for tantalum on a volume basis, but the latter is better on material-weight basis.

The above results indicate clearly that, for a given level of radiation damage to the magnet components, Δ_{BS} for the W-B₄C can be significantly less than that for the SS-B₄C blanket/shield. A blanket/shield of design type III with $\Delta_{BS} = 84.5$ cm is equivalent in terms of magnet protection to the SS-B₄C reference design. Therefore, using W-B₄C in the inner blanket/shield allows the toroidal-field coils to be closer to the plasma. For the ANL-TEPR reference design this results in 5.4% increase in the magnetic field at the plasma axis and ~ 24% increase in the reactor thermal power as well as a substantial improvement in confinement (see Eqs. VI-1 and VI-2 and Chapter I).

The entire blanket/shield system is conceived to be constructed of several modules for many practical reasons, such as assembly, disassembly, and replacement. Therefore, constructing the inner blanket/shield module from materials different than those used in the modules of the outer blanket/shield should not involve any added difficulty or significant increase in the fabrication cost. However, use of tungsten or tantalum in the inner blanket/shield, instead of stainless steel, involves a cost penalty resulting from the large difference in material cost. This increase in cost depends greatly on the size of the inner blanket/shield. With the geometry parameters in Fig. VI-2, replacing the 100 cm SS-B₄C with the 84.5 cm W-B₄C increases the cost by ~34 million dollars on the basis of material cost of 2, 3, and 70 \$/kg for SS, B₄C, and W, respectively (note that the average major radius for the inner blanket/shield is only 335 cm). This cost is directly proportional to the angle, θ , formed by the boundaries of the inner blanket/shield at the plasma centerline ($\theta = 50^\circ$ in Fig. VI-2). The angle θ must be large enough to reduce the overall power requirements of the refrigerators and small enough to limit the amount of tungsten used. In the present design, θ can be reduced to 30° without increasing the power required to run the refrigerators beyond 1% of the plant electrical power output. The added capital cost of 20 million dollars in this case could probably be justified on the basis of the 24% increase in the reactor power output and the substantial improvement in confinement.

C. Primary Energy Conversion System Thermal Hydraulic Analysis

The primary energy conversion system has four major components that require varying degrees of thermal energy removal. Proceeding in a direction away from the plasma, along the minor radius, the first component is a low-Z liner, which protects the plasma from high-Z impurities dislodged from the vacuum wall (stainless steel). This liner may be integral with the first wall (as a coating) or physically separated from it. (The optimum method is still under investigation.) The next component is the stainless steel vacuum wall, which provides structural integrity for the entire first wall assembly. It is presently a region of stainless steel cooling passages capable of accepting (1) the energy lost from the plasma by radiation and (2) the large heat load passed to it during plasma shutdown at the end of a burn cycle. Beyond the first wall are the blanket and shield regions, each consisting of a series of concentric layers of stainless steel alternating with B_4C . This arrangement of material, which has the dual function of slowing down the neutrons and protecting the superconducting magnets, was determined based on the neutronic considerations given in Section VI-B and Appendix D. The thermal analysis study described in this section was carried out to characterize the removal of thermal energy generated in the first wall and the blanket. In this analysis, two fluids were considered as primary coolants for the blanket region, i.e. 50 atm helium and 136 atm water. Nomenclature for this section is given in Table VI-6.

1. Convective Cooling of the Blanket

The energy deposited on the first wall, due to plasma radiation and heat transport during the burn and at shut-down, is removed by the forced-convection cooling provided in the first wall. This load is not being considered as part of the steady-state nuclear heating occurring in the blanket.

The blanket analysis given here follows closely the work presented in Ref. 13 in which the blanket is assumed to be composed of an aggregate of hollow cylinders, which are heated uniformly and cooled at their inner radius. The governing equation for the steady-state heat removal for such a cylinder is given as

$$\frac{1}{R} \left[\frac{d}{dR} \left(\frac{RdT}{dR} \right) \right] + \frac{\dot{q}'''}{K} = 0$$

with the $T = T_m$ at $R = D/2$

$T = T_w$ at $R = d/2$

TABLE VI-6. Nomenclature for Section VI-C

A	Area
Cp	Specific heat
d	Coolant channel characteristic diameter
f	Friction Factor
F_{1-2}	Interchange coefficient
h	Heat transfer coefficient
K	Thermal conductivity
L	Tube length
P	Pressure
Q	Heat transfer rate
$\bar{q}$	Average heat generation rate
R	Gas constant
Ra	Shield front face radius
T	Temperature
U	Velocity
V	Void fraction
Vols	Liner volume
x	Liner thickness
δg	Stand-off dimension
ϵ	Emissivity
μ	Constant
σ	Stephen-Boltzmann constant
P_w	Neutron Wall Loading

Subscripts:

- 1 - Rear face of liner, inlet
- e - exit
- m - mean
- 2 - First wall
- i - inlet

and the condition of no heat flow at the outer surface is given by

$$\left. \frac{dT}{dR} \right|_{R=D/2} = 0.$$

T_m is the maximum temperature allowed anywhere in the blanket material and T_w is the maximum temperature allowed in the coolant passage wall. D is the outer diameter of the cylinder and d is the inner diameter, i.e., tube diameter. Defining void fraction to be the ratio of coolant-tube crosssectional area to the total crosssectional area, we have

$$V = \frac{\pi}{4} d^2 / \frac{\pi}{4} D^2 = \frac{d^2}{D^2}$$

and the heat generated per unit length of tube is

$$\dot{q}' = \frac{\pi}{4} \dot{q}''' D^2 (1-V).$$

The temperature drop in the blanket material required to remove all of the heat generated by the neutrons is given by

$$\Delta T = \frac{\dot{q}''' D^2}{16K} (1-V+\ln V).$$

Solving for the outer diameter as a function of the assumed temperature drop in the blanket material we have

$$D = 4 \sqrt{\frac{K \Delta T}{\dot{q}'''}} (V-1-\ln V)^{-1/2}$$

and

$$d = D \sqrt{V}.$$

Heat transfer from a tube wall, with fully developed flow existing in the tube, can be approximated using the relation

$$Q = \bar{h} \pi d (T_w - T_b) L$$

where $\bar{h}$ is the average heat transfer coefficient for the tube and T_b is the bulk temperature [$T_b = (T_e - T_i) / \ln(T_e / T_i)$] of the fluid flowing in the tube.

The energy added to the flowing fluid is given by the relation

$$Q = \rho C_p U_m \frac{\pi d^2}{4} (T_e - T_i)$$

where U_m is the mean velocity in the tube, T_i is the entrance temperature and T_e is the exit temperature. The ratio of these two quantities gives rise to the Stanton number, St , as follows

$$St = \frac{h}{C_p U_m} = \frac{Nu}{RePr} = \frac{d}{4L} \frac{(T_e - T_i)}{(T_w - T_b)} .$$

The flow length, L , is then given by

$$L = \frac{d}{4St} \frac{(T_e - T_i)}{(T_w - T_b)} .$$

The flow rate that is required to remove the heat generated in the cylinder then becomes

$$FLOW = L \dot{q}' / C_p (T_e - T_i)$$

and the pressure drop and pumping power for a perfect gas follow directly

$$\Delta P = \frac{32f}{\pi^2} \frac{RT_{avg}}{P_1} \frac{(FLOW)^2 L}{d^5} .$$

The preceding analysis deals with a single tube. To analyze the entire blanket a computer program was written for a set of concentric cylinders each of which has different thermal properties and heat-generation rates. N tubes were then assigned to each cylinder to insure that all of the heat generated was actually removed (conservation of thermal energy). Following the establishment of the number of cooling passages required in each ring of material, the thermal power and pumping power were computed. The program was written with void fraction as the independent variable. In this way the tube diameter could be held constant in each of the blanket sections. Figure VI-12 represents the variation of tube diameter as a function of void fraction, using power density as a parameter. This information is used to establish the expected void fraction in each of the blanket subsections. Figure VI-13 gives the results of the analysis for a preliminary, 30 cm stainless steel TEPR blanket. Three different tube diameters were investigated using helium while

Figure VI-12 Void Fraction versus Tube Diameter for the TEPR Preliminary Design

Figure VI-13 Void Fraction versus Radial Distance from First Wall for the TEPR Preliminary Blanket Design

only one diameter was investigated for pressurized water*. The expected void fraction is given for the first 30 cm of blanket thickness since approximately 90% of the energy deposited in the blanket is removed from the first 30 cm. Results of these preliminary analyses of the TEPR blanket are summarized in Table VI-7. A conservative wall loading of 0.3 MW/m^2 was used.

2. Analysis of the First-Wall Liner

Although the exact mechanical arrangement of the low-Z liner relative to the first wall has not been determined, a preliminary analysis was undertaken to determine if a stand-off arrangement is feasible. The concept of a thin, radiatively-cooled thermal liner was analyzed in an approximate fashion to determine the influence of material thickness and composition and to establish the upper bound above which forced-convection cooling may become necessary. Figure VI-14 is a schematic representation of the liner placed between the plasma and the first structural wall. The first wall is assumed to be held at a temperature $T_2 = 900^\circ\text{K}$, the upper operating limit of stainless steel.

The analysis developed for the radiatively cooled liner assumes that all the heat generated in the liner due to nuclear heating plus 20%** of the neutron flux incident upon the front face of the liner must be transferred by radiation to the first wall at temperature T_2 . The distribution of heat generated in a thick liner is given by the approximate relation $\dot{q}(x) = \dot{q}(0)e^{-\mu x}$ where $\dot{q}(0)$ is the internal heat generation rate at the front face of the liner. Values of $\dot{q}(x)$ are established through the nuclear heating calculations described in Section VI-B. Using these values, an average heating rate for any thickness of liner can be computed and is given by the expression

$$\bar{\dot{q}} = \frac{\dot{q}(0)}{x} \left(\frac{1 - e^{-\mu x}}{\mu} \right)$$

The transfer of heat by radiation between two concentric cylinders having a temperature T_1 and T_2 respectively is given by the expression⁽¹⁴⁾

$$\dot{q} = F_{1-2} \sigma (T_1^4 - T_2^4)$$

* For water, it is coolant volume fraction, rather than void fraction, which is calculated.

** This is an approximation of the alpha-particle and bremsstrahlung radiation energy emanating from the plasma.

TABLE VI-7. Thermal Fluid Analysis Parameters for the FEPR Preliminary Blanket Design

Parameter	Coolant	
	Water ^a	Helium ^b
Thermal Power Removed, ^c MW	186 ^f	186 ^f
Coolant Inlet Pressure, ^c atm	136	50
Pressure Drop in Blanket, ^e atm	<1	~1
Coolant Channel Diameter, ^c cm	1.0	2.5
Pumping Power for Blanket, MW	<1	~7
Coolant Inlet Temperature, ^c °C	38	357
Coolant Exit Temperature, ^c °C	302	527
Maximum Blanket Temperature, ^c °C	600	600
Tube Wall Temperature, ^c °C	327	550
Number of Radial Zones ^c	6	6
Coolant Velocity	(d)	(d)

^aParameters consistent with PWR Technology.

^bParameters consistent with HTGR Technology.

^cParameters fixed in calculations.

^dIncompressible flow was assumed, i.e., the coolant velocity was kept at <0.3 x sonic velocity.

^eAverage pressure drop per tube.

^fBased on a conservative overestimate of the neutron wall load of 0.3 MW/m².

Figure VI-14 Radiatively-Cooled Liner Concept

where F_{1-2} is an interchange factor, σ is the Stephen-Boltzman constant, and $\dot{q}$ is the net heat flux leaving area A_1 . The heat flux is given by the expression

$$\dot{q} = \frac{\text{Vols } \bar{q} + 0.2A_1 P_w}{A_1}$$

and F_{1-2} is given by

$$\frac{1}{F_{1-2}} = \frac{1}{\epsilon_1} + \frac{(R_a + x)}{(R_a + x + \delta)} \left(\frac{1}{\epsilon_2} - 1 \right).$$

For a known T_2 the rear face temperature of the liner becomes

$$T_1^4 = T_2^4 + \left[\frac{R_a + x/2}{R_a + x} \right] \frac{\dot{q}(0)}{\mu} \left(\frac{1 - e^{-\mu x}}{\sigma} \right) + \frac{0.2P_w}{\sigma} \left[\frac{1}{\epsilon_1} + \frac{(R_a + x)}{(R_a + x + \delta)} \left(\frac{1}{\epsilon_1} - 1 \right) \right].$$

The limiting operating temperature, beyond which the vapor pressure of the materials is large enough to compromise reactor performance, is reached at the front face of the liner. This temperature can be estimated using the relation

$$T_F = \frac{\dot{q}x}{K} + T_1$$

where $\dot{q}$ is the total heat flux leaving the rear face of the liner. This limiting temperature is taken to be in the range between 2000°C and 2200°C for graphite and 1500°C to 1700°C for silicon carbide.

These equations were applied to three candidate materials; namely, commercial grade graphite, pyrolytic graphite, and silicon carbide. The thermal properties were taken from Ref. 15. The front- and rear-face temperatures are tabulated in Table VI-8 and VI-9 as a function of liner thickness for two neutron wall loads of 0.1 and 1.0 MW/m². At 0.1 MW/m² silicon carbide is limited to 10 cm or less if the maximum front-face temperature is to be below 1500°K. Graphite does not appear to be limited at this wall loading. At 1 MW/m² the silicon carbide is limited to a maximum thickness of about 5 cm with the graphite thickness only somewhat greater. These results indicate that radiation cooling is adequate provided the wall flux is below 1 MW/m². However, this is the case with the TEPR ($R = 6.25m$, $P_w \approx 0.2 \text{ MW/m}^2$), hence, the uncooled stand-off liner could find an application in this reactor.

TABLE VI-8. Maximum Temperature-Radiatively Cooled Liner
(Wall Loading = 0.1 MW/m^2)

Material	Liner Thickness $X(\text{m})$	First Wall $T_2(^{\circ}\text{K})$	Liner Rear Face $T_1(^{\circ}\text{K})$	Liner Front Face $T_F(^{\circ}\text{K})$	Thermal Conductivity K
Silicon Carbide	0.001	900	1048	1048	25 watt/m- $^{\circ}\text{K}$
	0.01	900	1086	1090	
	0.1	900	1274	1543	
	0.2	900	1341	2086	
Graphite (commercial)	0.001	900	1046	1046	20 watt/m- $^{\circ}\text{K}$
	0.01	900	1070	1072	
	0.1	900	1208	1383	
	0.2	900	1272	1794	
Pyrolytic Graphite	0.001	900	1046	1046	100 watt/m- $^{\circ}\text{K}$
	0.01	900	1070	1070	
	0.1	900	1208	1243	
	0.2	900	1272	1376	

TABLE VI-9. Maximum Temperature-Radiatively Cooled Liner
(Wall Loading = 1 MW/m^2)

Material	Liner Thickness $X(\text{m})$	First Wall $T_2(^{\circ}\text{K})$	Liner Rear Face $T_1(^{\circ}\text{K})$	Liner Front Face $T_F(^{\circ}\text{K})$	Thermal Conductivity K
Silicon Carbide	0.001	900	1575	1576	25 watt/m- $^{\circ}\text{K}$
	0.01	900	1648	1724	
	0.1	900	2126	4815	
	0.2	900	2268	9724	
Graphite (commercial)	0.001	900	1570	1570	20 watt/m- $^{\circ}\text{K}$
	0.01	900	1638	1662	
	0.10	900	1981	3736	
	0.20	900	2122	7345	
Pyrolytic Graphite	0.001	900	1570	1570	100 watt/m- $^{\circ}\text{K}$
	0.01	900	1638	1643	
	0.10	900	1981	2332	
	0.20	900	2122	3167	

A complete design study will be made during the coming year of the energy conversion thermal-fluid systems. The two coolant options will be investigated in an effort to establish optimum performance of each, so that the most promising can be chosen for further development. The analysis will include the sizing and placement of cooling channels in the blanket, placement of supply manifolds, and preliminary analysis of those aspects of blanket design relating to facility remote maintenance.

If the TEPR is operated in a pulsed mode, e.g. approximately 50 seconds of burn followed by 15 seconds of fueling and ash removal, the heat-transfer process in the blanket will be transient and will result in temperature fluctuations in the energy conversion systems. It is not at all clear that the thermal inertia of the blanket and the intermediate heat exchanger (if there is one) will be sufficient to damp these temperature variations. Large fluctuations would cause excessive thermal stresses in the primary energy converter and could compromise the performance of the secondary conversion machinery. During the coming year efforts will be made to develop a dynamic model of the energy conversion system to establish its thermal stress environment and the performance penalty associated with pulsed operation.

D. Engineering Analysis of the Primary Energy Conversion System

1. First-Wall System Engineering Description

The initial design of the toroidal vacuum chamber (described in Section VI-A) depicts a stainless steel vessel, approximately 1 cm thick, with an applied low-Z coating or a separated low-Z liner. For assembly purposes, the 2.4 meter minor diameter torus is divided into 16 segments joined with mechanical joints, which can be formed remotely. Each segment (Fig. VI-1) will contain two 90 cm vacuum ports mounted top and bottom on the vertical midplane. Eight of the segments will also have two dual apertures for neutral-beam injection ports (Fig. VI-15). Two or more segments will contain flux-breakers. Coolant (helium or water) will be available at the outer wall. Support of the chamber will be afforded through the blanket/shield manifold regions or through the bottom outer blanket/shield. The design will include provision for remote maintenance and replacement of all first-wall components.

Figure VI-15. ANL-TEPR Horizontal Section View

During the coming year, the engineering design effort for the toroidal chamber will assess all aspects of the following major items.

(a) First Wall

Geometrical configurations for three or more types of toroidal segments will be established utilizing compatible materials selected from the first-wall development effort and will include identification of a credible fabrication and assembly process. This basic configuration will be reviewed in detail for joining techniques, thermal-hydraulic configurations, vacuum and neutral-beam appurtenances, structural supports, and blanket interfaces. The entire first-wall system will be designed with due consideration for remote maintenance and replacement.

(b) Toroidal Joints

Investigation and selection of designs that will satisfy vacuum seals of both metallic and insulator capability are required. These seals will be made remotely and access to them provided. Existing technology will be taxed heavily in an effort to develop a satisfactory insulative flux-breaker coupling/seal component. A wall-thinning or line-contact type of non-conductive joint will serve as an alternative approach. Current plans include investigation of blind aluminum-oxide gaskets and aluminum-oxide coatings that are shielded from direct plasma radiation.

Presently, it is intended to make the flux-breaker joints separate from the nominal 16 assembly joints. The assembly joints showing the most promise are the Marmon-type seals, utilizing peripheral V-wedge flanges and couplers. These units are especially suited for large, thin members that require remote assembly capability. Remote welding is a backup possibility.

(c) Access Ports

A total of 52 ports (in addition to diagnostics and small experimental ports) are contemplated to serve the following purposes:

32 Vacuum Ports - 0.9 meters ID, circular

16 Dual Neutral Beam Ports - 2 meters x 1 meter, irregular polygon

4 Experimental Access Ports - 1.5 meters ID, circular

Nozzles for each of the above ports will terminate in an internal thin-necked flange with provisions for metallic gaskets with fasteners accessible from inside the aperture. The height of these port stubs will be kept minimal and

extension ducts will be used to carry the aperture through the blanket/shield region. All seals and fasteners will be readily replaceable. The port nozzles will be designed to withstand the structural and thermal stresses. Special consideration will be given to cooling of the nozzle transition zones. Vacuum- and neutral-beam port shutters will be integral with the extension ducts.

(d) Heat Removal

Heat exchange surface will be supplied along with coolant channels for the first-wall, heat-removal system. This, with appropriate coolant manifolding, will be integrated with and run through the blanket.

(e) Structural Support

At this time, it is not clear whether the first-wall vessel will be supported by or through the blanket/shield. Whichever method is utilized will depend upon relative thermal expansion and coolant arrangements, and the various forces exerted both at rest and during the pulsed operation.

(f) Maintenance and Repair

All portions of the first-wall chamber will be remotely replaceable; however, since this vessel is relatively inaccessible, every effort will be made to supply remote inspection and repair capability. Four horizontally-positioned access/experimental ports of 1.5 meter diameter are contemplated for major inspection and repairs. A master robot manipulator with attached television camera could enter these ports and perform most types of foreseeable repairs. The vacuum ports will be designed to provide additional convenient access for inspection and repair.

2. Blanket-Shield Engineering Analysis

The major engineering considerations that are to be addressed in the design of the blanket/shield system deal with the material compatibility, thermal expansion, and structural support needed to meet the functional requirements of all components. As shown in Fig. VI-2, there are four regions of unique design, the inner blanket and inner shield (Zone ABC) and the outer blanket and outer shield (Zone EFGH). The following items will be covered in the engineering design:

(a) Blanket and Shield

A heat exchanger concept will be determined for each of the four regions mentioned. These will be distinct segments selected on the basis of heat-generation rates, type of coolant, thermal hydraulics, layout geometry, material requirements, fabricability and assembly techniques. They will also be designed for remote maintenance and replacement and, in addition, for replacement with test sections of improved or advanced breeding or energy conversion capability.

Current studies encompass three types of heat exchanger structures, namely: (1) units made of pipes and tubes carrying coolant; (2) sections made of solid stainless steel, or of steel clad* tantalum (or tungsten) alloy blocks with drilled coolant-channel holes; and (3) fabricated sections of various materials with interconnecting coolant duct work. The initial TEPR concept favors the latter approach where annular portions of stainless steel formed between layers of boron carbide (see Fig. VI-2) will carry the primary coolant (helium or water). Four segments of blanket composition will form the blanket annulus and no fewer than four lengths of blanket section will be joined together for a hydraulic circuit serving 25% of the toroid. A similar arrangement of shielding segments will encompass the blanket regions adding the necessary neutron and gamma attenuation required for protection of the superconducting magnet.

Each segment is fabricated so that it may be fitted systematically into position and hydraulically sealed to its adjoining circuit unit. Blanket irregularities caused by neutral-beam penetrations, vacuum ports and other appurtenances to the plasma region will be accounted for. All maintenance and repairs are planned using remote-handling equipment.

(b) Coolant Lines

Methods for supply and removal of coolant from the inner and outer blanket/shield segments will be developed during the coming year. Detailed designs of manifolding with appropriate connectors, seals and instrumentation will be made with allowance for remote maintenance and repair.

(c) Support Structures

Detailed design of the individual segment and total support structures for the blanket/shield system will be provided. These structures will be reviewed for both static and dynamic (magnetic) loadings along with thermal expansion

*Cladding of channel holes in tantalum or tungsten will be necessary to protect the refractory metal blankets from erosion by the coolants selected, i.e., helium or water.

from shutdown to hot operating conditions. Major requirements for interface with the first-wall system will be of prime consideration for this design. Stresses that develop from various short-cycle phenomena, like plasma shutdown, will be examined.

(d) Experimental Ports

Four 1.5 meter diameter ports are provided for inspection, maintenance and repair of the first-wall vessel. These access ports (see Fig. VI-15) will provide space for removable test and instrumented sections of the blanket and shield. Alternative materials, geometries, coolants, breeding media, etc. may be installed in these same access areas for test purposes.

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CHAPTER VII

TRITIUM PROCESSING AND CONTAINMENT

This chapter contains a description of the results of a preliminary analysis of the tritium handling and containment requirements for the TEPR design approach outlined in Chapter II. The initial stage of operation (Stage I) calls for a device with a primary energy conversion system (PECS) that provides for sensible heat removal and adequate shielding of the confinement systems, but does not contain a tritium-breeding material. In the second stage of operation (Stage II), one or more modules or test sectors are inserted into the original (Stage I) device to verify promising tritium breeding and recovery concepts. It is anticipated that the extended operation (through Stage II) of the design approach described in Chapter II will force the demonstration of all aspects of tritium handling, containment, and control technology eventually needed for full-scale fusion power reactors. In addition, since tritium production at a breeding gain greater than unity is not a planned objective of this device, the bulk of the tritium required for fueling will have to be supplied by an external source. The following sections of this chapter address in order (1) the fueling requirements and prospects for their fulfillment, (2) the status of conceptual analyses of an integrated tritium handling facility for the TEPR and (3) the important major research and technology development areas that have been identified thus far. A bibliography of recent publications⁽¹⁻¹⁶⁾ that are pertinent to the subject of this chapter follows Section VII-C.

A. Fueling Requirements, Fuel Supply, and Tritium Inventory

The continuous operation of a TEPR at a cycle averaged power of ~100 MW_e will result in the consumption (burn-up) of roughly 16 grams of tritium per day (assuming 16.5 MeV per D-T fusion reaction). Since the total amount of tritium introduced into the plasma chamber during continuous operation could range anywhere from 30 to upwards of 100 times the quantity consumed (depending on the fractional burn-up achieved, the extent to which fuel particles diffusing from the plasma are recycled back into the plasma during the burn, and the amount of cold fuel that is used as a gas blanket), the cumulative amount of tritium passing any point in the mainstream fuel cycle may reach 1600 gm per day. The magnitude of the total tritium inventory on hand during operation of

the TEPR (including that in storage) depends on a variety of factors which include cyclic residence times in the processing equipment, procurement schedules, shipping requirements, licensing agreements, and contingency considerations. A value for this inventory has not been firmly established for the present design, but at this time it seems essential to have in excess of one day's maximum throughput (~2000 gm) on hand. More extensive study of this question may eventually show that lower inventories are reasonable, particularly if cyclic residence times in the fuel processing equipment can be reduced to, at most, a few hours.

Although no quotable figures are available, the assumption is made that current tritium production capabilities in the U. S. are adequate to meet the fueling needs of TEPR. Taking 3000 to 9000 \$ per gram (delivered) as a reasonable cost range for the large quantities of tritium required by TEPR, the annual fueling cost for production of 100 MWt at a plant factor of 30% would fall in the range from 5 to 15 million \$. While at first thought this may seem like a large annual expenditure for an experimental reactor, it probably amounts to less than 2% of the total construction cost of the entire TEPR facility, and could be more than offset by the savings realized from (1) reduced PECS complexity, (2) extended time scale for breeder-blanket development, and (3) inherently more reliable PECS operation. Furthermore, this cost range probably only represents an upper limit, since it may not be necessary to operate the TEPR at much more than a 15% plant factor in order to demonstrate all of its primary objectives.

Based on the projected tritium consumption figures given at the beginning of this section, the corresponding deuterium burn-up rate would be ~11 gram per day for continuous operation at the 100 MWt cycle averaged power level. Existing production facilities that supply deuterium (e.g., for heavy water reactors) appear to have capabilities far in excess of those required by the TEPR. Current costs for high-purity (99.5+%) deuterium gas in laboratory-scale quantities run around 4 \$ per gram. This extrapolates to an annual deuterium supply expenditure of ~5000 \$ (assuming a 30% plant factor) -- an insignificant cost compared to that associated with tritium supply.

Based on the above assumptions for cycle averaged power and fuel-throughput, the maximum fueling rate (D + T) could approach 2700 gm per day. Description of a processing system to handle this flow rate is given in Section VII-B-3.

B. Tritium Processing and Containment Facility

As part of the TEPR design activity, an effort was initiated to assess the tritium processing requirements for a TEPR. For the purposes of this assessment, the combined tritium processing facility, currently envisioned for a TEPR, has been divided into seven major systems. These systems are listed in Table VII-1 along with the primary functions of each system that have been identified thus far. The first six systems listed in Table VII-1 are required for Stage I operation, hence, their development in an orderly fashion consistent with the design and construction of the first TEPR is essential. In addition to those tritium handling systems needed for Stage I operation, the second stage (Stage II) will require a blanket-processing system, the main purpose of which is to recover tritium from the breeding medium. A schematic diagram of the mainstream fuel cycle, which includes most of the features of the first four systems listed in Table VII-1, is given in Fig. VII-1. Current design considerations and the anticipated scope of near-term assessment activities for all of the systems in Table VII-1 are given below:

1. Fuel-Delivery System

The function of this system is vital to sustained operation of reactor level plasmas in a TEPR. A substantial fraction of the total quantity of D-T fuel involved in a single burn cycle must be delivered to the plasma after initiation of the discharge. (The amount delivered is highly dependent on the extent to which fuel particles lost from the plasma are recycled back into the plasma during the burn.) Fueling schemes that involve either high speed injection of solid D-T pellets or application of a cold D-T gas blanket are leading candidates. Whereas the delivery of a uniformly distributed spray of fuel (gas blanket) to the plasma chamber appears to be relatively straightforward, a number of tritium-containment problems could develop as a result of the intricacies and/or inefficiencies associated with pellet formation and injection. Special features of each of these fueling schemes that are necessary to insure efficient recycle and adequate containment of tritium will be evaluated, and estimates of the additional costs associated with these special features will be made. Within the context of this system, the consequences of tritium backflow into the neutral beam injectors will be investigated.

TABLE VII-1 Tritium Handling and Control Systems for TEPR

<u>System</u>	<u>Functions</u>
Fuel-Delivery System	<ul style="list-style-type: none"> Condition fuel for injection Provide controlled injection of DT mixture into plasma
Fuel-Circulation System	<ul style="list-style-type: none"> Provide interface with fuel-storage system Evacuate plasma chamber and collect exhaust Transport exhaust gas to fuel-processing system Transport processed fuel to fuel-delivery system
Fuel-Processing System	<ul style="list-style-type: none"> Remove helium and other impurities (e.g., O, N, C, etc.) Provide isotopic enrichment <ul style="list-style-type: none"> i. Remove protium (^1H) ii. Adjust D/T ratio
Fuel-Storage System	<ul style="list-style-type: none"> Provide fail-safe fuel storage
In-Plant-Containment System	<ul style="list-style-type: none"> Primary -- low permeability structural materials Secondary -- double wall purge jacket Tertiary -- reactor hall purge
Purge-Processing System	<ul style="list-style-type: none"> Remove tritium from purge streams Concentrate for disposal or recycle
Blanket-Processing System	<ul style="list-style-type: none"> Remove tritium from blanket material Control impurities that limit processing and recycle efficiencies

Figure VII-1 Preliminary Fuel-cycle Schematic for the TEPR

2. Fuel-Circulation System

The tritium handling facility for the TEPR is comprised of a network of systems which either operate on or store the fuel at various stages of the fuel cycle. The subsystems and components that fall under the fuel-circulation system are those which provide the interfaces between the other six tritium handling systems, e.g., all interconnecting ductwork and piping, compressors, circulation pumps, the vacuum pumping equipment, numerous intermediate conditioning steps, monitoring equipment, the overall-facility control center, valves, by-passes, etc.

From the standpoint of fuel-cycle integrity and maintainability, it seems desirable to incorporate one or more levels of parallel redundancy for components that are susceptible to failure. For example, the valves in the main-stream fuel cycle will be remotely operated (either electrically or mechanically) and will not be generally accessible while fuel is cycling. These valves could be the type designed to fail in the closed position, and groups of such valves could be ganged together in parallel combinations thereby avoiding any instantaneous disruption of the fuel cycle. Pumps and compressors should likewise be arranged redundantly and should be of the sealed variety with magnetically coupled drives wherever possible.

Over the coming year an intensive study will be conducted to identify the methodology for performing all of the diverse functions that are herein considered collectively as the fuel-circulation system. This study will include (1) evaluations of existing technology, (2) costing and sizing of individual components, (3) definition of criteria for the design of a computerized fuel-cycle control center, and (4) development of a detailed parameter list commensurate with a representative design for the TEPR.

3. Fuel-Processing System

The functions of this system are listed in Table VII-1 and many of them are illustrated schematically in Fig. VII-1. The tentative view of this system's operation, given in Fig. VII-1, shows that the D-T fuel, the gaseous impurities (e.g., H, He, O, N, C), and any particulate-size debris (e.g., from first-wall erosion) released during regeneration of the cryosorption pumps (see Section V-B) are compressed and cycled (1) through a fines-filter/hot trap designed to remove all impurities except D, T, H and He, (2) through a cold trap to remove any condensible species that might plug the

cryogenic still, and (3) through the cryogenic still to separate He and H from the bulk of the D-T mixture. The enriched D-T mixture coming from the still bottom passes a check point where its purity is monitored continuously. If the level of protium or any other impurity exceeds an acceptable value, provision is made for diversion of the contaminated D-T mixture to the appropriate processing step. The effluent from the top of the still (mostly He and H) is likewise monitored continuously for tritium content and is directed to the appropriate enrichment and/or processing level of the purge-processing system (described in Section VI-B-6). Sufficient redundancy will be provided, such that no more than half of the trapping equipment and the distillation units are required to maintain the fuel cycle during periods of continuous operation. The composition of the active material in the fines-filter/hot trap will be selected to give adequate impurity entrainment or trapping with minimum hold-up of tritium. The relative merits of regenerable versus disposable traps will be examined in the coming year.

The mainstream fuel enrichment method which appears most promising, based on studies performed thus far, is low temperature cryogenic distillation using cascades of parallel plate or packed bed columns.⁽¹⁵⁾ Design and operation of a 150 gram per hour unit should be well within the reach of existing hydrogen isotope distillation technology. Several of these units could be located in adjoining rooms for maintenance purposes. (Any one of the units would be capable of handling the maximum daily fuel cycle load.) Important performance characteristics of the mainstream enrichment cycle which need more detailed study at this time include (1) the extent (if any) to which isotopic separation of deuterium and tritium will be necessary, (2) the impact of helium in the fuel stream on still operation, and (3) special features of the distillation unit required as a consequence of the presence of tritium. During the coming year, at least three alternative mainstream fuel enrichment schemes will be examined, i.e., (1) the scheme shown in Fig. VII-1 with a hydrogen permeable membrane in advance of the still to remove helium prior to distillative enrichment, (2) replacement of the cryogenic still with a multistage permeable-membrane enrichment step, and (3) laser-excited isotopic enrichment.

4. Fuel-Storage System

Various approaches to the provision of fail-safe fuel storage and storage access are being investigated. The illustration in Fig. VII-1 shows a

barricaded storage block housing separated, independently controlled cells that contain a high-capacity hydrogen getter (e.g., La_5Co , U, Ti). It is conceivable that enough redundancy could be built into the cell controls to permit permanent encasement of the storage block, but this approach will have to be subjected to more careful study. The barricade in Fig. VII-1 has incorporated in it a quenching system to permit rapid reduction of cell temperatures in the event of an incident that could compromise the storage of tritium.

The three principal access requirements for the storage vault are (1) reception of the enriched D-T mixture from the cryogenic still, (2) reception of incoming tritium from the production facility, and (3) release of fuel to the fuel-delivery system. The barricaded vault in Fig. VII-1 is intended largely for tritium storage, but, of course, with time the fuel coming from the storage cells will be progressively enriched with deuterium. In order to assure proper fuel composition prior to delivery to the plasma chamber, the mainstream fuel cycle includes a fuel-blender as shown in Fig. VII-1. It will also be necessary to have the incoming tritium and outgoing D-T mixture pass through a permeable membrane (1) to prevent introduction of contaminants which could ultimately reduce the capacity of the getter-material in the storage cells and (2) to filter off any ^3He (from tritium decay) that builds up in the storage cells. A method for removing ^3He from the storage cells will be included in a later design.

Included in the assessment of the fuel storage system will be those considerations which are pertinent to fail-safe transportation of (1) tritium from a distant production facility to the TEPR site and (2) tritium-contaminated wastes from the TEPR site to a disposal site. Prospects for near-term development of the required transportation, storage, and disposal facilities (based on existing technology) look promising.

5. In-Plant-Containment System

Current thinking on this system suggests that consideration be given to three levels of tritium containment (see Table VII-1). Where it is possible to do so, construction of all components that come into direct contact with tritium should be made with low-permeability materials (e.g., selected alloys, metal composites, ceramic barriers), particularly in elevated temperature ($>300^\circ\text{C}$) regions. It may be necessary to provide an external, closely-fitted jacket with provision for in-jacket purging around those tritium facility

components that (1) are susceptible to failure, (2) operate at elevated temperature, and/or (3) contain relatively large quantities of tritium. In addition, the main reactor hall and the other in-plant rooms that house tritium handling systems will be designed to include hermetic sealing (from the environment) and an inert, processible atmosphere.⁽³⁾ The walls, floors, and ceilings of the TEPR facility represent an enormous surface area; hence, these surfaces should be constructed with or coated with materials that resist tritium adsorption and impregnation. If it proves advantageous to couple the primary coolant to a conventional steam turbine, problems associated with large-scale tritium migration through the blanket structure and the coolant circuits and into the steam generator discharge must be addressed, as must the additional tritium containment problems that arise during Stage II operation with a lithium-containing blanket.

The cost and relative cost-benefits of the three containment levels discussed above will be evaluated in parallel with the systems scoping and design activities for the in-plant-containment system.

6. Purge-Processing System

The principal function of this system is to remove tritium from both the jacketed and whole-room purge streams and, insofar as possible, provide for tritium recycle. Besides removing tritium from these purge streams under normal operating conditions, it will have to be capable of handling all plausible forms of high-level release.⁽³⁾ For the purposes of the present design (Chapter II), consideration has been given to three levels of purge-processing coupled with two levels of isotopic enrichment (in addition to the mainstream fuel enrichment step). Effluents from the purge streams and tritium-containing by-products of other in-plant operations (e.g., the mainstream fuel enricher and the fuel-delivery equipment) are subjected to various levels of purge-processing and enrichment, depending on the T/H ratio of the effluent.

A diagram illustrating the principal steps currently envisioned for the purge scrubbing and enrichment cycle is shown in Fig. VII-2. The need for a fairly elaborate purge processing system is predicated on the assumptions that (1) the TEPR should demonstrate scrupulous tritium containment, (2) excessive tritium contamination of room atmospheres and exposed equipment surfaces will severely hamper maintenance operations, (3) it will be difficult to control

Figure VIII-2. Purge Processing and Enrichment Cycle for the TEPR

the many pathways for protium insertion into the mainstream fuel cycle and the purge streams, (4) for reasons of both economy and environmental impact it will be advantageous to maximize the recycle of tritium that escapes the mainstream fuel cycle. During the coming year, the credibility of each of these assumptions will be carefully examined. Existing tritium decontamination,^(2, 3,11,12) disposal, and enrichment technology (e.g., at Mound Laboratory, Savannah River Laboratory, and elsewhere) will be evaluated in the context of the projected needs of a TEPR; and the costs and cost-benefits associated with the various levels of purge scrubbing and tritium enrichment will be subjected to detailed assessment.

7. Blanket-Processing System

This system is essential to Stage II operation; its major functions are listed in Table VII-1. During recent years, considerable attention has been given to tritium recovery from various potential blanket materials; the literature that has evolved on this subject is being reviewed. During the coming year, an attempt will be made to identify, in the broadest sense, all of the blanket processing requirements of a TEPR.

Appendix C-4 contains a summary of the results of an assessment of various blanket material options for a D-T fusion reactor. Based on the conclusions of this assessment (given in Appendix C), two separate Stage II blanket designs will be evaluated -- one employing liquid lithium, and the second employing a solid lithium alloy or compound. For the liquid lithium blanket design, consideration will be given to two alternative cooling schemes (see Section VI-B-4) -- one which uses liquid lithium as the breeder-blanket and coolant and a second which employs pressurized helium as coolant for a stagnant lithium blanket. (In the stagnant blanket scheme, a relatively slow flow of the lithium will be maintained for processing purposes only.) For the solid blanket design, examinations will be made of both packed-bed and fluidized-bed operation, wherein an inert gas is used to sweep tritium out of the blanket as it is released from the bed particles. Two approaches to the problem of tritium recovery from liquid lithium will be investigated -- countercurrent extraction using a molten salt⁽¹⁴⁾ and scrubbing of the lithium in a fluidized-bed containing a solid hydrogen getter.⁽¹⁶⁾ Tritium recovery from the inert gas used to sweep the solid blanket is expected to be performed in a manner similar to that described in Section B-6. for the purge-processing system.

C. Major Research and Development Needs In Tritium Technology

During the course of (1) a continuing review of existing literature (*viz.*, references 1-16) and (2) the effort described herein to provide a scope for the design of a TEPR tritium handling facility, a lengthy list of necessary research and development items has evolved. Based on this list, a program plan has been formulated for development of the tritium containment and control technology that will be needed to establish a final design for the TEPR and to support its implementation. This plan is given in Appendix I. A number of specific items from the program plan, that are considered to be essential even to near-term TEPR design studies, are summarized below. It is important that work on most of these items begin at the earliest possible time, in order to assure credible design descriptions for all of the tritium handling systems by the end of FY-1977.

1. Research and Method Development

(a) A thorough survey of the literature on tritium handling and containment practices should be completed within the next two years. Particular emphasis should be placed on searches of the classified literature, followed by appropriate declassification action, wherever it is possible to do so from the standpoint of national security. The survey should be carried out in the context of the needs of a TEPR, and its results should be made available to the TEPR design groups in a timely fashion, even as the search itself proceeds. It would be advantageous to have this survey conducted by knowledgeable persons, e.g., at Mound Laboratory, at Savannah River Laboratory, and at other appropriately selected organizations. Reference 2 represents a good start for this survey activity, but in the face of restrictions on dissemination of much of the information pertaining to tritium, it can hardly be considered complete.

(b) There is need for the development of reliable costing and cost-benefit models that are capable of predicting the relative benefits and total costs associated with various approaches to tritium handling, containment, and control requirements for a TEPR. These models should take account of (1) all the mainstream fuel cycle, fuel storage, and fuel transportation equipment, (2) all aspects of the construction of the TEPR that accrue to the presence of tritium and the stringent demands for its economical handling and its containment (e.g., hermetically-sealed rooms, double-jacketing, low-permeation and low-leak construction, complete closed-cycle operation), and (3) all non-obvious tritium

contamination threats (e.g., cycle control malfunction, back-flow into the neutral beam injectors). (c) Insofar as it is possible to do so without benefit of large-scale TEPR system simulation experiments, a thorough analytical study should be made of the significant pathways for protium insertion into the mainstream fuel cycle and other high tritium-content circuits. The outcome of this study is expected to provide a basis for evaluating the magnitude of tritium enrichment required for efficient fuel cycle operation in a TEPR.

2. Technology Development

(a) It is expected that the survey of literature, classified or otherwise, recommended above, will reveal the status of hydrogen isotope enrichment technology. If this technology appears inadequate or inapplicable to the tritium enrichment needs of a TEPR, work should begin immediately to augment existing methods or develop new ones. (b) It would be desirable to begin identifying low-cost, easily fabricated metals and alloys with hydrogen isotope permeabilities which are several orders of magnitude lower than those of stainless steels and most other conventional construction materials. (c) Materials and/or methods that are capable of removing non-metallic impurities from hydrogen streams, without simultaneously gettering or entraining large quantities of tritium, could have wide applicability in operations designed to maintain fuel stream purity and condition protium-tritium mixtures for enrichment. (d) Although the lead time for development of blanket processing technology is somewhat extended for the staged approach to a TEPR described in this report, work should begin immediately to establish criteria and identify suitable methods for the extraction of tritium from liquid lithium and other promising tritium-breeding materials. This work should also include development of broad-spectrum impurity control technology for all promising breeder-blanket concepts.

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APPENDIX A

PLASMA CALCULATIONAL MODEL

1. Plasma Particle and Energy Balances

The D-T ions are considered as a single species [$n_T = pn_i$, $n_D = (1 - p)n_i$] with an effective atomic mass $A_{DT} = 3p + 2(1 - p)$, where p is the fraction of the D-T ions which is tritium. Global particle balance are written for the D-T ions, alpha particles and impurities, and global power balances are written for the D-T ions and the electrons. The alpha particles and the injected energetic deuterons are assumed to thermalize instantaneously and to distribute their energy among the electrons, the D-T ions, the thermalized alpha particles, and any impurity ions which may be present. The power produced in the plasma by the injected deuterons is enhanced by the alpha heating resulting from suprathermal fusion of the fast deuterons with the thermal tritons. Power is lost from the plasma by transport and radiation, and particles are lost by diffusion and D-T fusion. An undefined source of cold D-T ions is assumed to be present to replenish the D-T ions.

a. Ions

(1) The D-T ion balance equations may be written as follows:

$$\frac{dn_i}{dt} = I_C + I_B(1 - 2f) - 2p(1 - p) \overline{\sigma v} n_i^2 - \left[\frac{n_i^2}{(n\tau)_p} \right] (1 - f_R), \quad (A-1)$$

where I_C is the cold D-T source, I_B is the injected source of energetic deuterons, $\overline{\sigma v}$ is the Maxwellian-averaged D-T fusion cross section, $(n\tau)_p$ is the particle confinement parameter, f is the fraction of the beam which undergoes suprathermal fusion, and f_R is the fraction of the particles which are recycled. For convenience, the parameter ξ is defined as the ratio of the plasma heating by the injected beam (including suprathermal fusion) to the plasma heating by alpha particles produced in thermal fusions

$$\xi \equiv \frac{G_B P_{inj}}{p(1-p) \overline{\sigma v} n_1^2 U_\alpha}, \quad (A-2)$$

where P_{inj} is the injected beam power density, $U_\alpha = 3.52$ MeV, and G_B is the suprathermal fusion beam enhancement factor

$$G_B \equiv \frac{U_B + f U_\alpha}{U_B}, \quad (A-3)$$

where U_B is the energy of the injected deuterons.

(2) The D-T ion power balance equation is

$$\begin{aligned} \frac{3}{2} \frac{d}{dt} (n_1 T_1) &= p(1-p) \overline{\sigma v} n_1^2 U_\alpha f_{\alpha 1} + G_B I_B U_B \hat{f}_{B1} \\ &+ \frac{3}{2} T_1 \left[I_B (1 - 2f) - 2p(1-p) \overline{\sigma v} n_1^2 \right] \\ &- K_{1e} - \frac{3}{2} T_1 \frac{n_1^2}{(n\tau)_I}, \end{aligned} \quad (A-4)$$

where $f_{\alpha 1}$ is the fraction of the 3.52-MeV alpha energy given to the D-T ions, $\hat{f}_{B1}$ is the fraction of the beam energy (including alphas from suprathermal fusion) given to the D-T ions,

$$\hat{f}_{B1} \equiv \frac{U_B f_{B1} + f U_\alpha f_{\alpha 1}}{G_B U_B}, \quad (A-5)$$

where f_{B1} is the fraction of the injected deuteron energy given directly to the D-T ions. The Coulomb energy exchange between ions and electrons is represented by the term

* All of the fusion alpha particles are assumed to be retained in the plasma during their thermalization.

$$K_{ie} = \frac{C_1 n_e n_i T_i}{T_e^{3/2}} \left(1 - \frac{T_e}{T_i} \right) A_{DT} \sum \frac{Z_j^2 r_j}{A_j}, \quad (A-6)$$

$j = i, \alpha$ impurities,

$$C_1 = 1.2 \times 10^{-18},^*$$

where r_j is the ratio of the density of species j to the D-T ion density, Z_j is the nuclear charge, and A_j is the atomic weight, in amu, of species j .

b. Alpha Particles

The alpha particle balance equation is

$$\frac{dn_\alpha}{dt} = p(1-p) \overline{\sigma v} n_i^2 + f I_B - \left[\frac{n_\alpha n_i}{(n\tau)_p} \right] (1 - f_R), \quad (A-7)$$

where the second term accounts for the production of alphas by supra-thermal fusion.

c. Impurities

The impurity particle balance equation is

$$\begin{aligned} \frac{dn_i}{dt} = & \left[p(1-p) \overline{\sigma v} n_i^2 + f I_B \right] S_N + \left[\frac{n_i^2}{(n\tau)_p} \right] S_{DT} \\ & + \left[\frac{n_\alpha n_i}{(n\tau)_p} \right] S_\alpha - \left[\frac{n_i n_i}{(n\tau)_p} \right] (1 - f_R), \end{aligned} \quad (A-8)$$

* All quantities are in MKS units, except T , which is in keV.

where S_N is the neutron sputtering rate, S_{DT} is the D-T sputtering rate, S_α is the alpha sputtering rate, and n_I is the particle density of impurity species I.

d. Electrons

The electron density is determined by the charge neutrality requirement

$$n_e = n_i + 2n_\alpha + \sum_I Z_I n_I,$$

where Z_I and n_I are defined above.

The electron power balance is written

$$\begin{aligned} \frac{3}{2} \frac{d}{dt} (n_e T_e) = & p(1-p) \overline{\sigma v} n_i^2 U_\alpha f_{ae} + G_{BIB} U_B \hat{f}_{Be} \\ & + \eta J^2 + K_{ie} - W_R - \frac{3}{2} T_e \frac{n_i n_e}{(n\tau)_E}, \end{aligned} \quad (A-9)$$

where W_R is the radiation loss term, which will be described subsequently, ηJ^2 is the ohmic heating power density, where η is the Spitzer resistivity modified to account for anomalous resistivity effects, and J is the current density,

$$\eta = \frac{2.8 \times 10^{-8} \gamma Z_{eff}}{T_e^{3/2}},$$

γ is the anomalous resistivity factor,

$$J = I_p / A_p,$$

where

I_p is the plasma current,

$A_p = \pi a^2 \kappa$ is the plasma cross-sectional area,

and

κ is the cross-sectional area of a noncircular plasma of minor radius a divided by πa^2 .

e. Slowing-Down Model

Thermalization of the injected deuterons and the fusion alphas is computed from a test particle model

$$\frac{dW_t}{dt} = \sum_j n_j g_{jt}(W_t), \quad (A-10)$$

$$g_{jt}(W_t) = C_4 \frac{(Z_j Z_t)}{m_t} \sqrt{\frac{\mu_{jt}}{T_j}} \left[\frac{1 - (2W_t/3T_j)}{1 - (4/3\sqrt{\pi})(\mu_{jt}/m_t T_j)^{3/2}} \right], \quad (A-11)$$

$$\mu_{jt} \equiv \frac{m_j m_t}{m_j + m_t}, \quad C_4 \equiv 1.286 \times 10^{-16}, \quad (A-12)$$

where W_t is the test particle energy (in keV), n_j is the number density of the j -th plasma component ($n_j = n_i, n_e, n_I, n_\alpha$) and m_j is the mass (in amu).

The energy given to the electrons by the test particle is

$$U_{te} = \int_0^T n_e g_{et}(W_t) dt = \int_{U_0}^{U_f} \frac{n_e g_{et}(W_t)}{\sum_k n_k g_{kt}(W_t)} dW_t, \quad (A-13)$$

where U_0 is the initial energy of the test particle, and U_f is $3/2 T_e$ or $3/2 T_i$, whichever is greater. In any case the total energy given up by the test particle is taken to be equal to $U_0 - 3/2 T_i$, and the energy $U_0 - 3/2 T_i - U_{te}$ is distributed equally among all the plasma ions. Thus, for each ion species

$$U_{tj} = \frac{n_i}{\sum_k n_k} \left(U_0 - \frac{3}{2} T_i - U_{te} \right). \quad (A-14)$$

Here it is assumed, implicitly, that all ions are at the same temperature. The quantities f_{tj} and f_{te} which appear in the plasma energy balance equations are

$$f_{tj} = \frac{U_{tj}}{U_0}, \quad f_{te} = \frac{U_{te}}{U_0}. \quad (A-15)$$

The average energies, suprathermal fusion rates, and test particle slowing-down times, in addition to the U_{tj} 's, are computed. The slowing down time, τ^{SD} , for a test particle is given by the expression

$$\tau^{SD} = \int_{U_0}^{U_f} \frac{dt}{dW_t} dW_t = \int_{U_0}^{U_f} \frac{dW_t}{\sum_j n_j g_{jt}(W_t)}, \quad (A-16)$$

while f , the number of fusions per beam particle, is

$$f = p n_i \int_{U_0}^{U_f} v(W_B) \sigma_f(W_B) \frac{dt}{dW_B} dW_B, \quad (A-17)$$

or

$$f = p n_i \int_{U_0}^{U_f} \frac{v(W_B) \sigma_f(W_B)}{\sum_j n_j g_{jB}(W_B)} dW_B. \quad (A-18)$$

In Eq. (A-18) it is assumed that the beam particle is a deuteron, and p is the fraction of the D-T ions which consist of tritium. The quantities $v(W_B)$ and $\sigma_f(W_B)$ are, respectively, the beam particle speed and the fusion cross section for deuterons striking stationary tritons.

Finally the average energy of a test particle during the thermalization process is taken to be

$$\bar{E}_t = \frac{1}{\tau_{SD}} \int_{U_0}^{U_f} W_t dt, \quad (A-19)$$

$$\bar{E}_t = \frac{1}{\tau_{SD}} \int_{U_0}^{U_f} \frac{W_t}{\sum_j n_j g_{jt}(W_t)} dW_t. \quad (A-20)$$

In Eqs. (A-16), (A-18), and (A-20) that part of the test particle's history during which $3/2 T_1 < W_t < 3/2 T_e$ is neglected in cases where $T_e > T_1$.

f. Radiation Model

The radiation term W_R in Eq. (A-9) is given by $W_R = W_C + W_B$, where the cyclotron radiation is given by

$$W_C = \frac{C_2 n_e B_t^{5/2}}{\sqrt{n_1}} \left(T_e^{11/4} + 0.0049 T_e^{15/4} \right),$$

$$C_2 = 3.7 \times 10^3. \quad (A-21)$$

B_t is the toroidal magnetic field in the plasma. A wall reflectivity for cyclotron radiation of 90% has been assumed. The second term is given by (1,2)

$$W_B = C_3 n_e n_i \sum_j r_j \left\{ z_j^2 T_e^{1/2} + \left[3.792 \times 10^{-2} \frac{z_j^4}{T_j} + 8.604 \times 10^{-4} \frac{z_j^6}{T_j^2} \left(\frac{T_e}{T_j} \right)^2 \right] \right\},$$

$$j = i, \alpha, \text{ impurities, } C_3 = 3 \times 10^{-21}, \quad (\text{A-22})$$

$$T_j = \begin{cases} T_e; & T_e \geq T_j^c \\ T_j^c; & T_e < T_j^c, \end{cases}$$

where the second term in the brackets is the usual form for line and recombination radiation^(1,2) modified to go to zero quadratically when T_e is less than T_j^c and

$$T_j^c = \left[1.033 \times 10^{-7} (z_j - 3)^3 + 3.4266 \times 10^{-3} (z_j - 3)^2 + 5.5574 \times 10^{-3} (z_j - 3) + 0.529 \times 10^{-3} \right] \text{ (keV)} \quad (\text{A-23})$$

is an empirical fit to the ionization potentials for the beryllium-like ion sequence.

2. Plasma and Reactor Parameters

Plasma transport and stability theory provides a number of relationships among the plasma parameters (T_e , T_i , n_i), the plasma current (I_p), the toroidal magnetic field (B_t), the aspect ratio (A), and the plasma radius (a). In this section, these and other relationships are exploited to arrive at a set of equations which determine the reactor parameters that are necessary to achieve a given set of plasma parameters.

a. Transport Model

The present state of research in plasma transport theory⁽³⁾ is not sufficient to allow a confident prediction of the particle and energy scaling laws in the D-T reactor regime. If neoclassical theory obtains, these plasmas would be in the banana regime, and there is evidence (e.g. Ref. 4) that confinement would be more than adequate at quite modest plasma currents. If, on the other hand, anomalous transport processes govern the particle and energy confinement, current theory would predict that trapped-ion mode (TIM) scaling would obtain in most of the D-T reactor regime, with trapped-electron mode (TEM) scaling obtaining in the very low-temperature part of the regime. The plasma current required to achieve a given confinement, $n\tau$, is generally significantly greater for anomalous transport scaling than for neoclassical scaling.

The trapped-particle confinement model used in this report can be summarized as follows: If the effective collision frequency is greater than the trapped particle bounce frequency (that is, the particles do not stay trapped), then the particle (P) and electron energy (E) transport is taken to be pseudoclassical (PC), and the ion energy (I) transport is taken to be neoclassical (NC). For effective electron-ion collision frequencies below the electron bounce frequency, the particle and electron energy transport are taken to be pseudoclassical, trapped-electron mode, or trapped-ion mode, whichever is greatest. The ion energy transport in this case is taken to be neoclassical or trapped-ion mode, whichever is greatest, when the ion-ion collision frequency is less than the ion bounce frequency. The $n\tau$ formulas used for the various transport theories are chosen to be consistent with Ref. 3, generalized for noncircular cross sections. In the regime of present experiments this model represents the measured confinement, which can be fit by the NC theory for the ion energy and PC theory for the particles and electron energy.⁽³⁾

(1) Pseudoclassical (Particles and Electron Energy)

$$(n\tau)_P^{PC} = \frac{1.8 \times 10^8 I_P^{2T^{1/2}}}{Z_{eff}},$$

$$(n\tau)_E^{PC} = 0.3 (n\tau)_P^{PC}. \quad (A-24)$$

(2) Trapped Electron Mode (Particles and Electron Energy)

$$(n\tau)_P^{TEM} = \begin{cases} \frac{6.35 \times 10^6 \bar{A}^{3/2}}{Z_{eff}} T_e^{1/2} I_P^2 \left[1 + \left(v_{ei}/\omega_0 \right)^2 \right], & \text{if } v_{ei} < \omega_{-1} \\ 0 & , \text{ otherwise} \end{cases}$$

$$(n\tau)_E^{TEM} = (n\tau)_P^{TEM} / \frac{3}{2} \bar{A}. \quad (A-25)$$

This is a combination, of the form given by Ref. 5, which approaches the asymptotic limits given in Ref. 3 for TEM-1 and TEM-2.

(3) Trapped Ion Mode (Particles, Electron Energy, and Ion Energy)

$$(n\tau)_P^{TIM} = (n\tau)_E^{TIM} = (n\tau)_I^{TIM}$$

$$= \begin{cases} \frac{2.05 \times 10^{-5} \beta_{0e}^2 I_P^4 Z_{eff}^2 \bar{A}^{5/2}}{T_e^{11/2}} \left[1 + \left(\frac{T_e}{T_i} \right) \right]^2, & \text{if } v_{ei} < \omega_{-1} \quad (P,E) \\ & \text{or } v_{ei} < \left(\frac{T_i}{T_e} \right)^2 \omega_{-1} \quad (I) \\ 0 & , \text{ otherwise. } \quad (A-26) \end{cases}$$

(4) Neoclassical (Ions)

$$(\nu\tau)_I^{NC} = \begin{cases} \frac{1.83 \times 10^7 T_i^{1/2} \bar{A}^2 I_p^2}{Z_{eff}}, & \nu_{ei} > \left(\frac{T_i}{T_e} \right)^2 \omega_{-2} \\ & \text{(collisional)} \\ \frac{9.95 \times 10^6 T_i^{1/2} \bar{A}^{1/2} I_p^2}{Z_{eff}} \left[\frac{\nu_{ei}}{\left(\frac{T_i}{T_e} \right)^2 \omega_{-1}} \right], & \left(\frac{T_i}{T_e} \right)^2 \omega_{-1} < \nu_{ei} < \left(\frac{T_i}{T_e} \right)^2 \omega_{-2} \\ & \text{(plateau)} \\ \frac{9.95 \times 10^6 T_i^{1/2} \bar{A}^{1/2} I_p^2}{Z_{eff}}, & \nu_{ei} < \left(\frac{T_i}{T_e} \right)^2 \omega_{-2} \\ & \text{(banana)} \end{cases} \quad (A-27)$$

where I_p is the plasma current, T_i and T_e are the ion and electron temperatures, $\bar{A} = R/\bar{a}$ is the average aspect ratio, R is the major radius, $\bar{a} = a\ell$ is the average radius of the cross section, $\ell = [(1 + \kappa^2)/2]^{1/2}$, where κ has been defined previously.

$$Z_{eff} = \frac{\sum_{ions} n_j Z_j^2}{\sum_{ions} n_j Z_j}$$

is the effective charge,

$$\nu_{ei} = \frac{1.6 \times 10^{-15} Z_{eff} n_i}{T_e^{3/2}}$$

is the electron-ion collision frequency,

$$\nu_{eff} = \frac{3}{2} \bar{A} \nu_{ei}$$

is the effective collision frequency,

$$\omega_0 = \frac{0.19 T_e^{1/2} I_p}{\bar{A}^{1/2} B_t a^2 \ell^2}$$

is the frequency where TEM peaks, B_t is the toroidal magnetic field,

$$\omega_{-1} = \frac{2.04 T_e^{1/2} I_p}{\bar{A}^{1/2} a l R B_t}$$

is the frequency where the neoclassical plateau regime for electrons changes to the banana regime, and

$$\omega_{-2} = 1.84 \bar{A}^{3/2} \omega_{-1}$$

is the frequency where the neoclassical collisional regime for electrons changes to the plateau regime.

A graph of the confinement parameter for a design of the Tokamak Experimental Power Reactor is shown in Fig. A-1.

For the steady-state analyses, for which the trapped ion mode scaling obtains, the following expression results for the plasma current that is required to create a given $n\tau$ [$(n\tau)_p = (n\tau)_E = (n\tau)_I$ for TIM].

$$I_p^4 = \frac{(n\tau) T_e^{11/2}}{2.05 \times 10^{-5} \beta_{\theta e}^2 Z_{\text{eff}} \bar{A}^{5/2} B_t^2 (1 + T_e/T_i)^2}, \quad (\text{A-28})$$

where $\beta_{\theta e}$ is the ratio of the electron kinetic pressure to the poloidal magnetic field pressure. In a β_θ -limited device, the ratio of the plasma kinetic pressure to the poloidal field pressure, β_θ , has some upper limit set by MHD stability requirements. $\beta_{\theta e}$ is related to β_θ in an obvious way.

b. MHD Constraints

The onset of "kink-mode" MHD instability places an upper limit on the plasma current, which is related to the toroidal field strength through

$$q = \frac{B_t}{\bar{A} B_\theta},$$

Fig. A-1. Confinement Versus Temperature for TEPR Design with $\beta_\theta = 2.2$, $q = 2.5$, 0.5% ^{16}O impurity.

$$B_{\theta} = \frac{\mu I_p}{2\pi a}.$$

Thus,

$$I_p = \frac{2\pi a B_t}{\mu q A} = \frac{2\pi a \ell^2 B_t}{\mu q A}, \quad (\text{A-25})$$

where $A \equiv R/a$. Here q is the safety factor, which is inversely proportional to the rotational transform of the net (toroidal plus poloidal) magnetic field. $q(r) > 1$ is the Kruskal-Shafranov condition, and $q(a) \gtrsim 2.5$ is generally thought to be an adequate stability requirement. (3) Theory indicates that somewhat higher values of q may be necessary to insure stability in an elliptical plasma, but that other noncircular shapes maintain stability for lower values of q . In this work, q refers to $q(a)$.

At sufficiently high plasma pressures, flute MHD modes are predicted to concentrate in local regions of unfavorable curvature and give rise to unstable "balloon-modes". This effect is thought to become important when the ratio of the plasma kinetic pressure to the poloidal magnetic pressure becomes comparable with the aspect ratio, which is the same pressure level at which the ability to obtain a proper equilibrium configuration becomes questionable. In computing the plasma pressure, we include not only the pressure of the thermalized particles but the fast alpha particles and deuterons from the beam which are in the process of thermalizing.

$$\beta_{\theta} \equiv \frac{\left[k \left(n_e T_e + n_i T_i + n_{\alpha} T_{\alpha} + \sum_I n_I T_I \right) + 2/3 n_{\alpha}^f \bar{E}_{\alpha} + 2/3 n_B^f \bar{E}_B \right]}{\left(\mu I_p / 2\pi a \ell \right)^2 / 2\mu}, \quad (\text{A-30})$$

where the sum in the first term is over impurities, $\bar{E}_{\alpha}$ and $\bar{E}_B$ are the mean energies of the thermalizing alpha particles and deuterons, respectively, and n_{α}^f and n_B^f are the corresponding densities of such particles at any given instant. These latter quantities are computed from the

general consideration that the number of particles in the process of thermalizing at any time is equal to the source rate times the thermalization time. The formulas are

$$n_{\alpha}^f = \left[p(1-p) \overline{\sigma v} n_1^2 + f I_B \right] \tau_{\alpha}^{SD}, \quad (\text{A-31})$$

and

$$n_B^f = I_B \tau_B^{SD}, \quad (\text{A-32})$$

where τ^{SD} is the thermalization time defined in Eq. (A-16).

Equation (A-30) determines the upper limit on the D-T ion density, n_1 , hence, the maximum power, when there is a limit on β_{θ} .

The toroidal β , which is defined by Eq. (A-30) with the denominator replaced by $B_t^2/2\mu$, is related to β_{θ} by

$$\frac{\beta}{\beta_{\theta}} = \frac{B_{\theta}^2}{B_t^2} = \left(\frac{l}{qA} \right)^2.$$

Experimentally, $\beta \approx 0.002$ has been achieved, and the theoretical upper limit is $\beta \approx 0.1$, from MHD stability considerations. For similar q , A and β_{θ} , noncircular cross-section plasmas are capable of achieving β 's larger by a factor of l^2 ($l^2 = 5$ for $\kappa = 3$) than are circular cross-section plasmas — this is reflected in Chapter III in the smaller B_t which will suffice for noncircular cross-section plasmas. For all q , A , and l considered in this work, $\beta \ll 0.1$.

c. Toroidal Magnetic Field

For a given field, $B_{\text{max}}^{\text{TFC}}$, at the toroidal field coils the toroidal field at the plasma centerline (magnetic axis) depends upon geometry as

$$B_t = \left(1 - \frac{r_w + \Delta_B + \Delta_S}{R} \right) B_{\text{max}}^{\text{TFC}}, \quad (\text{A-33})$$

where the various dimensions are identified in Fig. A-2.

d. Reactor Performance Parameters

The neutron power flux through the first wall is

$$P_w (\text{MW/m}^2) = 2.26 \times 10^{-18} J_w, \quad (\text{A-34})$$

where,

$$J_w = \left[p(1-p) \overline{\sigma v} n_1^2 + f I_B \right] \frac{V_p}{A_w},$$

is the 14-MeV neutron current density at the first wall, $A_w = 4\pi^2 r_w lR$, is the surface area of the first wall, and $V_p = 2\pi^2 a^2 \kappa R$ is the plasma volume.

The total thermal power is

$$P_T (\text{MW}) = 1.602 \times 10^{-22} \left\{ E_{\text{fus}} \left[p(1-p) \overline{\sigma v} n_1^2 + f I_B \right] + I_B U_B \right\} V_p + P_\Omega, \quad (\text{A-35})$$

where

$$E_{\text{fus}} (\text{keV}) = \left[14.1 \epsilon_{\text{bkt}} + 3.52 \right] \times 10^3,$$

with ϵ_{bkt} accounting for the energy enhancement in the blanket, and P_Ω is the ohmic heating power,

$$P_\Omega (\text{MW}) = 10^{-6} n J^2 V_p = 5.6 \times 10^{-14} \frac{\gamma Z_{\text{eff}} T_e^2 R}{a^2 \kappa T_e^{3/2}}. \quad (\text{A-36})$$

The neutron power is

$$P_N = \epsilon_{\text{bkt}} P_w A_w,$$

where ϵ_{bkt} , P_w and A_w have been defined above.

Fig. A-2. Tokamak Configuration

The radiation power is

$$P_R^{(MW)} = 1.602 \times 10^{-22} (W_C + W_B) V_p ,$$

where W_C and W_B have been defined in Eqs. (A-21) and (A-22). The power loss by transport processes is

$$P_L^{(MW)} = 1.602 \times 10^{-22} \left[\frac{3}{2} T_i \left(\frac{n_i^2}{n \tau_i} \right) + \frac{3}{2} T_i \left(\frac{n_a n_i}{n \tau_p} \right) + \frac{3}{2} T_e \left(\frac{n_e n_i}{n \tau_e} \right) \right] V_p . \quad (A-37)$$

e. Plasma Driving (Ohmic Heating) System

(1) Steady-State Model. The maximum change in flux, neglecting the reduction due to mutual coupling, which must be generated by the plasma driving (ohmic heating) coils to induce the plasma current is, $\Delta\phi_{ind} = \frac{L I_p}{p}$, where

$$L_p = \mu R (\ln 8A/\sqrt{\kappa} - 1.75) , \quad (A-38)$$

where μ is the permittivity. Equation (A-38) was confirmed by direct inductance calculations for several plasma cross-section shapes. Assuming that the transformer coils encircle the major axis of the torus and are located external to the toroidal field coils, the change in transformer field corresponding to $\Delta\phi_{ind}$ is (see Fig. A-2)

$$\Delta B_{OH} = \frac{\Delta\phi_{ind}}{\pi r_v^2} = \frac{L I_p}{\pi r_v^2} . \quad (A-39)$$

If no bootstrap current materializes, the magnetic flux must continue to change according to Faraday's law in order to maintain the plasma current

$$\frac{d}{dt} (\Delta B_{OH}) = \frac{1}{\pi r^2} \frac{d}{dt} (\Delta\phi) = \frac{2\pi R E_t}{\pi r_v^2} = \frac{2R \eta J}{r_v^2} = \frac{1.8 \times 10^{-8} R \gamma Z_{eff} I_p}{r_v^2 a^2 T_e^{3/2} \kappa} ,$$

(A-40)

where E_t is the toroidal electric field.

(2) Time-Dependent Model. In the nonsteady-state, the ohmic heating system is modeled as being composed of the power supply and the ohmic heating primary. It can be described by a coupled two-mesh system with the following equivalent circuit:

Fig. A-3. Equivalent Circuit

where I_{pri} is the current source driving the ohmic heating primary, V_{pri} is the voltage across the current source, L_{pri} is the primary self-inductance, L_p is the plasma self-inductance, M is the primary-to-plasma mutual inductance, R_{pri} is the primary resistance, R_p is the plasma resistance. At time $t = 0$ the plasma is injected, ionized and acts as a one-turn conducting secondary. The equation for the second mesh is:

$$R_p I_p + L_p \frac{dI_p}{dt} = M \frac{dI_{pri}}{dt},$$

$$M \equiv K \sqrt{L_{pri} L_p},$$

where K = coefficient of coupling.

For an actual coil configuration, L_{pri} is computed from the flux linkage of a many-coil system. A typical value of $L_{pri} = 1 \mu H$ has been used as the equivalent single-turn inductance. This is in the general range but does not conform precisely to the designs discussed in Section II-C. (Note also that the single-turn equivalent inductance is equal to an N -turn inductance divided by N^2 .) For the TEPR design with the circular plasma cross section, $L_p = 11 \mu H$.

$$R_p = \frac{2\pi R \eta}{A_p} \text{ is the plasma resistance.}$$

R_{pri} is dependent on the type of primary, for a superconducting primary $R_{pri} = 0$.

Power Supply Power and Energy Expressions

The total energy delivered by the power supply is

$$U_{PS} = \frac{1}{2} L_p^2 I_p^2 + \frac{1}{2} L_{pri} I_{pri}^2 - M I_p I_{pri} + \int_0^t \left[R_p I_p^2 + R_{pri} I_{pri}^2 \right] dt. \quad (A-41)$$

The power delivered by the power supply is

$$P_{PS} = \frac{d}{dt} U_{PS}. \quad (A-42)$$

The actual flux change produced by the power supply is

$$\begin{aligned} \Delta \phi &= \int_0^t \frac{P_{PS}(t')}{I_{pri}(t')} dt' = I_p \sqrt{\frac{L_p L_{pri}}{K}} \left(\frac{1 - K^2}{K} \right) + \frac{1}{K} \sqrt{\frac{L_{pri}}{L_p}} \int_0^t R_p I_p dt \\ &\equiv \Delta \phi_{ind} + \Delta \phi_{res}. \end{aligned}$$

The ohmic heating power supply must charge, discharge, and store energy at various times in the operating cycle. A certain amount of energy will be dissipated in this process and is not recoverable. This is modeled by assuming

$$U_{NR} = \int_0^t |P_{PS}| dt, \quad (A-43)$$

where f_{PS} is the fraction of the power dissipated during each time interval.

The recovered energy is thus

$$U_R = (1 - f_{PS}) \int |P_{PS}| dt . \quad (A-44)$$

f. Additional Parameters

There are three additional parameters which are useful in characterizing the performance of the plasma in a tokamak (for that matter, in any reactor). The plasma Q_p is defined as the total thermonuclear power generated by the plasma (including the blanket enhancement) divided by the total power put into the plasma from external sources. In a steady-state situation

$$Q_p \equiv \frac{P_T - (P_B + P_\Omega)}{P_B + P_\Omega} , \quad (A-45)$$

where $P_B = P_{inj p}$ is the beam power injected into the plasma, and $P_{inj} = I_B U_B$. This quantity becomes very large for devices approaching ignition ($\xi \rightarrow 0$) and provides a measure of the gain when neutral beam injection is used to maintain the plasma in a sub-ignition condition. It is used in the steady-state analyses of Section III-A. The definition of Q_p averaged over a burn cycle is

$$Q_p = \frac{\int_0^t (P_N + P_L + P_R) dt}{\int_0^t (P_B + P_\Omega) dt} - 1 . \quad (A-46)$$

The ratio of the energy density of the injected deuterons to the plasma energy density is

$$\Gamma = \frac{I_B U_B \tau_B^{SD}}{\left[3/2 k (n_e T_e + n_i T_i + n_a T_a + \sum_I n_I T_I) + n_a^f \bar{E}_a + n_b^f \bar{E}_b \right]} . \quad (A-47)$$

The fractional burnup is

$$f_{bu} = \frac{2 \left[p(1-p) \overline{\sigma v} n_1^2 + f I_B \right]}{2 \left[p(1-p) \overline{\sigma v} n_1^2 + f I_B \right] + (n_1^2 / n \tau_p)} . \quad (A-48)$$

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APPENDIX B

PLASMA AND REACTOR PARAMETRIC SURVEY

This study has a number of objectives, all of which relate to establishing the allowable range of parameters of a tokamak experimental power reactor (TEPR) operating in the trapped-ion mode (TIM) regime, on the basis of current theory. Such a reactor should be capable of producing an electrical power output of several tens of megawatts. Thus $P_T = 150 \text{ MWt}$ is a reasonable value for the thermonuclear power output during the peak, steady-state portion of the burn pulse. The power level affects the calculated values of P_{inj} , P_w , and β_θ . (See Appendix A for a description of the computational model and the definition of variables.) The first two parameters scale as P_T and the last as $\sqrt{P_T}$, so the results of this report can readily be scaled to other power levels.

Previous work,⁽¹⁻³⁾ as well as results reported in Chapter III, indicate that a 50-50 mixture of deuterium and tritium in the plasma is optimal in the range of plasma Q_p values which are reasonable for a TEPR, and this composition is used in this work. The consideration that a TEPR should represent a prudent extrapolation of plasma parameters beyond the current and next generation tokamaks (see Table I-2) and the consideration of cost both argue for a small device. The quest for the smallest device capable of producing 150 MWt in quasi-steady-state^{*} operation leads to consideration of plasmas with non-circular cross sections, and one of the principal results of this work is a comparison of parameters for circular and noncircular cross-section plasmas. A second principal result is the examination of tradeoffs to be made by operating in the beam-driven mode.^(1,4) This is another means of reducing both the size of the device and the extrapolation of plasma parameters from present experiments, wherein an energetic deuteron beam is employed to maintain a sub-ignition plasma.^{**} The possibility of production of net electrical power

^{*}The burn cycle dynamics are considered in Chapter III. Hopefully, a power level of 150 MWt during the burn will still lead to a respectable power output when averaged over the burn cycle.

^{**}Further gains in the driven mode could be realized by "clamping" the fast deuterons at the energy which maximizes the suprathermal fusion rate,⁽¹⁾ if burn time is comparable to the deuteron slowing-down time. Compression and electric field programming are possible mechanisms for clamping. It is envisioned that burn times will be long compared to the deuteron thermalization time, so it is not obvious how clamping would work with a continuously injected beam.

limits the feasible range of driven devices to $Q_p \gtrsim 10$. However, somewhat less efficient devices are of possible interest. At $\xi > 1$, the beam heating exceeds the alpha heating of the plasma. Hence, $\xi \leq 1$, which corresponds to $Q_p \gtrsim 6.5$, is taken as a plausible limit to the range of beam-driven devices which are of interest for a TEPR.

1. Technological Constraints

Certain technological constraints are invoked to limit the range of allowable reactor parameters. The maximum magnetic field, $B_{\max}^{\text{TFC}}$, in the toroidal field coils (TFC) should be in the range 80-85 kG for niobium-titanium superconductor. Since stray fields may be present at the position of the coils, the maximum field produced by the TFC probably must be 5-10 kG smaller. The ability of the ohmic heating coils (OHC) to produce the requisite plasma current is ultimately limited by the maximum field in the OHC, assuming adequate power supplies. With conventional copper conductor, $\Delta B_{\text{OH}} \leq 120$ kG is taken as a reasonable constraint. With niobium-titanium superconductor, the primary current may be reversed, thus producing a change in magnetic field which is twice the maximum field, so that $\Delta B_{\text{OH}} \leq 160$ kG is taken as a constraint.

2. Fixed Parameters

The thickness of the blanket, shield and magnet coils, and the dimension of the first-wall radius have a large impact upon the overall size of a TEPR. Allowing 1.0 m for the blanket and shield ($\Delta_B + \Delta_S = 1.0$ m) provides adequate energy and radiation attenuation to the TFC and should provide enough space for energy conversion and some tritium breeding (see Chapter VI) — this dimension is critical only on the inside of the torus insofar as these calculations are concerned, so a thicker blanket on the outside of the torus would be optional. A 10% neutron energy enhancement in the blanket is assumed; this allows for the energy enhancement being down by a factor of 2 from an optimal blanket design, and is of little consequence to this work in any case. One meter is allowed for the TFC and OHC ($\Delta_m = 1$ m). The first-wall radius is assumed to be 20% greater than the plasma radius ($r_w = 1.2 a$). An impurity concentration of 1/2% ^{16}O is assumed. The injected deuterons are assumed to have an energy of 180 keV, for operation in the beam-driven mode. In summary:

$$\begin{array}{ll}
\Delta_B + \Delta_S = 1.0 \text{ m} & U_B = 180 \text{ keV} \\
\Delta_M = 1.0 \text{ m} & 50-50 \text{ DT} \\
Z_{\text{eff}} = 1.3 (1/2\% {}^{16}\text{O}) & q(a) = 2.5 \\
P_T = 150 \text{ MW} & n\tau \sim \text{TIM}
\end{array}$$

3. Reactor Parameters — General

The reactor size (major radius, R) which would be required to achieve confinement is shown in Fig. B-1 for two combinations of geometry and toroidal field $\{\kappa = 1, A = 3, B_{\text{max}}^{\text{TFC}} = 85 \text{ kG}\}$ and $\{\kappa = 3, A = 5, B_{\text{max}}^{\text{TFC}} = 70 \text{ kG}\}$. The size of the device must increase sharply with temperature in order to compensate the $T_e^{-11/2}$ dependence of $n\tau$ in the TIM theory. This figure also illustrates the reduction in size which can be achieved by operating in the driven mode and the significantly smaller major radius which can be achieved by using a noncircular plasma cross section. The parameter ξ is the ratio of beam-to-alpha heating of the plasma.

Another way of presenting these results is in terms of the major radius that is required to achieve a given Q_p ($Q_p \sim \infty$ for ignition). This information is displayed for the $\{\kappa = 1, A = 3, B_{\text{max}}^{\text{TFC}} = 85 \text{ kG}\}$ case in Fig. B-2. This figure displays a number of interesting features, which will be discussed in turn. The sharp dependence of size upon plasma electron temperature is again evident, and it is apparent that operation at the lowest possible temperature minimizes the size of the device. The lowest achievable temperature depends upon the impurity concentration. At $Z_{\text{eff}} = 1.3$ (0.5% ${}^{16}\text{O}$ impurity), operation at $T_e = 6 \text{ keV}$ is possible over a wide range of Q_p values from $Q_p < 1$ to $Q_p \sim \infty$. However, at $Z_{\text{eff}} = 2.8$ (0.3% ${}^{26}\text{Fe}$ impurity), operation at $T_e = 6 \text{ keV}$ is limited to highly driven systems with $Q_p \lesssim 1.5$, because of the enhanced radiation loss. At $T_e = 10 \text{ keV}$, a $Z_{\text{eff}} = 2.8$ plasma can be achieved only over a range $Q_p \lesssim 10$.*

It is interesting that increasing Z_{eff} reduces the size of highly driven devices and that the range of Q_p over which a size reduction is obtained by

* Z_{eff} is known to have a marked effect upon ignition conditions, as discussed in Chapter II and elsewhere, (2,3,5). The maximum Z_{eff} for which ignition can be achieved with a ${}^{26}\text{Fe}$ impurity is ~ 3.5 for $T_e = 15 \text{ keV}$, and ~ 5.4 for $T_e = 20 \text{ keV}$.

Fig. B-1. Reactor Size (solid line — $B_{\max} = 85$ kG, $\kappa = 1$, $A = 3$; dashed line — $B_{\max} = 70$ kG, $\kappa = 3$, $A = 5$; $\Delta_B + \Delta_S = 1$ m). ξ is the ratio of injected beam power to thermal alpha power.

Fig. B-2. Thermonuclear Power Amplification ($B_{\text{max}} = 85 \text{ kG}$, $\kappa = 1$, $A = 3$, $\Delta_B + \Delta_S = 1 \text{ m}$)

increasing Z_{eff} increases with increasing T_e . This effect can be understood from Eqs. (A-28) and (A-29) and consideration of the effect of Z_{eff} and neutral beam injection on the plasma energy balance. In a highly driven device at low T_e , the energy balance consists essentially of gains from the injection heating and transport losses, so the Z_{eff} effect on radiation losses is relatively unimportant and the required $n\tau$ is insensitive to Z_{eff} . Thus, from Eq. (A-28), $I_p^4 \propto Z_{\text{eff}}^{-1}$ and, from Eq. (A-29), $R \propto I_p \propto Z_{\text{eff}}^{-1/4}$ for fixed aspect ratio. Thus, an increase in Z_{eff} results in a reduction in R . An increase in Z_{eff} also increases $\beta_{\theta e}$ (at fixed T_e) since β_0 is fixed and $n_e/n_i \propto Z_{\text{eff}}$. This further decreases R with increasing Z_{eff} . In an ignition device, or a device in which the beam heating is small relative to the alpha heating, radiation is the dominant energy loss mechanism. Hence, an increase in Z_{eff} significantly increases the $n\tau$ which is required to maintain the energy balance, and, from Eq. (A-28), I_p increases. This explains the crossing over of the curves for $T_e = 10$ keV. The same qualitative effects occur at $T_e = 15$ keV, but the range of Q_p over which an increase in Z_{eff} reduces R is much greater.

Another interesting point about Fig. B-2 is that the major radius does not decrease monotonically with decreasing Q_p (increasing beam power), but that there exists a minimum R . Increasing the beam power decreases the required $n\tau$. Increasing beam power also decreases $\beta_{\theta e}$ (for fixed β_0) in two ways. The beam pressure increases and the ratio T_i/T_e increases due to the preferential beam heating of the ions. Increasing T_i/T_e increases the relative ion and alpha pressures (through increased fusion). Thus, increasing beam power increases the beam, ion and alpha fractions of the pressure relative to the electron fraction of the pressure and, since the total pressure is fixed, the electron density must be decreased, thereby decreasing $\beta_{\theta e}$. The increase in T_i/T_e also decreases the $(1 + T_e/T_i)^2$ term in Eq. (A-28). Thus, at a fixed T_e , Eq. (A-28) states

$$I_p^4 \propto \frac{n\tau}{\beta_{\theta e}^2 (1 + T_e/T_i)^2} \quad (\text{B-1})$$

As the beam power increases, $n\tau$ decreases and the denominator decreases. At low beam power (large Q_p), the $n\tau$ decrease predominates; thus, the major radius decreases. For highly driven devices (low Q_p), the decreasing denominator becomes dominant and the major radius increases. Thus, R at first

decreases, then increases, as Q_p decreases.

The key to the interesting behavior of Q_p in the highly-driven devices is the inclusion of the alpha and beam contributions, and the calculation of both ion and electron contributions, in the plasma pressure. If the electron pressure, rather than the total pressure, had been held constant ($\beta_{e0} = \text{constant}$), then I_p and R would have decreased monotonically with increasing beam power.

Figure B-2 also indicates that there is a limiting value of Q_p which is approached as the beam power increases. From Eqs. (A-3), (A-35), and (A-45) this is

$$Q_{\min} = \frac{E_{\text{fus}} (G_B - 1)}{U_\alpha} \quad (\text{B-2})$$

Some perspective on the relationship of a TEPR to feasibility experiments, on one hand, and commercial reactors, on the other, may be obtained from Fig. B-2. Feasibility experiments are intended to demonstrate $Q_p \geq 1$, with a minimum size. Thus, a device with a circular cross-section plasma with $T_e = 6$ keV and $R \approx 4$ m would appear to be a satisfactory feasibility experiment.* However, TEPR should operate with a beam-to-alpha heating ratio less than unity, which is $Q_{\text{TEPR}} \gtrsim 6.5$, and must be capable of operating with a significant amount of impurity in the plasma; the latter dictates that $T_e \gtrsim 10$ keV. Thus, a TEPR with a circular cross-section plasma would have a minimum size of $R \gtrsim 7$ m.** A commercial reactor should probably operate with $Q_p > 30-100$ ($Q_p \gtrsim \infty$ for ignition) at the temperature which maximizes the fusion rate-to-radiation rate ratio, which is $T_e \gtrsim 13$ keV, or at the temperature which minimizes the cost per unit power, which may be higher.^(6,7) Thus, a commercial reactor with a circular cross-section plasma would have a major radius of

*The TFTR⁽⁴⁾ is designed to operate with no blanket or magnet shield, with a somewhat larger toroidal field in the plasma, with $T_e = 6$ keV in the center of the plasma ($T_{\text{av}} = 3$ keV), and with $R = 2.5$ m. An alternative design⁽²⁾ for the feasibility experiment, with parameters very close to those used in this work ($q = 3$, $A = 4$, $B_{\text{max}} = 85$ kG, $T = 6.5$ keV, $Z_{\text{eff}} = 2$, $\Delta_B + \Delta_S = 1$ m, $n_T = \text{TIM}$) has a calculated major radius $R \approx 4.3$ at $Q = 1$.

**There was a slight difference between the models used in obtaining the results shown in Figs. B-2 and B-4 such that the corresponding minimum size TEPR from Fig. B-4 would be 7.5 m.

R ≈ 10-15 m. These remarks are intended to provide perspective, on the basis of a set of calculations which have been performed with consistent assumptions and constraints, rather than to indicate any absolute constraint on sizes. A fuller appreciation for the options available for a TEPR will be developed in the parametric study in the next section.

The stability of the solutions to the plasma balance equations against temperature-density fluctuations was examined, using linear stability analysis.⁽⁷⁾ The growth rate of the dominant temperature-density modes are presented in Fig. B-3, for two cases $\{\kappa = 1, A = 3, B_{\max}^{\text{TFC}} = 85 \text{ kG}\}$ and $\{\kappa = 3, A = 5, B_{\max}^{\text{TFC}} = 70 \text{ kG}\}$. These results indicate that plasmas in the range of interest for TEPRs ($\xi < 1$) will be unstable against temperature-density fluctuations at low temperatures, and some form of control will be necessary if the burn time is more than a few seconds. The instability occurs as follows: There is a positive temperature feedback due to fusion alpha heating — an increase in temperature causes an increase in fusion rate and hence in fusion heating, which causes the temperature to increase further. On the other hand, the radiation and transport provide a negative feedback — an increase in temperature causes an increase in these losses, which causes the temperature to decrease. The positive feedback mechanism dominates at lower temperatures, causing instability. Stability is improved with increasing ξ because of the corresponding decrease in the portion of the plasma heating which is provided by the fusion alpha particles. The critical temperature below which the solution is unstable, varies directly with the ion density,⁽⁷⁾ and this causes the difference in the $\kappa = 1$ and $\kappa = 3$ cases in Fig. B-3.

4. Parametric Study ($T_e = 10 \text{ keV}$)

Calculations have been performed to determine the range of plasma and reactor parameters which are accessible for a quasi-steady-state TEPR operating at $T_e = 10 \text{ keV}$. The fixed parameters cited previously were used. Some of these results are presented in Table B-1, in a format intended to emphasize the dependence upon toroidal field ($B_{\max}^{\text{TFC}}$) and geometry (κ and A). For a given $B_{\max}^{\text{TFC}}$, κ and A , Table B-1 gives the parameters which would be associated with a device driven with a neutral beam to $Q_p \approx 6.5$ ($\xi = 1$), which is about the lowest value of interest for a TEPR. ΔB_{OH} in this table is calculated for the OHC located outside the TFC and accounts only for the inductive flux. Somewhat less than a factor of two should suffice to account for plasma resistive

Fig. B-3. Temperature-Density Stability Index (solid line — $B_{\text{max}} = 85$ kG, $\kappa = 1$, $A = 3$; dashed line — $B_{\text{max}} = 70$ kG, $\kappa = 3$, $A = 5$, $P_T = 150$ MW)

Table B-1. Tokamak Parameters

(T = 10 keV, $P_T = 150$ MW, Blanket + Shield = 1 m, TFC + OHC = 1 m)

Ignition Devices											
B_{max} (kG)	κ (b/a)	A	R (m)	R_v (m)	a (m)	P_w (MW/m ²)	I_p (MA)	Δt_{ind} (V-sec)	B_{OH} (kG)	τ_{ind} (V-sec) mfr	ρ_a
50	1	3	16.10	7.66	5.37	0.03	9.62	278	15.1	0.73	0.75
	1	4	18.20	10.70	4.55	0.03	7.34	288	8.0	0.88	1.18
	1	5	21.30	14.20	4.23	0.03	6.07	315	5.0	0.97	1.67
	2	3	7.82	2.69	2.61	0.07	10.30	144	63.2	0.80	0.81
	2	4	8.48	3.94	2.12	0.08	7.71	141	29.0	0.99	1.29
	2	5	9.63	5.32	1.93	0.08	6.32	148	16.7	1.12	1.83
	3	4	5.20	1.64	1.30	0.16	8.26	93	109.0*	1.15	1.44
	3	5	5.72	2.35	1.14	0.16	6.69	93	53.9	1.33	2.06
70	1	3	10.60	4.39	3.53	0.06	8.38	160	26.4	0.96	0.80
	1	4	11.80	6.25	2.95	0.07	6.35	162	13.1	1.17	1.27
	1	5	13.60	8.34	2.72	0.06	5.23	173	7.9	1.31	1.80
	2	3	5.60	1.30	1.87	0.14	9.17	92	159.0*	1.00	0.86
	2	4	5.89	2.12	1.47	0.17	6.83	87	61.3	1.26	1.37
	2	5	6.54	2.97	1.31	0.17	5.55	89	32.0	1.45	1.96
	3	5	4.14	1.15	0.83	0.32	6.00	61	146.0*	1.65	2.17
	3	6	4.39	1.51	0.73	0.33	4.88	57	79.7	1.82	2.83
85	1	3	8.53	3.12	2.84	0.10	7.78	119	39.0	1.12	0.83
	1	4	9.32	4.52	2.33	0.11	5.67	118	18.4	1.37	1.32
	1	5	10.60	6.09	2.12	0.10	4.81	125	10.7	1.54	1.88
	2	4	4.88	1.42	1.22	0.25	6.42	67	107.0*	1.43	1.42
	2	5	5.33	2.05	1.07	0.26	5.19	67	50.9	1.66	2.02
	3	5	3.50	0.67	0.70	0.43	5.70	49	342.0*	1.84	2.23

*Does not satisfy B_{OH} constraint.

Driven Devices ($P_{in} = 20$ MW, $Q = 4.5$)											
B_{max} (kG)	κ (b/a)	A	R (m)	R_v (m)	a (m)	P_w (MW/m ²)	I_p (MA)	τ_{ind} (V-sec)	B_{OH} (kG)	τ_{ind} (V-sec)	ρ_a
50	1	3	13.70	6.24	4.54	0.03	8.84	198	14.7	0.71	0.89
	1	4	15.40	8.78	3.85	0.03	6.12	203	8.4	0.86	1.35
	1	5	17.90	11.40	3.59	0.03	5.05	221	5.7	0.95	1.91
	2	3	6.86	2.12	2.29	0.06	8.64	107	75.8	0.77	0.92
	2	4	7.36	3.16	1.84	0.09	6.49	103	33.0	0.94	1.47
	2	5	8.30	4.20	1.64	0.09	5.30	107	18.8	1.08	2.09
	3	4	4.63	1.24	1.16	0.17	7.01	70	188.0*	1.09	1.63
	3	5	5.04	1.83	1.01	0.18	5.64	69	84.0	1.27	2.34
70	1	3	9.21	3.53	3.07	0.07	7.04	114	29.4	0.93	0.91
	1	4	10.10	5.06	2.53	0.08	5.32	116	14.3	1.14	1.45
	1	5	11.40	6.80	2.32	0.08	4.37	123	8.5	1.28	2.04
	2	3	5.01	1.63	1.67	0.11	7.79	70	221.0*	0.95	0.97
	2	4	5.20	1.64	1.30	0.19	5.78	65	76.1	1.20	1.54
	2	5	5.72	2.25	1.14	0.20	4.68	65	37.7	1.39	2.22
	3	5	3.72	0.83	0.78	0.33	5.11	66	214.5*	1.55	2.46
	3	6	3.91	1.13	0.65	0.36	4.14	43	190.5*	1.72	3.26
85	1	3	7.46	2.47	2.49	0.11	6.56	88	45.7	1.07	0.94
	1	4	8.06	3.64	2.03	0.12	4.93	86	20.6	1.33	1.51
	1	5	9.12	4.93	1.82	0.12	4.03	90	11.7	1.50	2.14
	2	4	4.36	1.05	1.09	0.27	5.45	51	187.6*	1.36	1.60
	2	5	4.72	1.58	0.94	0.29	4.39	50	84.0	1.58	2.29
	3	5	3.20	0.43	0.64	0.44	4.86	38	651.0*	1.72	2.52

losses. Thus, an inductive $\Delta B_{OH} \approx 100$ kG represents an upper limit to the maximum field in the OHC when resistive allowance is made for losses. ΔB_{OH} can be reduced by locating the OHC outside the TFC, but this creates other problems.

a. Circular Plasma Cross Section ($\kappa = 1$)

The minimum size device with a circular plasma cross section is constrained by the maximum field that can be obtained at the TFC ($A \approx 3$ leads to a minimum R). For $B_{max}^{TFC} = 85$ kG, an ignition device would have $R \approx 8.5$ m, and this could not be reduced to less than $R \approx 7.5$ m by operating in the driven mode with $Q_p \approx 6.5$. A TEPR with $R \approx 8.5$ m and $a \approx 2.8$ m would represent a substantial extrapolation beyond PLT and TFTR (see Table I-2).

At 150 MWt these large devices ($R \approx 7.5$ – 8.5 m and $A = 3$) would operate with $\beta_0 < 1$ (a range which has already been achieved experimentally) and with rather low first-wall neutron loads of $P_w \lesssim 0.1$ MW/m². Although the volt-seconds required to create the plasma current imply substantial power supplies, the core (r_v) is so large that the requirements on the OHC (ΔB_{OH}) are quite modest. The volt-seconds required to maintain the plasma current are negligible relative to those required to create it.

A further set of calculations was performed on the $\{B_{max}^{TFC} = 85$ kG, $\kappa = 1$, $A = 3\}$ case to examine ways in which the size of the device could be reduced and to establish the sensitivity of the results to some of the important constraints and parameters. The results are displayed in Fig. B-4 in a manner that emphasizes the trade-off between injected power (driven mode), size, and plasma current.

The size of the device is very sensitive to the maximum field that can be obtained at the TFC. The development of superconductors which could operate substantially above the niobium-titanium maximum field (~ 85 kG) would be a possible means of reducing the size. Increasing B_{max}^{TFC} would also decrease the required plasma current, due to the decrease in size. However, the advantage to be gained from the development of higher-field superconductors is greater for devices in the TEPR class than for the larger commercial devices — for a given B_{max}^{TFC} (assuming roughly constant blanket plus shield thickness) Eq. (A-33) shows that the field in the plasma increases with increasing R.

Fig. B-4. Trade-off and Sensitivity (base case — $B_{max} = 85$ kG, $\kappa = 1$, $A = 3$, $q = 2.5$, $Z_{eff} = 1.3$, $\Delta_B + \Delta_S = 1$ m, $\Delta_M = 1$ m, $n\tau = TIM$)

Reducing the blanket thickness is another feasible way to reduce the size. It is seen that the reduction in R is about a factor of 2 greater than the reduction in blanket thickness since the field in the plasma increases as the blanket thickness is reduced. This reduction in blanket thickness need only be made on the inside of the torus insofar as its effect upon R. Thus, a design with a ~0.5 m shield on the inside of the torus and a blanket and shield on the outside of the torus could lead to ~1 m reduction in R relative to a design with a symmetric blanket. Decreasing the blanket thickness would also decrease the required plasma current.

The size increases substantially with aspect ratio for $A \gtrsim 3$. $A \approx 3$ leads to minimum R. On the other hand, the required plasma current decreases with increasing A; however, not much advantage results from this.

As discussed previously, the size of a relatively highly driven device can be reduced by increasing the Z_{eff} of the plasma, although increasing Z_{eff} increases the size of ignition and slightly driven devices. In fact, Z_{eff} must be less than 2.8 for an ignition device to be achievable. Increasing Z_{eff} increases the plasma current that is needed for ignition and near-ignition devices primarily because of the Z_{eff} effect on the size of such devices. The impurity is ^{26}Fe , except for the $Z_{\text{eff}} = 1.3$ case, in which it is ^{16}O .

The penetration of the neutral beam into the plasma depends upon the beam energy, U_B , and the plasma density. The mean-free path, in meters, for "ionization" is

$$\lambda \approx \frac{2.75 \times 10^{17} U_B (\text{keV})}{n_e (\text{m}^{-3})},$$

and the beam energy will be deposited preferentially near the center of the plasma when $\lambda > a/4$.⁽⁸⁾ For the $\{B_{\text{max}} = 85 \text{ kG}, \kappa = 1, A = 3\}$ case, $n_e \approx 7 \times 10^{19} \text{ m}^{-3}$. Thus $\lambda > a/4$ for $U_B \gtrsim 180 \text{ keV}$. Injected deuteron energies of $U_B = 300$ and 500 keV were also examined. The results were practically indistinguishable from those with $U_B = 180 \text{ keV}$.

If future experiments indicate that plasmas are stable for $q < 2.5$, a size reduction would result from operating at $q < 2.5$. However, in present machines, the energy confinement deteriorates for $q \lesssim 3$, which is a larger

value than is generally thought to be necessary on theoretical grounds. Size is rather sensitive to q , and plasma current is much less so.

Perhaps the largest single uncertainty in these calculations is the confinement scaling relation of Eq. (A-28). This may be rewritten,

$$n\tau = \alpha f(I, A, B_t, T_e), \quad (B-3)$$

where α is an adjustable parameter which we vary to determine the sensitivity to the scaling law. In Fig. B-4, $n\tau \times 10$ corresponds to $\alpha = 10$, etc. If the confinement turns out to be better than predicted by TIM theory by an order of magnitude, a substantial reduction in size and plasma current would result.

A further appreciation of the importance of and uncertainty associated with the confinement scaling law can be obtained by repeating the calculations for the $\{A = 3, B_{\max}^{TFC} = 85 \text{ kG}\}$ case using the pseudoclassical relation, Eq. (A-24)

$$I^2 = \frac{(n\tau)_p Z_{\text{eff}}}{1.8 \times 10^8 T_e^{1/2}} \quad (B-4)$$

instead of Eq. (A-28). With pseudoclassical scaling, the major radius is $R \sim 2.5 \text{ m}$ and the plasma current is $I_p \sim 0.9 \text{ MA}$ for an ignition device, as contrasted with $R \sim 8.5 \text{ m}$ and $I_p \sim 7.8 \text{ MA}$ with TIM scaling. Pseudoclassical scaling is in relatively good agreement with recent experiments. If the confinement is an order of magnitude worse than predicted by the pseudoclassical relation, the major radius of an ignition device is still $R < 5 \text{ m}$.

Thus, there are at least five factors which could combine to reduce the size of the device to $R = 5.5\text{--}6.5 \text{ m}$, a range which represents a more modest extrapolation from the TFTR. Three of these factors are technological: development of higher field superconductors capable of $B_{\max}^{TFC} \gtrsim 120 \text{ kG}$, design for minimum shield on the inside of the torus, and impurity control to regulate Z_{eff} . The other two factors, better confinement than is predicted by TIM theory and stable plasma configurations at $q < 2.5$, depend upon how the plasma physics turns out, although the latter may be amenable to some form of feedback stabilization.

b. Noncircular Plasma Cross Section

Using a noncircular plasma cross section significantly reduces the size of the device. The reasons for this follow from some approximate arguments based upon Eqs. (A-28), (A-29), and (A-33). For a given $n\tau$, the required plasma current is determined from Eq. (A-28). Ampere's law is

$$2\pi a \ell B_{\theta}(a) = \mu I, \quad (B-5)$$

where B_{θ} is the poloidal field. The safety factor depends upon the circumferential distance in the poloidal and toroidal directions, as well as the poloidal and toroidal fields

$$q = \frac{2\pi a \ell}{2\pi R} \cdot \frac{B_t}{B_{\theta}} = \frac{\ell B_t}{A B_{\theta}}. \quad (B-6)$$

These two effects are embodied in the factor ℓ^2 in Eq. (A-29), which leads to a smaller a , hence R , for noncircular plasmas with fixed q , I_p , B_t , and A . The smaller toroidal field in the noncircular plasma which results as a consequence of Eq. (A-33), for a given $B_{\text{max}}^{\text{TFC}}$, acts to partially offset the reduction in R which can be obtained by using noncircular plasma cross sections.

In the noncircular plasma calculations the plasma minor dimension, a , is taken as the scale length in deriving the confinement expression. Thus, $A \equiv R/a$ is used in Eq. (A-28) to relate $n\tau$ to size. Elsewhere in this report, the product $a\ell$ is taken as the scale length, with the result that $\bar{A} \equiv R/a\ell$ is used in Eq. (A-28). The procedure used in this appendix makes the noncircular plasmas look rather more favorable in comparison with the circular plasmas than does the procedure used in Chapters II and III.

For devices with noncircular plasma cross sections of $\kappa \geq 3$, the minimum R is constrained by the maximum change in magnetic field in the OHC (ΔB_{OH}) because of the small core. This is particularly acute for the smaller aspect ratios, and $A \approx 5$ seems attractive for this reason. The values of ΔB_{OH} given in Table B-1 are computed for transformer (OHC) windings mounted outside of the TFC. Mounting the OHC inside the TFC (nearer to the plasma) relaxes the ΔB_{OH} constraint considerably.*

* Placing the OHC inside of the TFC may introduce difficulties with accessibility and coil interactions.

Aside from the reduction in size relative to the circular cross-section plasma, the most striking feature about the noncircular plasma cross-section devices to emerge from Table B-1 is that they can operate with maximum fields in the TFC well below the limiting values of 80-85 kG. In fact, reduction of $B_{\max}^{\text{TFC}}$ is an attractive way to relax the requirements on ΔB_{OH} . It appears possible to obtain an ignition device with $R \approx 4.1 \text{ m}^*$ for noncircular plasmas with $\kappa = 3$, $R/a = 5$ at $B_{\max} \approx 70 \text{ kG}$. However, such devices would operate with $\beta_\theta \approx 2-3$ and with a first-wall neutron load $P_w \approx 0.2 \text{ MW/m}^2$. Lower β_θ and P_w could be achieved simply by operating at reduced power, $P_T < 150 \text{ MW}$.

The driven mode of operation is less attractive for noncircular cross sections than it is for circular cross sections, for two reasons. Ignition devices with reasonable size major radii can be obtained, and there is little incentive to rely upon the yet unproven neutral beam technology any more than is necessary. The reduction in size which is gained by driving the device exacerbates the ΔB_{OH} problem. Thus, with the noncircular devices, the driven mode of operation does not appear to be essential and can be held in reserve in case some other assumption proves to have been overly optimistic.

A further set of calculations was performed on the $\{B_{\max}^{\text{TFC}} = 70 \text{ kG}, \kappa = 3, A = 5\}$ case to determine the sensitivity of the results to some of the important constraints and parameters. These results are plotted in Fig. B-5. Significant reductions in major radius could be achieved by reducing the blanket thickness on the inside of the torus and, in ignition or near-ignition devices, by controlling impurities. If it turns out that we have been too optimistic on the plasma physics — if it is necessary to operate with $q = 3$ or if the confinement is poorer by a factor of ~ 5 than is predicted by the TIM theory — a noncircular device with $R \approx 5 \text{ m}^{**}$ should still be achievable. Some of the cases in Fig. B-5 represent limiting cases in the sense that the ΔB_{OH} constraint can barely be satisfied. $q = 2$, $B_{\max}^{\text{TFC}} = 85 \text{ kG}$, and $n\tau \times 10$ are in this category.

* 5.9 m if $a\lambda$ is used as the scale length in Eq. (A-28).

** $R \approx 7 \text{ m}$ if $a\lambda$ is used as the scale length in Eq. (A-28).

Fig. B-5. Trade-off and Sensitivity (base case — $B_{max} = 70$ kg, $\kappa = 3$, $A = 5$, $q = 2.5$, $Z_{eff} = 1.3$, $\Delta B + \Delta S = 1$ m, $\Delta M = 1$ m, $nT = TIM$; dashed line indicates violation of ΔB_{OH} constraint).

4. Summary

The plasma and related reactor parameters for a 150-MWt tokamak experimental power reactor (TEPR) were calculated on the basis of trapped-ion-mode plasma confinement scaling. These calculations indicate that quasi-steady-state reactors with plasma currents in the range ~ 4 –10 MA and with injected neutral beam power in the range 0 (ignition) to 20 MW ($Q_p \approx 6.5$) can be achieved within realistic technological constraints on toroidal and ohmic field coils and on first-wall power fluxes. For circular plasma cross sections, the major radius of such a device would be in the range of 7.5 to 8.5 m, even with a toroidal field at the coils of 85 kG. Several design, technological, and plasma constraints were examined which could possibly reduce this size. With noncircular plasma cross sections, devices with a major radius in the range 3.5 to 4.5 m* are obtainable for a plasma height-to-width ratio of 3 at a maximum field of 70 kG. The minimum size is constrained by the maximum field obtainable in the toroidal field coils for the circular plasma cross-section devices and by the maximum field obtainable in the ohmic heating (transformer) coils for the noncircular plasma cross-section devices. The trade-off among injected power, size, and plasma current was examined in detail, and the sensitivity of this trade-off to several plasma and reactor parameters was established.

* ~ 5 –6 m if a_l is used as the scale length in Eq. (A-28).

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APPENDIX C

MATERIALS CONSIDERATIONS

1. First-Wall System

For purposes of this report the first-wall system is defined as the vacuum wall surrounding the plasma region and the other associated components that face the plasma. The primary components of the first-wall system include: (1) the primary-containment vessel, (2) a flux-breaker to minimize the ohmic heating of the vacuum wall, (3) a limiter and liner if required to prevent excessive damage of the vacuum wall or excessive plasma contamination caused by bombardment of the wall by high-energy plasma particles, and (4) all penetrations of the vacuum wall such as the neutral-beam, vacuum, and diagnostic ports.

Since the first-wall components face the plasma and serve as the first physical barrier that isolates the plasma from the rest of the reactor system, the materials of these components are exposed to the most severe radiation environment in the reactor. As a result, the materials problems encountered by the first-wall components present one of the major technological constraints to the successful operation of a fusion power reactor.

a. Primary-Containment Vessel

(1) Function

The primary function of the first-wall system is to provide a region of appropriate geometry and sufficiently pure environment that a satisfactory plasma burn can occur. To accomplish this the first-wall system must: (1) prevent excessive plasma contamination from external leakage, (2) not itself be a source of excessive contamination to the plasma, and (3) maintain its integrity for sufficient times under the severe conditions imposed by an operating fusion reactor.

(2) Materials Requirements

In order to ensure a minimum of plasma contamination from the atmosphere, the vacuum wall must be constructed of a nonpermeable material, most likely a metal. Because of the large size of the vacuum chamber, it will be necessary to assemble the vacuum wall in sections or modules. Therefore, reliable welding or joining methods for the first-wall material are essential. Similarly,

the flux-breaker must be constructed from nonpermeable material and assembled with the vacuum wall by leak-tight joining procedures. Reliable joining of the access ports to the primary vacuum wall is also essential.

The internal sources of plasma contamination must also be maintained at acceptable levels to permit plasma burn. The major internal sources of plasma contamination include (1) sputtering or blistering of the first wall caused by high-energy neutrons, ions, electrons, and photons, (2) products of chemical interactions between the first-wall materials and plasma ions, (3) outgassing or desorption from the first-wall materials, and (4) vaporization of the first wall. Probably the major source of internal contamination results from sputtering of the first-wall material. The sputtering rates of materials are measured in terms of the sputtering coefficient, S , which is the ratio of the number of wall-atoms sputtered per incident particle. The available data on sputtering coefficients are quite limited and, particularly for neutron sputtering, considerable discrepancy exists between data of various investigators as well as between theory and experiment. For this preliminary assessment, it is not appropriate to distinguish between sputtering coefficients for the metals of interest because of the lack of data and the degree of uncertainty. Table C-1 summarizes the sputtering-coefficient data obtained from the literature⁽¹⁻¹³⁾ for the various incident particles. The recommended values are representative of the combined data and the probable range is indicative of the uncertainty in the data. One can see that the range for neutron-sputtering coefficients is particularly large. This is due to a large extent to the "chunk" type sputtering, which has been observed by some investigators⁽⁶⁾ but not by others.⁽¹⁴⁾ An effect of stress state of the material, vis., annealed versus cold-worked, and surface roughness on the sputtering rate has been observed for some materials.⁽⁶⁾ A further complication, which has yet to be determined, is the synergistic effect of the sputtering and blistering processes. Helium and hydrogen ions cause blistering of metal surfaces under certain conditions; however, the importance of blistering on plasma contamination during simultaneous sputtering processes is unclear. Sputtering coefficients for nonmetals are less well known but are generally assumed to be slightly lower than those for metals.⁽¹⁵⁻¹⁶⁾ As discussed in Chapter III, the detrimental effect on the plasma that is caused by sputtering of the first wall is dependent not only on the amount of material leaving the first wall but also on the atomic number. Since this detrimental effect on

Table C-1 Sputtering Coefficients for TEPR First-Wall Materials

Incident Particle	Mean Energy	Sputtering Yield, atoms/ion	
		Recommended	Maximum Probable Range
n	14 Mev	5×10^{-4}	$5 \times 10^{-5} - 5 \times 10^{-1}$
n	0.1 - 10 Mev	2×10^{-4}	$1 \times 10^{-4} - 5 \times 10^{-2}$
D ⁺	20 Kev	5×10^{-3}	$2 \times 10^{-3} - 2 \times 10^{-2}$
T ⁺	20 Kev	7×10^{-3}	$3 \times 10^{-3} - 3 \times 10^{-2}$
He ⁺	20 Kev	5×10^{-2}	$1 \times 10^{-2} - 1.5 \times 10^{-1}$
He ⁺	100 Kev	1×10^{-2}	$8 \times 10^{-3} - 2 \times 10^{-2}$

the plasma is a power function of the effective atomic number, there is a strong incentive to utilize low-Z materials for first-wall components facing the plasma.

The chemical interactions between hydrogen isotopes from the plasma and the high-temperature first wall must be analyzed as a source of plasma contamination.⁽¹⁷⁻¹⁸⁾ This may be particularly important if carbonaceous materials are utilized within the vacuum wall. Materials with low-vapor pressures at elevated temperatures must be utilized in the TEPR first wall to prevent excessive plasma contamination. Other sources of plasma contamination from the first-wall system that effect the first-wall design and selection of materials include residual leaks and outgassing or desorption of impurities from the first-wall surface.

The first-wall system must maintain its structural integrity for extended lifetimes under the severe conditions imposed by the fusion reaction environment. The structural integrity of the first-wall system may be reduced both by wall thinning, as a result of erosion processes, and by bulk changes in the mechanical properties. These limitations appear to be less restrictive than the plasma contamination problems for the proposed TEPR operating parameters, viz., 0.2 MW/m² wall loading, ~30% duty cycle, and ~5-10 year lifetime at power.

However, maintenance of the structural integrity of the first wall for the long times and severe conditions anticipated for the commercial power reactors appears to be a much more difficult objective to achieve.

The radiation environment produces both surface and bulk effects that may limit the allowable lifetime of the first-wall components. Substantial surface erosion, due to sputtering, blistering, and spalling of materials during bombardment by high-energy neutrons, ions, electrons, and photons, has been observed.⁽¹⁻¹⁸⁾ Although significant discrepancies exist in the very limited data available, calculated erosion rates based on available sputtering and blistering data⁽¹⁻¹⁸⁾ are not excessive (< 0.1 mm/yr) for TEPR conditions. Therefore, sufficient wall thicknesses can be utilized to allow for this amount of thinning. The more critical concern is to determine where the eroded material deposits and the subsequent consequences of this deposition on reactor operation and shielding requirements. This is particularly true for materials that become highly radioactive.

The bulk irradiation effects that must be considered in assessing the lifetime of the first wall include void swelling, radiation damage embrittlement, helium and hydrogen embrittlement, and transmutation effects. Void swelling and embrittlement are known to occur extensively in stainless steel and have been observed to a greater or lesser extent in most metals of interest.⁽¹⁹⁻³¹⁾ The extent of void swelling in metals varies with several parameters, e.g., temperature, dose rate, prior history (annealed, cold-worked, etc.), and alloying. The temperature dependence of void swelling has been fairly well established in stainless steel.⁽¹⁹⁻²²⁾ In addition, the temperature at which maximum swelling occurs increases with an increase in dose rate.⁽²²⁾ Extensive work on the effect of pretreatment has shown that 20% cold-worked, Type-316 stainless steel swells considerably less than the solution annealed material under typical fast-fission neutron irradiation.⁽¹⁹⁻²²⁾ Unalloyed vanadium swells extensively in a fast-fission neutron spectrum; however, small alloying additions of titanium or chromium reduce the swelling to barely detectable levels under similar irradiation conditions.⁽²³⁾ Differences in the swelling behavior between niobium and the niobium-1% zirconium alloy have also been investigated.⁽²⁴⁾ Ion-simulation studies are now being used in attempts to obtain data at the high-damage levels expected in fusion reactor applications, since sufficient damage levels are not feasible using existing

neutron-irradiation facilities.⁽³¹⁾ High-neutron irradiation levels also affect the ductility of most materials.⁽²⁶⁻²⁹⁾ Large reductions in the ductility are likely to be the most critical irradiation effect on the mechanical integrity of the first-wall materials. Many alloys exhibit ductilities (uniform elongation) of less than 1% after irradiation to neutron fluences of $10^{22} - 10^{23}$ n/cm².⁽²⁵⁾ The effect of simultaneous helium and hydrogen production in metals during neutron irradiation has not been investigated for the range of conditions expected in fusion reactor environments. The effect of helium on swelling is believed to result from the production of a higher concentration of voids.^(21,30) The exact mechanism for helium embrittlement is yet to be resolved, but it is believed that helium bubbles at the grain boundaries link up under an applied stress to initiate a crack, since fracture typically occurs intergranularly.⁽²⁶⁾ Although the effect of other transmutation reactions must be considered, the extent of compositional changes anticipated for the TEPR are not expected to significantly affect the mechanical integrity of the structural materials. Since the combined radiation levels anticipated for the first wall greatly exceed those that have been tested for most materials, considerable extrapolation of available data is required to estimate the total bulk damage of the first-wall materials.

Chemical interactions pose a variety of effects that relate to the integrity of the first-wall system.⁽¹⁷⁻¹⁸⁾ Chemical interactions between the first-wall materials and plasma ions may occur. Also, possible interactions between the vacuum wall material and, for example, potential liner materials for the relatively high-hydrogen partial pressures in the plasma region may be important, particularly for designs utilizing high-temperature carbonaceous liners. Depending on the design and the method of cooling the first wall, the extent of chemical interactions between the first-wall materials and the coolant or impurities in the coolant must be considered. As in the case for surface erosion due to sputtering or blistering of the first wall, the more critical consideration may be where the corroded material deposits in the coolant system.

Several thermal effects pose serious design problems in the first-wall system. Besides the typical reduction in mechanical strength observed at high temperatures in most materials, high-thermal gradient stresses and thermal fatigue, caused by the intermittent burn cycle, are quite severe. The thermal-fatigue problem may be particularly critical in view of the

simultaneous irradiation embrittlement phenomena discussed previously. The possibility of partial melting or vaporization of liner or limiter materials during plasma quenching and will require further analysis when the plasma physics is established in more detail.

Several physical effects may also be important when analyzing the structural integrity of the TEPR first-wall system. Mechanical fatigue or erosion caused by a high-velocity coolant in the first-wall system may seriously limit the operating parameters of the TEPR. Also, limiting stresses produced in the first wall by high-pressure coolants or eddy currents induced by the magnetic fields must be established.

The major neutronic effects that must be considered in the selection of materials for the first-wall system include the induced radioactivity from long half-life isotopes, the afterheat produced by short half-life isotopes, and the reduction in breeding capability as a result of parasitic neutron capture in first-wall materials. For reasons of shielding, safety, and remote maintenance requirements, it is generally desirable to select materials that produce a minimum of induced radioactivity. However, the importance of this problem, particularly for the TEPR, is dependent upon the selection of other materials in the reactor. If high levels of radioactivity are produced in other regions, e.g., the blanket, shield, or magnet regions, the penalty for producing high levels of radioactivity in the first wall may not be critical. The associated afterheat problem relates primarily to safety aspects during reactor shutdown and is, therefore, design sensitive. Since breeding of tritium is not an objective of Stage I operation of TEPR, the effect of the first wall on breeding is of limited significance. However, since demonstration of breeding in a blanket module is an objective of Stage II operation, the parasitic neutron absorption in the first-wall system becomes important.

A final consideration that appears to be of substantially different importance in the TEPR in comparison to a commercial power reactor relates to the permeability of deuterium and tritium through the vacuum wall. For the Stage I TEPR with no breeding capability, this is primarily a fuel-handling and safety concern since loss of fuel from the plasma region may occur. Whereas for Stage II TEPR or a commercial power reactor with a breeding blanket, permeation of tritium into the plasma region may be more important. For the present study, this topic is discussed in Chapter VII.

(3) First-Wall Design Options

In view of the previous analysis, the available materials property data, and CTR conceptual-design studies; the first-wall system design options can generally be considered in four classifications. These classifications are: (1) a metal vacuum wall that faces the plasma, (2) a vacuum wall fabricated from sintered powder of the SAP type, (3) a composite first wall consisting of a metal vacuum wall with an adherent protective coating that faces the plasma, and (4) a metal vacuum wall with a nonmetal liner placed between the vacuum wall and the plasma.

Of the four first-wall system design options, the metal vacuum wall facing the plasma is by far the most desirable from a fabrication standpoint because of the ease in fabrication, generally more available fabrication technology, and subsequently higher reliability. Clean-metal walls typically possess good vacuum properties such as low-outgassing rates. With respect to other types of materials, metals typically have a good compromise between strength properties, ductility, and fatigue resistance. The major problems with the use of a metal vacuum wall facing the plasma relate to the large currents of high-energy neutrons and ions incident on the wall. Substantial amounts of sputtering and blistering of the wall facing the plasma will occur for all metals. The particular significance of the sputtering is the effect on the plasma, since most metals considered for the vacuum wall have a relatively high atomic number. The bulk-irradiation damage varies considerably from one metal to another, as do the neutronic considerations. Potential plasma quenching problems may be critical for this design concept, since some of the metals of interest have fairly low melting points. Particular types of metals considered for vacuum wall applications include: (1) austenitic stainless steels, (2) commercial nickel-base superalloys, (3) refractory-metal alloys, viz., vanadium-, niobium-, and molybdenum-base alloys, (4) aluminum alloys, and (5) beryllium. Each of the metals listed has a particular property that makes it attractive for first-wall application, however, the optimum choice will depend on the objectives of a particular reactor design. Table C-2 summarizes several considerations of materials properties and gives an assessment of these properties for potential first-wall materials. It should be emphasized that the listed values give a qualitative rating and that a particular reactor design should

Table C-2 Evaluation of Potential First-Wall Materials for Tokamak
Experimental Fusion Power Reactor (TEPR)

Good = 1 → 4 = Poor

PROPERTY REQUIREMENT	ALLOY RATING ^a						
	V	Nb	Mo	Fe	Ni	Al	Be
Fabricability, Weldability	2	2	4	1	1	2	3
High Temperature Mechanical Properties	1	1	1	3	2	4	4
Melting Point-Vapor Pressure	2	1	1	2	2	4	3
Radiation Swelling Resistance	1	2	2	4	1	?	4
Radiation Damage Embrittlement	2	3	3	4	3	4	4
Sputtering Resistance	2	2	3	3	3	3	2
Blistering Resistance	2	2	2	3	3	4	?
Low-Z Benefit	3	4	4	3	3	2	1
Effect on Breeding Ratio	2	3	3	3	4	3	1
Induced Activity	1	4	3	4	4	1	1
Compatibility With Liquid Lithium	2	2	1	4	4	4	?
Compatibility With Helium	4	4	3	1	1	2	4
Materials Resources	3	3	2	1	3	2	4
Economic Impact	3	3	3	1	3	2	4

^aV = V - 15Cr - 1Ti

Nb = Nb - 1Zr

Mo = Mo - 0.5 Ti

Fe = Austenitic Stainless Steel (Type 316)

Ni = Superalloys (P.E. 16, Hastelloy)

Al = > 95% Al

Be = Be-Al or Be-Mg Alloys

utilize the advantageous properties of the material chosen and minimize the effects of the less desirable properties by judicious design.

The primary advantages of the stainless steels are the available fabrication technology, available resources, and economic considerations. However, stainless steels are subject to extensive swelling and embrittlement and become highly radioactive when exposed to high-neutron fluences. Stainless steel is probably unacceptable for use in contact with high-temperature lithium. The commercial nickel-base superalloys are fabricable, compatible with helium, and some have good radiation-swelling resistance; however, they become highly radioactive during irradiation and have a detrimental effect on breeding capability. Like the stainless steels, nickel-base alloys are probably unacceptable for use in contact with lithium. The interest in aluminum stems primarily from the low levels of induced activity. Aluminum is limited to temperatures below $\sim 300^{\circ}\text{C}$ and is not compatible with lithium. The main reason beryllium is included in this assessment is the low-Z benefit. Beryllium also enhances the tritium-breeding capability and has a low-induced activity, but in most other respects beryllium has very poor properties. The refractory metals are particularly attractive because of their favorable high-temperature properties and compatibility with lithium. The major criticism of the refractory metals relates to the fabrication technology. Reliable welds of molybdenum are known to be difficult to obtain, however, the questionable fabricability of vanadium and niobium is due more to a lack of experience, than from technological difficulties. As expected, several of the properties of the refractory alloys included in Table C-2 are similar; however, selected vanadium alloys exhibit superior properties with respect to radiation-swelling resistance, radiation-damage embrittlement, low-Z benefit, effect on tritium breeding, and induced activity. From the overall comparison of the alloys in Table C-2, vanadium-base alloys appear to be the most attractive for future fusion-reactor applications.

The second first-wall system design option is similar to the first except for the use of a sintered product as the vacuum wall facing the plasma instead of a metal wall. The primary advantage of this concept is the apparent superior blistering and swelling resistance of this type of material.⁽³²⁾ The major difficulties are associated with the low ductility and difficult fabricability, particularly welding, typical of this type of material. The sintered aluminum product (SAP), the most often mentioned of these materials, is also attractive

because of its low-induced activity; however, it is limited to low-temperature applications. Of particular interest is the potential of selected higher temperature sintered products that maintain the favorable properties while improving some of the less favorable ones.

The concept of a metal vacuum wall with an adherent low-Z coating facing the plasma has many attractive features. This concept maintains many of the advantages of the first concept, the metal vacuum wall, while removing some of the less favorable aspects of the metal wall such as the high-Z plasma interface that is inherent for most metals considered. The fabrication problems for this concept are not greatly increased over those of the simple metal wall, and although not essential, it is conceivable that the coating could be put on after the vacuum wall is assembled. If the primary purpose of the coating is to prevent sputtering of high-Z metal ions into the plasma, it is important to note that a crack or break in the coating will not produce a catastrophic effect as is often the case when coatings are used for oxidation protection. In fact, the more desirable coatings for this application are those whose adhesion to the metal wall is greater than its cohesion. This would tend to promote some cracking of the coating rather than scaling or spalling of large flakes. The cracking would expose only very small areas of the metal to the plasma, compared to that resulting from scaling or flaking of the coating. Materials that are of interest for the low-Z coatings include materials such as carbon or graphite, beryllium, beryllia, and alumina. In addition to the low-Z advantage of these coating materials, it may be possible to obtain improved blistering resistance similar to that noted for the sintered products.

The fourth option, which consists of a separate liner to protect the metal vacuum wall, has received much attention in recent conceptual fusion-power reactor designs. The liner may be in various forms such as the graphite curtain, a relatively thick graphite or silicon-carbide shield, etc. The major advantages of this concept are the utilization of low-Z, high-temperature materials facing the plasma. Two main disadvantages with this concept relate to increased outgassing and vacuum system problems and fabrication difficulties.

Design and fabrication of the suspended liner to operate reliably in the severe fusion-reactor environment appears to be much more difficult to achieve than that for the low-Z coating concept. The radiation growth, which has been observed in some graphites,⁽³³⁾ the possibility of establishing a discharge between liner and wall, and possible chemical interactions between the

hydrogenous plasma, carbon in the liner, and the metal vacuum wall must be considered in the design of the liner concept.

b. Flux-Breaker

A flux-breaker in the primary vacuum vessel is required to disrupt the flux that develops in the metal vacuum wall due to magnetically induced currents produced by the ohmic-heating coils. The flux-breaker must have a low-electrical conductivity and may consist of either a thinned metal section or a ceramic insulator ring in the torus. Many of the materials considerations for the primary containment vessel are also important for the flux-breaker, therefore, the discussion here will be limited. The material selected must be compatible with the rest of the first-wall system and must maintain its mechanical and electrical properties in the severe thermal and irradiation environment of the first-wall region. The first-wall system can probably be designed to avoid direct exposure of the flux-breaker to the plasma, thereby reducing the sputtering considerations. Incorporation of the flux breaker into the vacuum wall to obtain a reliable leak-tight system is likely to be a major consideration.

c. Plasma Aperture

The satisfactory inclusion of a plasma-aperture limiter into the first-wall system is extremely important and will require careful coordination with the plasma physics considerations. Both structural limiters of the rail or collar type and magnetic limiters, viz., toroidal or poloidal divertors, have been proposed in conceptual fusion reactor designs. Structural limiters will be exposed to much the same environment as other first-wall components that face the plasma, except that they will likely be exposed to much higher plasma-particle currents and, as a result, higher-energy deposition rates. This will be particularly critical during plasma quenching since large amounts of energy could deposit rapidly in the limiter during shutdown of the discharge. Very high-temperature materials appear to be essential for limiter applications and the possibility of significant erosion of the limiter should be considered in the design. A discussion of magnetic limiters (divertors) is a subject in itself and has not been considered in this report. The protection of the plasma containment vessel by a structural or magnetic limiter or some other alternative will be critical to the successful operation of fusion power reactors.

d. Access Ports

Several access ports, such as vacuum ports, neutral-beam ports, and diagnostic penetrations, will be required in the vacuum-containment wall. Important implications of these ports relate to the fabrication and maintenance of a vacuum-tight plasma region and provision for sufficient radiation shielding. Materials considerations for these ports are similar to those for the vacuum wall. Further discussion of these components is included in the sections of this report that described the systems of which the ports are an integral part. (See Chapter V).

2. Blanket Structural Material

The blanket-structural material is required to support the blanket system and it may also serve as the containment for the breeding material, the coolant, or both. Since the blanket-structural material will be exposed to much the same thermal and radiation environments as the first wall, many of the materials considerations are similar to those discussed previously for the first-wall system. Therefore, only the materials considerations that differ substantially from those of the vacuum wall will be described here. One major difference involves the surface-irradiation effects, viz., sputtering and blistering, which will be much less critical for the blanket structure than for the first wall. The design criteria for the blanket structure should not be limited by these phenomena. However, the bulk-irradiation damage still will be an important materials consideration and the deleterious effect on the breeding ratio will be more critical in the blanket structure because of the larger mass required. These considerations include blanket concepts, which might be incorporated as test modules in Stage II, as well as those concepts appropriate to Stage I.

Compatibility of the structural material with the coolant and/or breeding material is an important consideration. In one blanket concept, liquid lithium is proposed as both the breeding material and coolant. Since commercial iron- and nickel-base alloys are probably not acceptable containment materials for lithium at proposed TEPR temperatures (up to 600°C), the refractory-metal alloys, viz., vanadium-, niobium-, and molybdenum-base alloys, are prime candidates for the structural material in this concept. As illustrated in Table C-2, selected vanadium-base alloys possess several desirable properties for this application. However, further work is required to establish the operating limitations of potential refractory-metal alloys exposed to lithium under anticipated TEPR

conditions. The major compatibility problems in a refractory metal-lithium system relate to non-metallic impurity element interactions. Additional fabrication development for selected refractory-metal alloys is essential to make the liquid lithium concept a viable option. A major question concerning the use of liquid lithium as the coolant relates to the deleterious effects on flowing lithium caused by the high magnetic field. As a result, a second blanket concept has been proposed which utilizes slowly circulating lithium as the breeding material and a different coolant, probably helium. In general, the structural materials that are compatible with lithium are not compatible with helium (at expected impurity level), and vice versa, at the temperatures of interest. This concept would probably require the development of a duplex container to separate the lithium from the helium. The critical compatibility or corrosion problem relating to either the helium or lithium coolant may be the transport of highly-radioactive corrosion products from the blanket to regions outside the shield. Careful analysis of this problem is required.

The solid-blanket concept (e.g., LiAl or $\text{Li}_2\text{Al}_2\text{O}_4$ as the breeding material) conceivably could utilize the breeding material as the structure with the coolant, probably helium, flowing through channels. Corrosion or erosion of these materials in high-velocity helium may be important. The strength properties necessary to contain high-pressure helium must be considered. The bulk-irradiation damage, viz., displacement damage, and gas (hydrogen and helium) production, will probably be a limiting factor for this concept. Relatively high gas-production rates may lead to excessive swelling or cracking. This type of damage may also be an important consideration in assessing the viability of solid-blanket concepts that utilize a separate structural material, since cracking of the breeding material may lead to deleterious physical interactions with the coolant.

A major objective of the materials selection process is the incorporation of the favorable characteristics of each blanket component, viz., breeding material, structure, and coolant, into a compatible system that will optimize the overall TEPR performance. To achieve this, a compromise in the utilization of some materials will undoubtedly be required in order to obtain a compatible system. Therefore, a reliable evaluation of the relevant trade-offs for the various blanket system materials will have a major impact on the satisfactory achievement of the Stage II TEPR objectives.

3. Primary Coolant

Both stages of operation for the TEPR described in Chapter II require a primary coolant for the blanket region that is capable of extracting sensible heat in a form suitable for conversion to electricity. This primary coolant must interface with the blanket without creating difficult materials compatibility problems, must not interact unfavorably with the plasma confinement systems, and must extract sensible heat without simultaneously occupying a large fraction of the blanket. Furthermore, it would be desirable if the coolant were a relatively benign medium that did not overly complicate blanket assembly or operation and did not require large power input for circulation. It is important to recognize that the coolant requirements for the Stage II operation are exacerbated with respect to those of Stage I because of the necessity for compatibility between the coolant and the breeding medium.

Table C-3 contains a summary of the principal advantages and disadvantages of four-coolant materials that were considered for use in Stage I and/or Stage II of the TEPR design. In making up this table, the requirements for a TEPR coolant spelled out in the previous paragraph were given special emphasis. Although these studies of the overall suitability of applicable coolant media for a TEPR are far from complete, the focus of initial design efforts for Stage I has been on pressurized water and pressurized helium, because of the substantial technology base that already exists for each. If it can be demonstrated that magnetohydrodynamic effects resulting from the flow of liquid lithium through the confinement fields of a Tokamak will not adversely effect plasma stability or the performance of lithium as a coolant, then liquid lithium will be considered very seriously as a Stage II blanket/coolant.

4. Breeding Materials

In the Stage II operation proposed for the TEPR, one or more tritium-breeding configurations will be tested to verify promising blanket concepts that could be employed in a Tokamak Demonstration Power Reactor or a second generation TEPR. These configurations will be incorporated into modular sections that might cover 10 to 20% of the torus and will include all the necessary features of an operating blanket system, e.g., blanket processing, tritium recycle, sensible heat removal, performance monitoring, etc.

TABLE C-3. ADVANTAGES AND DISADVANTAGES OF SELECTED COOLANT OPTIONS FOR TEPR

<u>Coolant Material</u>	<u>Principal Advantages</u>	<u>Principal Disadvantages</u>
1. <u>Liquid Lithium</u>	Excellent Heat Transfer Characteristics Minimum Interference with Breeding Simple Interface with a Liquid Lithium Blanket	Subject to Large MHD Effects Potentially Large Mass Transfer Rates Aggressive, Flammable Medium Requires Refractory Metal Structure
2. <u>Molten Salt</u> e.g. LiF-BeF ₂ LiCl-KCl NaBF ₄ (Others)	Good Heat Transfer Characteristics Tolerable MHD Effects More Benign than Lithium Good Prospects for Tritium Removal and Containment Simple Interface with a Molten Salt Blanket	Probably Restricted to Use at Temperatures above 400°C Subject to MHD Induced Electrolysis Radiolysis Could Enhance Corrosion Undesirable Effects of Constituent Elements on Neutronics
3. <u>Helium</u>	Most Benign Medium No MHD Effects Excellent Prospects for Tritium Containment Little Interference with Breeding Substantial Technology Base Exists	May Require Substantial Pumping Power Difficult to Interface with Liquid Lithium Blanket Requires Relatively Large (Area) Heat Transfer Interface
4. <u>Pressurized Water</u>	Good Heat Transfer Characteristics Low Void Fraction for Channeling Low Pressure Drop in Blanket Neutronically Compatible Substantial Technology Base Exists	Difficult to Interface with Liquid Lithium Blanket Limited Overall Thermal to Electrical Efficiency Potential Source of Large Protium Backflux Potential Sink for Tritium

The blanket/shield thickness will be as severely restricted for Stage II as it is for Stage I because the same toroidal-field coil set will be used; hence, it will be essential that inclusion of a tritium-breeding material not require more than a 5 to 10% increase in the overall blanket/shield thickness. In order to evaluate the prospects for effectively implementing the breeding-test stage (Stage II) for a TEPR, an assessment of various breeder-blanket options was conducted. Since there appear to be no suitable substitutes for lithium as a tritium-breeding source, only elemental lithium and lithium-containing substances were considered.

Table C-4 contains a summary of the general classes of breeder-blanket materials that are believed to have potential applicability in fusion reactors, together with a listing of principal advantages and disadvantages of each class of material. In making up this table, emphasis was placed on the following criteria: (1) melting and boiling point, (2) heat transfer characteristics, (3) breeding prospects with and without the use of beryllium, (4) breeding potential in relatively thin zones, (5) structural-material requirements, (6) induced activity and afterheat, (7) maintenance requirements, (8) tritium hold-up and recovery, (9) operational safety during normal and transient conditions, (10) MHD effects, and (11) neutronic compatibility. Many aspects of the neutronic characterization of these classes of materials are taken from results presented in Chapter VI-B and Appendix D. A perusal of Table C-4 shows that the demonstration of a breeding gain near unity with natural lithium would require a substantially thicker zone (~40 cm) than is readily accommodated in the TEPR design described in Chapter II. It is important to note, in this regard, that natural lithium is still considered a viable candidate for the breeding medium in larger power reactors based on the Tokamak concept, for which the restrictions on overall blanket/shield thickness are not expected to be as severe as they are for the TEPR. Also worthy of note is the fact that most of the tritium breeding material options that were considered require the use of enriched lithium (~90% ^6Li) and beryllium in order to realize a net breeding gain. In view of the questionable long-term availability of beryllium as a consumable fusion reactor material, serious question is raised as to which aspects of breeder-blanket performance are worth striving for in a TEPR blanket test module. At least three alternative approaches appear to deserve consideration: (1) employ a representative thickness of natural-liquid lithium, extract and

TABLE C-4. ADVANTAGES AND DISADVANTAGES OF SELECTED BREEDING BLANKET OPTIONS FOR TEPR

<u>Blanket Material</u>	<u>Principal Advantages</u>	<u>Principal Disadvantages</u>
1. <u>Liquid Lithium</u>		
A. Natural Lithium (~92% ${}^7\text{Li}$)	No Beryllium Required for Net Breeding Excellent Heat Transfer Characteristics External Tritium Removal is Possible External Make-Up and Processing	Thick Zone (>40 cm) Required for Net Breeding Probably Requires Refractory Metal Support Structure (Difficult Fabrication) Construction Materials Subject to Activation Likely to Require Remote Maintenance Difficult Tritium Recovery Problems
B. Enriched Lithium (~90% ${}^6\text{Li}$)	Best Tritium Breeding Prospects in a Thin Zone (<10 cm) External Tritium Removal is Possible External Make-Up and Processing	Required Beryllium for Net Breeding Probably Requires Refractory Metal Support Structure (Difficult Fabrication) Construction Materials Subject to Activation Likely to Require Remote Maintenance Difficult Tritium Recovery Problems
2. <u>Molten Salts</u>		
e.g. LiF-BeF_2 LiCl-LiF (Others)	Good Heat Transfer Characteristics External Tritium Removal is Possible External Make-Up and Processing	Requires Enriched Lithium and Beryllium for Net Breeding Construction Materials Subject to Activation Salts Subject to MHD Induced Electrolysis Radiolysis Could Enhance Corrosion
3. <u>Solid Materials</u>		
e.g. Li-Al Alloys $\text{Li}_2\text{O-Al}_2\text{O}_3$ $\text{Li}_2\text{O-SiO}_2$ LiF (Others)	Inherently Safer Operation (Easier Containment) Net Breeding in a Thin Zone with Enriched Lithium Best Prospects for Minimum Activation and Low Rad-Waste Designs Larger Tritium Hold-Up May be Tolerable	Practical Designs Require Enriched Lithium and Beryllium for Net Breeding Requires Large Surface Area for Tritium and Heat Removal Generally Poor Heat Transfer Characteristics May be Subject to Catastrophic Sintering or Melting Requires <u>In Situ</u> Tritium Separation, Make-Up and Processing Appears Limited to Gas Cooling

recycle whatever tritium is bred, demonstrate all aspects of the operation of a natural-liquid lithium blanket (except net breeding); (2) employ enriched liquid lithium together with enough beryllium to demonstrate a breeding gain near unity, extract and recycle tritium, demonstrate all aspects of the operation of an enriched liquid-lithium blanket; and (3) employ a lithium-containing solid blanket material (appropriately enriched with respect to ^6Li) together with beryllium and follow through in the same way as for the preceding approach. Within the framework of reasonable operating conditions for a TEPR, there appear to be several difficulties associated with operating temperature ranges and neutronic compatibility that make the implementation of molten salts a questionable endeavor for a TEPR.

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APPENDIX D

BLANKET/SHIELD SENSITIVITY AND OPTIMIZATION STUDIES

The choice of materials for a detailed design of a TEPR blanket/shield system is a complex process involving a number of technical areas such as neutronics, radiation damage, heat transfer, and economics. Besides ensuring its satisfactory operation, a prudent design of the blanket/shield must be based on careful optimization studies to meet the conflicting requirements imposed by other components of the reactor.

The purpose of this appendix is to present the results of examinations of the various trade-offs in the nuclear design of the TEPR blanket/shield upon which the preliminary reference design discussed in Chapter VI was based. The nuclear performance of various material compositions for both the blanket and magnet shield is discussed in Section D-1. Tritium breeding and non-breeding blankets are both considered in order to appreciate how a Stage II breeding module might be designed to be compatible with the basic Stage I TEPR blanket and also demonstrate a tritium breeding capability. Section D-2 is devoted to a study of the important considerations for selection of dimensions for the blanket and magnet shield. Quantitative magnet protection criteria are also developed in Section D-2.

1. Nuclear Performance of Various Material Compositions

Nuclear performance of materials is one of the most important aspects to be considered in the selection of material composition for the blanket and magnet shield. The effectiveness of various materials in attenuating neutrons and secondary gamma-rays is investigated and compared in this subsection.

Selection of material composition for the blanket/shield depends on several other considerations besides effectiveness in neutron and gamma attenuation. Material properties such as melting points, useful operating temperature ranges, material behavior under irradiation, compatibility with other system components (e.g. the coolant), and cost are some of these important considerations. Furthermore, material selection for the blanket and magnet shield depends to a large degree on the major requirements of and constraints on the overall reactor design. For example, if tritium breeding is required, then lithium in one form or another must be used in the blanket.

The high energy of the D-T neutron imposes a particularly difficult shielding problem in fusion devices operated on the D-T cycle. Space restrictions are particularly severe in small size reactors and are further complicated by the necessity of using a niobium-titanium superconductor in which the maximum practical magnetic field is limited to ~ 80 kG, as will be discussed later. A basic requirement, therefore, of materials to be used in the TEPR blanket/shield is to have large attenuation coefficients for high energy neutrons and gamma-rays. Materials satisfying these requirements are generally of moderate or large mass number (e.g., Fe, Pb, Ta, W) because inelastic scattering is the most efficient mechanism for reducing the energies of the high energy neutrons and because gamma-ray attenuation increases significantly with the atomic number. Below the inelastic threshold, however, the neutron attenuation effectiveness of these materials is greatly reduced and a lighter element (e.g., water, polyethylene, lithium hydride, graphite) should be present in the blanket/shield. Furthermore, in order to minimize the gamma emission from radiative capture reactions the use of an appropriate amount of a strong neutron absorber (e.g. boron) is necessary.

Three recent studies of fusion reactor magnet shielding have been reported in Refs. 1, 2 and 3. The study of Kriesse and Steiner⁽¹⁾ examined the attenuation properties of several materials in the magnet shield coupled to a 1 m natural lithium-graphite blanket. Among the materials considered, they found that in terms of reducing the magnet heating in a fixed shield thickness a tantalum-borated graphite mixture was the most and borated graphite the least efficient with lithium hydride, borated water, and polyethylene in between. Abdou and Maynard⁽²⁾ examined several possible shielding materials and made an attempt to optimize the magnet shield dimensions and composition on the basis of minimum reactor cost. Their study concentrated on large commercial reactors (major radius ~ 13 m) with lithium blankets. They concluded, for the materials investigated that (1) a mixture of 70% stainless steel plus 70% boron carbide results in the smallest magnet shield thickness for a given tolerable level of nuclear heating in the magnet; (2) although a mixture of 70% lead plus 30% boron carbide requires a much larger shield thickness than the 70% stainless steel plus 30% B₄C mixture, the former results in a lower overall cost; (3) the requirements of magnet protection are satisfied by the cost optimized shield; and (4) optimum shield composition and dimension are moderately sensitive to reactor wall radius, wall loading, material cost, refrigerator capital

cost as well as several other parameters. The study did not cover the effect of varying the major radius on the optimization results. Gabriel, Santoro and Engle⁽³⁾ investigated the shield design for the ORMAK-F/BX, the ORNL version of the Tokamak Fusion Test Reactor (TFTR), which has a plasma radius of 120 cm. They selected a shield which consists of a 35-cm-thick region of 65% iron plus 35% borated water surrounded by a 5-cm thick lead liner. They found that this shield is very adequate to protect the magnet at a peak fusion power of 6 MW for a total fusion energy generation of 4×10^7 MJ.

The possible options for a TEPR where the size, space restrictions, and objectives lie between the TFTR and large scale commercial reactors are examined in this study. The nuclear performance of various materials is compared primarily on the basis of nuclear heating and radiation damage in the magnet. The total (neutron plus gamma) energy leakage from the shield to the magnet is taken as a measure of energy deposition in the magnet. One of the most important effects of radiation damage to a cryogenically stabilized magnet is the increase in the resistivity of the normal-conductor stabilizer. Here, we select copper as the stabilizer and atomic displacement as the radiation damage indicator. The correlation between radiation induced resistivity and atomic displacement will be derived in the following section. Five sets of designs, A, B, C, D and E are examined. Each set consists of several alternative designs which cover the options for a particular concept of the TEPR blanket/shield. Since some of the designs are used in more than one set, each design is given a unique three-digit number. The description of dimensions and material compositions for the various designs in the five sets is given in Tables VI-1 through VI-5. All percentages in material composition given in these tables, and everywhere else in this section, are on a volume basis.

The neutron and gamma-ray fluxes were calculated in all cases by solving the discrete ordinates form of the transport equation using the ANISN⁽⁴⁾ program in the S_8 - P_3 approximation in one-dimensional cylindrical geometry. The neutron and gamma-ray multigroup cross sections were processed using SUPERTOG⁽⁵⁾ and MUG.⁽⁶⁾ The photon production cross sections were processed with LAPHNGAS.⁽⁷⁾ The neutron multigroup reaction cross sections and kerma factors were generated with MACK.⁽⁸⁾ Nuclear data of ENDF/B⁽⁹⁾ Version III were used in all cases. The displacement cross sections reported in Ref. 10 were used except in the case of copper which, for the purpose of this work, were generated with a displacement energy of 40 eV.

Design set A consists of designs 101, 102, 109, 110, and 111 as shown in Table D-1. The composition and dimensions of the blanket were varied in order to examine the effects of lithium thickness and enrichment on the total blanket/shield thickness, Δ_{BS} . In all the five designs, a 5-cm thick reflector of stainless steel (SS) and a shield of variable thickness with a 50% stainless steel plus 50% B₄C composition were used. The blankets in designs 101 and 111 are respectively 25- and 35-cm thick zones containing 90% natural lithium plus 10% structure. Vanadium (V) was used in the 1-cm thick first wall and as the structural material. Results derived here are not overly sensitive to this choice. Design 102 uses a 25-cm thick blanket of stainless steel. The blanket in design 109 consists of 2 and 3 cm regions of 90% enriched lithium plus 10% vanadium separated by a 5-cm zone of 90% beryllium + 10% vanadium and followed in the outer region by 15 cm of stainless steel. Design 110 is similar to design 109 except that the 5-cm beryllium-vanadium zone is replaced with stainless steel.

Tritium breeding ratios of 0.87, 1.1, 0.73, and 1.08 were calculated for designs 101, 109, 110 and 111, respectively. Figures D-1 and D-2 show the atomic displacement in units of dpa/(MW-yr/m²) and the total energy leakage in units of MeV/D-T neutron, respectively, as a function of the blanket plus shield thickness, Δ_{BS} . From considerations given in the next section, for a reasonable size TEPR (major radius, $R \approx 5-7$ m, thermal power ≈ 150 MWt), Δ_{BS} should be no more than 1 m, the displacement rate in the stabilizer should be in the range of 10^{-4} to 10^{-3} dpa/(MW-yr/m²) and the energy leakage to the magnet in the range of 10^{-5} to 10^{-3} MeV/D-T neutron. Note that the blanket plus shield thicknesses shown in all results for design sets A, B, C, D, and E do not include the space required for coolants, thermal expansion, access regions for fueling, dimensional changes due to radiation damage, etc. Thus the actual blanket/shield thickness in the radial dimension from the first wall to the toroidal field coils will be $\sim 15\%$ larger than the values shown in this section. From results in Figs. D-1 and D-2 the following conclusions can be made:

- a. Comparing the results for designs 101 and 111 shows that increasing T from 0.87 to 1.08 requires an increase in Δ_{BS} of ≈ 6 cm.
- b. The results for designs 109 ($T = 1.1$) and 111 ($T = 1.08$) show that using highly enriched lithium and beryllium instead of natural lithium could save as much as 20 cm on Δ_{BS} . In addition, using

TABLE D-1

Description* of Design Set A

(Thickness and enrichment of lithium are varied)

Design No.	Blanket	Reflector	Shield
101	25 cm 90% natural ^a lithium + 10% vanadium	5 cm SS	50% SS + 50% B ₄ C
102	25 cm Stainless steel (SS)	5 cm SS	50% SS + 50% B ₄ C
109	2 cm 90% enriched ^b lithium + 10% vanadium 5 cm 90% beryllium + 10% vanadium 3 cm 90% enriched ^b lithium + 10% vanadium 15 cm stainless steel	5 cm SS	50% SS + 50% B ₄ C
110	2 cm 90% enriched ^b lithium + 10% vanadium 5 cm stainless steel 3 cm 90% enriched lithium + 10% vanadium 15 cm stainless steel	5 cm SS	50% SS + 50% B ₄ C
111	35 cm 90% natural lithium + 10% vanadium	5 cm SS	50% SS + 50% B ₄ C

* All percentages in material composition are volume percentages.

^a Isotopic composition of natural lithium is 7.42% ⁶Li and 92.58% ⁷Li

^b Enriched lithium of 90% ⁶Li and 10% ⁷Li isotopic composition.

Figure D-1. Variation of Atomic Displacement in the Magnet Stabilizer (copper) with Blanket plus Shield Thickness for Designs in Set A.

Figure D-2. Variation of Total (neutron plus gamma) Energy Leakage to the Magnet with the Blanket plus Shield Thickness for Designs in Set A.

highly enriched lithium and beryllium instead of natural lithium makes it possible to minimize the lithium and tritium inventories as shown previously.⁽¹¹⁾

- c. Replacing beryllium in design 109 by stainless steel reduces T by .35% and Δ_{BS} by only 2 cm.
- d. Employing an all-stainless-steel blanket in design 102 reduces Δ_{BS} by ~27 cm relative to the natural lithium blanket (design 111) but only by 5 cm relative to design 109 (${}^6\text{Li}$ -Be blanket).

Therefore, if tritium production with a high breeding ratio is required, there will be a penalty in terms of increasing Δ_{BS} , particularly if natural lithium is to be used as the breeding medium. The problem is greatly reduced by using highly enriched lithium (~90% ${}^6\text{Li}$) in the presence of a neutron multiplier. The TEPR, as presently envisioned, will breed tritium only in a blanket experimental module in Stage II. These results indicate a number of ways that such a module could be designed to demonstrate tritium breeding capability.

The optimum volume percentages of boron carbide (B_4C) in a stainless steel- B_4C shielding mixture is examined by design set B described in Table D-2. The set consists of designs 101, 104, 105, and 106. All of the four designs, which are possible Stage II modules, have a 1-cm vanadium first wall, 25 cm blanket of 90% natural lithium plus 10% vanadium, and a 5-cm stainless steel reflector. The percentage of B_4C in the stainless steel- B_4C shield mixture is varied from zero in design 106, to 25% in design 104, to 50% in design 101, and to 75% in design 105. The variation of the atomic displacement in the magnet copper and the energy leakage to the magnet with the blanket-shield thickness are shown in Figs. D-3 and D-4, respectively. The results in Fig. D-4 show that for a specified level of energy leakage to the magnet, Δ_{BS} is smallest for the 75% stainless steel + 25% B_4C mixture which is in agreement with the results of Ref. 2. However, in terms of copper displacement, Δ_{BS} is smallest for the 50% stainless steel + 50% B_4C mixture. The difference in Δ_{BS} for the optimum compositions for atomic displacement and energy leakage is relatively small but the reason why there is a difference is interesting. The energy leakage in the multigroup treatment is the sum over all groups of the neutron and gamma-ray current for each energy group multiplied by the effective energy for the group. The atomic displacement rate is the sum over all groups of the neutron flux for each group multiplied by the displacement cross section

TABLE D-2

Description* of Design Set B

(Percentage of Boron Carbide in SS-B₄C mixture is varied)

Design No.	Blanket	Reflector	Shield
101	25 cm 90% natural lithium + 10% vanadium	5 cm SS	50% SS + 50% B ₄ C
104	25 cm 90% natural lithium + 10% vanadium	5 cm SS	75% SS + 25% B ₄ C
105	25 cm 90% natural lithium + 10% vanadium	5 cm SS	25% SS + 75% B ₄ C
106	25 cm 90% natural lithium + 10% vanadium	5 cm SS	100% SS

* All percentages in material composition are volume percentages.

Figure D-3. Variation of Atomic Displacement in the Magnet Stabilizer (Copper) with Blanket plus Shield Thickness for Designs in Set B

Figure D-4. Variation of Total (neutron plus gamma) Energy Leakage to the Magnet with Blanket plus Shield Thickness for Designs in Set B.

for this group. The displacement cross section for copper does not drop linearly with decreasing energy, e.g., its value at 1 MeV is about 25% of that at 10 MeV. Since a large fraction of the neutrons in the shield are shifted toward the low end of the MeV energy range, effective reduction of the atomic displacement in the magnet requires the use of a shield which has large attenuation coefficient for low and intermediate energy neutrons, i.e., increasing the percentage of boron carbide. The energy leakage indicator, however, emphasizes the high energy range and includes the contribution from secondary gamma rays, thus favoring a higher percentage of stainless steel. However, using an all-stainless steel blanket increases, for the same Δ_{BS} , the energy leakage and atomic displacement in the magnet by about two orders of magnitudes relative to the other stainless steel-B₄C shield mixtures. This is because of the rapid buildup of neutrons in the keV and low MeV energy range and the significant increase in gamma production from radiative capture reactions in stainless steel when B₄C is absent.

The two most likely candidates for TEPR coolant are helium and water. Lithium cooling in TEPR is a possibility for a Stage II module, providing that the necessary technology for handling liquid metal coolants under the effect of strong magnetic field are developed in a time period which meets the proposed schedule for TEPR.

Neutronics effects of using water as a coolant and moderator compared with helium and lithium cooling are brought out by comparing the nuclear performance of designs 101, 112, 102, 116, and 120 in design set C (see Table D-3). Both designs 101 and 112 include a 25-cm thick blanket of 90% natural lithium plus 10% vanadium. The lithium in both designs serves as the breeder and coolant in the blanket region and yields a tritium breeding ratio of ~ 0.9 . The shield in design 101 is 50% stainless steel plus 50% B₄C and is helium cooled. The water-cooled shield in design 112 consists of 75% SS + 20% H₂O + 5% B. No internal cooling of the 5-cm stainless steel reflector is provided for as face cooling from the blanket and shield sides is adequate. Designs 102 and 116 compare the helium and water cooling of the shield as designs 101 and 112 do, but the blanket in each of these two designs is a helium-cooled 25-cm stainless steel blanket. Design 120 is water cooled in the blanket, reflector, and shield and consists of 75% SS + 20% H₂O + 5% B. The case of lithium cooling in the shield was not considered, as the heat generation rates in the shield are too low on one hand and it is advantageous

TABLE D-3
Description* of Design Set C
(Comparison of Various Coolants)

Design No.	Blanket	Reflector	Shield
101	25 cm 90% natural lithium + 10% vanadium	5 cm SS	50% SS + 50% B ₄ C
112	25 cm 90% natural lithium + 10% vanadium	5 cm SS	75% SS + 20% H ₂ O + 5% B
102	25 cm Stainless steel (SS)	5 cm SS	50% SS + 50% B ₄ C
116	25 cm Stainless steel (SS)	5 cm SS	75% SS + 20% H ₂ O + 5% B
120	30 cm 75% SS + 20% H ₂ O + 5% B		75% SS + 20% H ₂ O + 5% B

* All percentages in material composition are volume percentages.

to keep the shield at low operating temperature with no tritium contamination on the other hand. Cooling the blanket with liquid lithium is a reasonable option only if tritium breeding is required.

Figures D-5 and D-6 show the atomic displacement in the magnet copper and the energy leakage to the magnet as functions of the blanket plus shield thickness for designs in set C. Since helium does not perturb the neutronics to any measurable extent it was not included in the calculations. Helium affects the required blanket and shield thickness, however, through a void fraction which represents the ratio of the volume of the helium coolant to the blanket or shield material volume. From heat transfer considerations (see Section VI-C), an average void fraction of 10% in the blanket region and 5% in the shield region is adequate for proper cooling at a neutron wall loading of $\approx 0.2 \text{ MW/m}^2$. Therefore, the blanket and shield thicknesses shown in Figs. D-5 and D-6 should be increased by 10%, and 5%, respectively, in case of helium cooling. The amounts of water and lithium included in the calculations are more than sufficient for cooling purposes. (The 75% stainless steel, 20% H_2O , 5% boron is a near-optimal composition, from the standpoint of nuclear radiation attenuation, for a mixture of these three materials.) From results in Figs. D-5 and D-6, the following conclusions can be drawn:

- a. Comparing the results for designs 101 and 112, and designs 102 and 116, shows that a 75% SS + 20% H_2O + 5% B is slightly better than a 50% SS + 50% B_4C shield mixture in terms of attenuation for a given thickness. Therefore, a helium cooled shield will be thicker than a water-cooled shield by slightly more than the space required for helium coolant, i.e., $\sim 5\%$.
- b. Although a 75% + 20% H_2O + 5% B in the shield region is a slightly better attenuator than the 50% SS + 50% B_4C shield mixture, 100% stainless steel is a better attenuator than both mixtures in the blanket region. This can be seen from comparing results for designs 116 and 120 and from results to be given shortly for design set D. However, the space required for helium cooling in design 102 makes design 120 a preferable choice in terms of overall blanket plus shield thickness. To attenuate the radiation level such that the atomic displacement in copper stabilizer is $1 \times 10^{-4} \text{ dpa/(MW-yr/m}^2\text{)}$ requires $\sim 85 \text{ cm}$ for both design 120 (water-cooled blanket and

Figure D-5. Variation of Atomic Displacement in the Magnet Stabilizer (copper) with Blanket plus Shield Thickness for Designs in Set C.

Figure D-6. Variation of Total (neutron plus gamma) Energy Leakage to the Magnet with Blanket plus Shield Thickness for Designs in Set C.

shield), and design 116 (helium-cooled blanket and water-cooled shield), ~88 cm for design 102 (helium-cooled blanket and shield).

Therefore, using helium coolant in both the blanket and shield increases the blanket plus shield thickness by ~4% as compared to a water-cooled blanket and shield or a helium-cooled blanket coupled to a water-cooled shield.

Reactor size is particularly sensitive to the inner clearance between the plasma and the vertical segment of the D-shaped toroidal field coil. Space restrictions in other regions surrounding the plasma are much less severe. It appears very advantageous therefore, that the blanket/shield region in the space-limited zone be constructed of materials which are very efficient attenuators; e.g., tungsten-B₄C and tantalum-B₄C mixtures. Designs 102, 103, and 108 in design set D described in Table D-4 are used as basis for comparing stainless steel with tungsten. The blanket/shield composition is 50% stainless steel + 50% B₄C in design 103, and 50% tungsten + 50% B₄C in design 108. The shield in design 102 is similar to that in design 103 but the blanket is 30 cm stainless steel. Figures D-7 and D-8 show the atomic displacement and energy leakage as function of Δ_{BS} for the three designs. Using the tungsten-B₄C mixture saves about 12 to 15% on Δ_{BS} compared with the stainless steel-B₄C mixture. Results in Chapter VI-B show also that tantalum-B₄C mixture is more effective than stainless steel-B₄C mixture, but tungsten-B₄C mixture, is better than both.

TABLE D-4
Description* of Design Set D
(Comparison of tungsten and stainless steel)

Design No.	Blanket	Shield
102	30 cm Stainless steel (SS)	50% SS + 50% B ₄ C
103	30 cm 50% SS + 50% B ₄ C	50% SS + 50% B ₄ C
108	30 cm 50% tungsten + 50% B ₄ C	50% tungsten + 50% B ₄ C

* All percentages in material composition are volume percentages.

Figure D-7. Variation of Atomic Displacement in the Magnet Stabilizer (copper) with Blanket plus Shield Thickness for Designs in Set D.

Figure D-8. Variation of Total (neutron plus gamma) Energy Leakage to the Magnet with Blanket plus Shield Thickness for Designs in Set D.

Tungsten and tantalum have the combined problems of high cost, strong activation and high density. The present material cost⁽¹²⁾ of 100 \$/kg for tungsten and tantalum is too expensive to justify its use in large quantities in the blanket/shield. However, their utilization in the inner blanket/shield in space-restricted and maximum magnetic field limited machines such as a TEPR can be very advantageous in terms of reduced reactor size for a given power level. While tungsten and tantalum activate strongly, the presence of very large amounts of boron carbide will eliminate most of the resonance and low energy region neutron capture and this will likely result in substantial reduction in tungsten and tantalum activation. Pure tungsten is very brittle and difficult to fabricate. However, there are several tungsten base alloys, with 90 to 97% tungsten content, the balance being copper, or nickel, or an iron-nickel mixture together with other additive elements.⁽¹³⁾ Some of these alloys are reasonably ductile and can be fabricated.

Induced radioactivity in the blanket, magnet shield, magnet, containment vessel and other reactor components is one of the important considerations in a tokamak reactor design. Of particular concern in the TEPR design are the effects of induced activation on personnel access for routine maintenance and necessary repairs. The maximum concentration of radioactivity in the reactor occurs in the first wall. A crude estimate of the biological dose at shutdown in a location near a first wall operated to an integral wall loading of 0.5 MW-yr/m² gives 1, 2, 3 and 6×10^8 mrem/hr for aluminum, V-20 Ti, stainless steel, and Nb-5 Zr first wall materials, respectively. These values decrease, in the same order, to 100, 3×10^6 , 1×10^8 , and 5×10^7 mrem/hr after a cool-down time ~5 weeks. It is emphasized here that these estimates are very approximate and have one-to-two-orders of magnitude uncertainty. However, comparing these values for the biological dose with a maximum permissible dose of 12 rems/yr for occupationally exposed individuals shows clearly that remote maintenance will be required for handling stainless steel, niobium, and vanadium structures. Aluminum, and perhaps graphite, may require only a minimal degree of radiation protection. It should be noted that the radioactivity builds up very rapidly during operation and decays relatively slowly after shutdown in stainless steel, niobium, and vanadium. Thus the biological dose is not very sensitive (in an order of magnitude sense) to the operating time and duty cycle. In this context, extending the cool-down time from a few weeks to a few months will not help a great deal in easing remote maintenance.

There is an incentive to examine the possibility of constructing the blanket and magnet shield from low-activation materials such as aluminum or graphite in order to ease the remote maintenance requirements. The nuclear performance of these materials is examined by designs 102, 108, 113, 114, 117, 118, and 119 in design set E (see Table D-5). The atomic displacement in the magnet stabilizer and the energy leakage to the magnet in these designs are shown in Figs. D-9 and D-10 as functions of the blanket/shield thickness. The results in these figures may be summarized as follows:

- a. Comparing the results for designs 113 and 117, and designs 114 and 118 show that there is little difference between graphite and aluminum in terms of the required Δ_{BS} for a given attenuation.
- b. Using 30 cm of aluminum (design 113) or graphite (design 117) in the blanket increases Δ_{BS} for a given attenuation, by ~20% relative to a 30-cm stainless steel blanket (design 102).
- c. Both designs 114 and 118 with 30-cm blankets of aluminum and graphite, respectively, followed by 50% aluminum + 50% B_4C shields would require increasing Δ_{BS} by a factor of 2 to produce the same attenuation obtainable with a 30-cm stainless steel blanket followed by 50% stainless steel + 50% B_4C shield (design 102).
- d. Replacing the 30-cm stainless steel blanket in design 102 by vanadium increases the required Δ_{BS} by ~8 cm.

A low induced activation system which consists of 30-cm blanket of aluminum or graphite followed by a 50% aluminum + 50% B_4C shield would require Δ_{BS} to be ~140 cm to meet the magnet protection criteria discussed later. This is too thick to be acceptable in the inner high magnetic field region in a TEPR. The use of aluminum and graphite in the outer blanket/shield will depend on many other considerations in the design. There are other problems associated with the utilization of graphite and aluminum in the blanket. For example, graphite is subject to severe radiation damage when irradiated to high fluences. An aluminum blanket imposes upon the design a low operating temperature (300 to 400°C) and a tenuous safety factor for blanket meltdown (aluminum melts at ~660°C).

TABLE D-5

Description* of Design Set E

(Performance of low induced activation materials in the Blanket and Shield compared with other materials)

Design No.	Blanket	Shield
102	30 cm Stainless steel	50% SS + 50% B ₄ C
108	30 cm 50% tungsten + 50% B ₄ C	50% tungsten + 50% B ₄ C
113	30 cm Aluminum (Al)	50% SS + 50% B ₄ C
114	30 cm Aluminum	50% Al + 50% B ₄ C
117	30 cm Graphite	50% SS + 50% B ₄ C
118	30 cm Graphite	50% Al + 50% B ₄ C
119	30 cm Vanadium	50% SS + 50% B ₄ C

* All percentages in material composition are volume percentages.

Figure D-9. Variation of Atomic Displacement in the Magnet Stabilizer (copper) with Blanket plus Shield Thickness for Designs in Set E.

Figure D-10. Variation of Total (neutron plus gamma) Energy Leakage to the magnet with Blanket plus Shield Thickness for Designs in Set E.

2. Important Considerations in Optimizing the Blanket/Shield Dimensions

a. Introduction

As indicated earlier, the size and cost of a tokamak reactor is sensitive to the dimension of the total clearance between the plasma and the toroidal field coils. To keep the size of the TEPR at a reasonable level (major radius ~ 5 to 7 m) and in the meantime produce a significant amount of power output, particular attention has to be given to optimizing the blanket/shield thickness. The magnitude of nuclear attenuation required by the magnet protection criteria cannot be compromised beyond the tolerable level for satisfactory operation of the superconducting magnet. As will be discussed in this section, experiments have shown that a substantial part of the radiation damage to the magnet can be recovered by annealing at elevated temperatures. However, magnet cooldown from room temperature to 4.2 K takes a long time (~ 2 to 3 months). Therefore, magnet protection criteria is, in some sense, related to many aspects of the overall design criteria for the reactor, e.g., the duty cycle. Besides the magnet protection, the electric power required to run the refrigerators represents another constraint which also favors increasing the blanket and shield thickness. In the following, these trade-off considerations are examined in some detail.

b. Magnet Protection Criteria

Since radiation damage to the magnet plays an important role in determining the size of the blanket shield system, quantitative correlations between the magnet damage and nuclear radiation levels need to be established. The magnet components of concern here are: (1) the superconductor; (2) the normal (stabilizing) conductor; (3) thermal and electric insulators; and (4) structural materials.

Limited, but very useful, information about the neutron irradiation effects on the magnet components have accumulated from experimental studies⁽¹⁴⁻¹⁸⁾ in the last few years. Many of these experiments were made at liquid helium temperatures; therefore, the results are applicable for superconducting magnets. The neutron spectrum, however, resembles in most cases a fission spectrum or softer. Such a spectrum is adequate for simulating neutron spectra in the superconducting magnets for large reactors where the blanket and shield may be 1.5 to 2 m with attenuation factors⁽²⁾ of $\sim 10^6$ to 10^8 . The neutron spectrum in a TEPR magnet with a blanket/shield of ~ 0.7 to 1 m, however, has a large

component of high energy neutrons. Many experiments have been performed at Argonne's low-temperature, fast-flux facility.⁽¹⁷⁾ Figure D-11 shows three neutron spectra A, B and C. Curves A and B represent the neutron spectrum in the magnet for 50 cm and 100 cm blanket/shield of stainless steel and boron carbide arranged in zones similar to that of the reference design described in Section VI-3. Curve C represents the neutron spectrum for the Argonne low-temperature, fast-flux facility. Figure D-12 shows the fraction, $f(E_0)$, of the total neutron flux with neutron energies above E_0 , as a function of E_0 for the same three fluxes, A, B and C. From the two figures, it can be seen that a typical fission spectrum such as that of C can simulate very well the neutron spectrum in a TEPR magnet for neutrons of energies below ~5 MeV. The fraction of neutrons above 0.1 MeV in C (90%) is greater than in A and B (~60%). However, the fission spectrum (C) has a very small component (1.5%) above 5 MeV and essentially no neutrons above 8 MeV. The TEPR magnet spectra (A and B) have ~5% of the neutrons of energies greater than 5 MeV. Many neutron-induced reactions [e.g., (n, α) , (n, p) , $(n, n'p)$, etc.] occur only at high energies. Furthermore, for a given reaction, the recoil energy generally increases with the neutron energy. Therefore, high energy neutrons are more capable of producing lattice damage than low energy neutrons. Consequently, extrapolation of present experimental results on liquid helium temperature irradiation to superconducting magnets in a fusion reactor with relatively thin blanket/shield should be exercised with care. A design safety factor on the neutron dose-related radiation damage parameters in the magnet seems necessary at present.

c. Irradiation Effects in Niobium-Titanium Superconductor

Experimental studies on the effects of neutron irradiation on superconducting materials have been reported in Refs. 14-18. Discussion here will be limited to niobium-titanium. The most important property in the behavior of superconducting materials under irradiation is its critical current density, J_c . It was found that the change in J_c depends greatly on the metallurgical structure (hence, initial J_c) of the samples irradiated. In general, the fluxoids are pinned by irradiation defects at low neutron fluences causing an increase in the critical current density. This effect saturates at a dose of $\approx 5 \times 10^{17}$ n/cm² and further irradiation results in a decrease in the pinning and consequently the critical current. Depending on the position of the maximum pinning force⁽¹⁸⁾ (product of the critical current density and the magnetic induction) a decrease in J_c of a fraction of 1 to 10% was observed for

Figure D-11. Comparison of Neutron Spectrum in TEPR Magnet and Neutron Spectrum used in Radiation Damage Measurements. (A and B normalized to $P_w = 0.2 \text{ MW/m}^2$.)

Figure D-12. The Fraction, $f(E_0)$, of the Total Neutron Flux with Neutron Energies above E_0 , as a Function of E_0 . Curves A and B are for the Flux Outside a SS-B₄C Blanket/Shield of Thickness 60 and 100 cm, Respectively. Curve C is for the Flux in ANL Low Temperature Facility.

irradiation up to 3×10^{18} n/cm². Annealing experiments showed that a considerable recovery can be reached at room temperature.

Decreasing the allowable current density, J_c , involves an increase in the magnet capital cost to produce a specified magnetic field. However, a 10% decrease in J_c represents only a few percent increase in the magnet cost and can also be accommodated by decreasing the liquid helium temperature a fraction of one degree Kelvin.

The total neutron flux at the magnet, ϕ_m , is related to the total neutron flux at the first wall, ϕ_w , by the relation

$$\phi_m \sim \phi_w e^{-\mu x}, \quad (D-1)$$

where x is the blanket plus shield thickness, and μ is the total flux attenuation coefficient. If the maximum total fluence at the magnet is restricted to 3×10^{18} n/cm², then,

$$e^{-\mu x} < \frac{1.4 \times 10^{-4}}{P_w \cdot t}, \quad (D-2)$$

where P_w is the neutron wall loading in MW/m² and t (in years) is the irradiation period or the time span between magnet anneals multiplied by the duty cycle. The product of P_w and t is called the integral wall loading, I_w , and is used frequently here in units of MW-yr/m². Figure D-13 shows the total neutron flux at the magnet as a function of blanket plus shield thickness for 50% stainless steel + 50% B₄C composition and a neutron wall loading of 0.2 MW/m². From this figure and Eq. (D-2), it can be seen that a blanket/shield of 50% stainless steel + 50% B₄C should be greater than 65 cm for irradiation up to an integral wall loading of 1 MW-yr/m².

d. Irradiation Effects on Stabilizing Material

Stabilizing superconducting magnets requires forming the superconductor into a composite with a sufficient amount of high conductivity normal metal usually called stabilizing material. Copper and aluminum are the most likely candidates for stabilizing materials. Cryogenic stabilization criterion requires⁽¹⁹⁾ that the heat transfer from the stabilized superconducting matrix must be sufficient to transfer the I^2R heat generated in the stabilizing material when a flux jump occurs, i.e.,

Figure D-13. Total Neutron Flux as a Function of Blanket plus Shield Thickness for 50% SS + 50% B₄C Composition and a Neutron Wall Loading of 0.2 MW/m².

$$I^2 \rho < aPq, \quad (D-3)$$

where I is the operating current, ρ is the resistivity of the stabilizing material, a is the normal conductor cross sectional area, P is the cooled perimeter of the composite, and q is maximum allowable heat flux in W/cm^2 . This shows that an increase in the resistivity of the stabilizing material results in a decrease in the allowable operating current in order to keep the cryogenic stability criterion satisfied.

Neutron irradiation of high purity metals near liquid helium temperatures showed a significant increase in the electrical resistivity. The rate of increase in the radiation-induced resistivity, $d\rho_r/dt$, was found to vary almost linearly with the radiation-induced resistivity, ρ_r , i.e.,

$$\frac{d\rho_r}{dt} = \alpha(\rho_s - \rho_i), \quad (D-4)$$

$$\rho_r = \rho_s(1 - e^{-\alpha t}), \quad (D-5)$$

where t is the irradiation time, and ρ_s and α are experimentally determined parameters for each metal. Although some nonlinear defect production rates have been observed, the scatter in the experimental data does not justify⁽²⁰⁾ a better fit than that in Eq. (D-4). The value of ρ_s can be determined to within 10 to 50% accuracy⁽¹⁷⁾ and is $\sim 3 \times 10^{-7}$ and $8 \times 10^{-7} \Omega\text{-cm}$ for copper and aluminum, respectively.

The parameter α in Eq. (D-5) is a characteristic of the metal and depends on the magnitude and energy dependence of the neutron spectrum. To relate the radiation-induced resistivity in the stabilizing material to other parameters of the reactor, the product of αt should be expressed in a form which is fairly independent of the neutron spectrum. Since the increase in the resistivity is due to production of defects, the choice of number of displacements per atom (dpa) as a measure of irradiation dose is justified. The latest experimental data of Brown, et al⁽¹⁷⁾ was used to derive the following formula for radiation induced resistivity in copper

$$\rho_r = 3 \times 10^{-7}(1 - e^{-563d}) \Omega\text{-cm}, \quad (D-6)$$

where the saturation resistivity for copper, ρ_s , is equal to $3 \times 10^{-7} \Omega\text{-cm}$ and d is the number of atomic displacements. This formula agrees with other experimental data to $\pm 10\%$ for $10^{-8} < \rho_r < 10^{-7}$. Outside this range, a maximum deviation of 50% was observed. The displacement cross sections for copper used in deriving the above formula are those used in the previous section. Current methods for calculating displacement cross sections have limited accuracy. However, in calculating ρ_r in any given system the results will be most accurate if the same displacement cross sections employed in deriving the above formula are used. The value of the displacement energy has no effect except through normalization of the numerical factor (563) in the exponent of the exponential term of Eq. (D-6). The accuracy of the above expression, however, will be affected strongly by the accuracy of the energy dependence of the displacement cross sections when the neutron spectrum is markedly different from that used in the experimental measurements.

Experimental studies have shown that full recovery of the radiation-induced resistivity can be accomplished by heating aluminum to ~ 260 K and copper to ~ 400 K. Partial recovery occurs at lower temperatures. The amount of recovery at various temperatures depends to some extent on the irradiation neutron spectrum. For fast neutron irradiation, 31%, 61%, and 95% recovery of the radiation-induced resistivity in copper occurs when it is heated up to 50, 170, and 320 K, respectively.⁽¹⁶⁾ For aluminum, 40% and 60% recovery are obtainable at 52 and 200 K, respectively. Since the specific heat for these metals increases with temperature, substantial savings in the power requirements to cool down the magnet can be made by warming the magnet up only to temperatures just enough for ~ 85 -90% recovery.

The consequences of a superconducting magnet for a fusion reactor going normal have not yet been fully assessed. However, they are very likely to be grave because of the tremendous amount of stored energy and the relatively high levels of radioactivity in the reactor components. Thus, it is generally accepted that the superconducting coils for the TEPR must be cryogenically stable. In addition to ensuring the stability of the superconductor composite, provisions must be made to prevent damage to the coils under any condition of malfunction. For example, by connecting the coils in series with a dump resistor between every two neighboring coils the energy can be removed fast enough and without accumulating a large voltage with respect to the ground⁽²¹⁾ (see Chapter IV). The increase in the resistivity of the stabilizing material can

be accommodated without violating the cryogenic stability constraint of Eq. (D-3) by lowering the operating current, increasing the heat flux, q , and/or modifying the conductor design. Reduction of the operating current is undesirable and the heat flux is limited by the rate of heat transfer to liquid helium. Therefore, modifying the conductor design is usually the preferred choice. If we assume the conductor thickness is t , and the conductor width is W , then $a = tW$, and for one face cooling (or 50% both face cooling) $P = W$. Thus, the resistivity increase can best be accommodated by increasing W . However, fabrication difficulties place a limit on the conductor aspect ratio (W/t). It should be noted that due to neutron attenuation inside the magnet, the dpa, and hence ρ_r , will vary with position within the magnet. Changing the dimensions from one conductor to another radially in the magnet will increase the fabrication cost. On the other hand, since the modulus of elasticity for copper at 4.2 K is about half that of stainless steel, the amount of structure (typically stainless steel) can be reduced by about half the increase in copper.

Design of superconducting magnets involve many complicated trade-off and optimization considerations in addition to several stringent constraints^(22,23) (see Chapter IV). Therefore, it is difficult at present to set an upper feasible limit on the increase in the resistivity of stabilizing materials. Future studies are needed in this regard and also to correlate the increase in cost to changes in the amount of conductor for typical TEPR design parameters.

The maximum allowable increase in the resistivity of the stabilizer due to neutron irradiation affects the blanket plus shield thickness, Δ_{BS} , for a given power level and blanket/shield composition. The intrinsic plus magneto-resistivity of copper at 80 kG is $\approx 4.6 \times 10^{-8} \Omega\text{-cm}$. A radiation-induced resistivity of $3 \times 10^{-8} \Omega\text{-cm}$ represents a 65% increase in the resistivity of copper at 80 kG and would require a change of the conductor cooling surface width by <30% to keep the operating current the same. Such modification can practically be made without much difficulty. A ρ_r of 3×10^{-8} is 10% of the saturation resistivity for copper and is reached at 1.87×10^{-4} dpa. Table D-6 shows the minimum blanket plus shield thickness required to protect the magnet such that the maximum dpa at the inner winding does not exceed 1.87×10^{-4} for various integral wall loading. The calculations are based on blanket/shield composition of 50% stainless steel + 50% B₄C. The integral wall loading is the product of the neutron wall loading, operating time, and duty cycle. Most of the values for the operating time in the table represent practical choices for

Table D-6

Dependence of Blanket Plus Shield Thickness and Refrigerator-Power Requirements on Integral Wall Loading if the Maximum Allowable Radiation-Induced Resistivity in the Stabilizing Copper Conductor is $3 \times 10^{-8} \Omega\text{-cm}$

Case	Neutron Wall Loading (MW/m ²)	Span Between Magnet Anneals (years)	Duty Cycle (%)	Integral Wall Loading (MW-yr/m ²)	Blanket Plus Shield Thickness ^a (cm)	Refrigerator Power ^b (%)
1	0.1	2	20	0.04	59	113
2	0.1	2	50	0.10	67	33
3	0.1	2	80	0.16	71.5	18
4	0.1	5	80	0.40	77	9
5	0.1	20	80	1.60	87.5	1.5
6	0.2	2	20	0.08	65	3.3
7	0.2	2	50	0.20	72.5	16
8	0.2	2	80	0.32	75.5	11
9	0.2	5	80	0.8	81	6
10	0.2	20	80	3.2	92.5	1
11	0.5	2	20	0.20	72.5	16
12	0.5	2	50	0.50	78.5	7.5
13	0.5	2	80	0.80	81.0	6
14	0.5	5	80	2.0	89	1.4
15	0.5	20	80	8.0	97	0.6
16	1.0	5	80	4.0	93.5	1
17	1.0	20	80	16.0	107	0.15

- a. Based on optimized blanket and shield composition of stainless steel and boron carbide; the calculations allowed a void fraction (for helium cooling, thermal expansion, etc.) of 10 vol.% in the first 40 cm, and 5% in the rest of the shield.
- b. Refrigeration electric-power requirements expressed in percentage of plant electric-power output. Assumptions: 70% of the outer surface area covered by the magnet, plant thermal efficiency of 30%, 16.5 MeV are recoverable for each fusion reaction, and 500 W electric power required per watt of thermal-power input to the refrigerators.

time span between magnet anneals for full recovery. The duty cycle includes the plasma cycle and plant availability factors and is varied from 20, 50 to 80%. It is very unlikely that a duty cycle greater than 50% can be accomplished with an experimental power reactor. The neutron wall loading is varied from 0.1, 0.2, 0.5 to 1 MW/m². It was assumed in deriving the results in Table D-6 that variation of power with time has no effect on the radiation induced resistivity except through the accumulated dpa and, hence, the integral wall loading.

Table D-7I shows that if the maximum allowable ρ_r in copper is 3×10^{-8} Ω -cm, the blanket plus shield thickness for 50% stainless steel + 50% B₄C mixture varies from 59 to 107 cm as the integral wall loading is varied from 0.04 to 16 MW-yr/m². For a given blanket/shield composition and dimensions, the fraction of electric power required to run the refrigerators of the superconducting magnet can be calculated using the results of the previous section and a few reasonable assumptions. Table D-6 shows this fraction for each Δ_{BS} . The assumptions made in deriving this quantity were: (1) 70% of the outer surface area covered by the magnet; (2) plant thermal efficiency is 30%; (3) recoverable energy per fusion reaction is 16.5 MeV; and (4) 500 W of electric power are required per watt of thermal input to the refrigerators. These are typical values which can vary by as much as 50% and, of course, the refrigeration power requirements will vary accordingly. From the results in the table, it can be seen that the electric power required to run the refrigerators, f_r , varies from over 100% to less than 1% when Δ_{BS} is varied from 60 to 100 cm. Economics considerations⁽²⁾ limit f_r in commercial power reactors to less than 0.1%. However, the cost per unit power output in a TEPR device is not as important as the total capital cost. Cryogenic refrigerators currently cost ~ 0.5 million dollars per kW of the refrigeration capacity. Thus, for 150 MWt an f_r of 0.1 corresponds to a capital cost of the refrigerators of ~ 4 million dollars and operating cost of ~ 0.5 million dollars per year of continuous operation. Therefore, an f_r as high as 10% may be tolerated. Increasing f_r beyond 10% is costly, creates problems of power supply for the refrigerators, and affects the reliability of the magnet.

The results of Table D-6 show that decreasing the integral wall loading reduces Δ_{BS} required to limit ρ_r in the copper stabilizer to 3×10^{-8} Ω -cm. However, decreasing Δ_{BS} increases the refrigerators power requirement signifi-

cantly and in a few cases to intolerable levels. Therefore, for low integral wall loading, the refrigerator power requirements can be more limiting on the blanket/shield design than the increase in the resistivity of the stabilizer. For a given neutron wall loading, decreasing the duty cycle and the time span between magnet anneals reduces the cumulative damage in the magnet but does not affect the refrigerator power requirements.

The results in Table D-6 are based on a uniform blanket/shield surrounding the plasma. The refrigeration power requirements, however, can be reduced substantially by increasing the blanket/shield thickness in the outer regions where space restrictions are much less severe than in the inner, high magnetic field, region as discussed in detail in Section VI-B.

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APPENDIX E

SUPERCONDUCTING MAGNETS: PURE-TENSION MODEL AND COIL INTERACTION ANALYSIS

1. The Theory of Pure-Tension Toroidal-Field Coils*

The electromagnetic force acting on the circular toroidal coil is non-uniform with large bending moments exerted on the coil, thus producing the peak stresses. For a given coil structure, significant decreases in the amount of structural material can be realized by reducing the peak stresses. The minimum peak stress is obtained when the stress is uniform; namely, the coil is in pure tension with no bending moments.

Assuming that the toroidal field, B , in the winding is inversely proportional to toroidal radius, File et al.,⁽¹⁾ obtained a pure-tension shape for a smooth torus. Stress analysis shows that the coil they obtained is not in pure tension for a torus with a finite number of coils. Considerable bending moments exist in the Princeton "simple D" coil. Two assumptions they made are not strictly applicable to an actual toroidal field coil system. The field in the winding is not strictly inversely proportional to the toroidal radius, and the force per unit length of coil segment cannot be obtained by assuming the coil to be straight line filaments. A second attempt was made by File et al.⁽²⁾ to improve upon the first assumption. However, the equation presented in their paper diverges when one approaches the coil where the Lorentz forces are generated.

The pure-tension shape can be developed by considering a string acted on by forces distributed continuously along the length of the string.⁽³⁾ Let a point on the string be specified by its distance S from one end, measured along the string. Further, let $\vec{f}(S)$ be the force per unit length at the point S ; that is, the force on a small segment of length dS is $\vec{f}(S) dS$. The total force acting on the string between the end $S = 0$ and the point S will be zero if the string is in equilibrium:

$$\vec{F}_0 + \int_0^S \vec{f}(S) dS + \vec{\tau}(S) = 0 \quad (E-1)$$

* D. W. DeMichele, Texas A & M University, was the principal developer of the theory for the pure-tension toroidal-field coils and of the computer code, MARIA, which incorporates that theory.

where F_0 is the supporting force at the end $S = 0$, and $\tau(S)$ is the tension at the point S , oriented in the direction of increasing S as shown in Fig. E-1. Differentiating Eq. (E-1) with respect to S , we obtain the differential equation for the tension $\tau(S)$

$$\frac{d}{dS} [\tau(S) \cos(\phi)] = \sin(\phi) f(S) , \quad (E-2)$$

$$\frac{d}{dS} [\tau(S) \sin(\phi)] = -\cos(\phi) f(S) , \quad (E-3)$$

where

$$\vec{\tau}(S) = \tau(S) [\cos(\phi) \hat{i} + \sin(\phi) \hat{j}] , \quad (E-4)$$

and

$$\vec{f}(S) = f(S) [-\sin(\phi) \hat{i} + \cos(\phi) \hat{j}] . \quad (E-5)$$

Defining the radius of curvature⁽⁴⁾ as

$$\frac{1}{\rho} = \frac{d\phi}{dS} . \quad (E-6)$$

The only solution to Eq. (E-1) is

$$\frac{d\tau(S)}{dS} = 0 , \quad (E-7)$$

and

$$\tau(S) + \rho f(S) = 0 , \quad (E-8)$$

which implies, as stated as the basic equation of reference (1),

$$\rho f(S) = \text{constant} . \quad (E-9)$$

Figure E-1. A String in Equilibrium with No Bending Moments

a. Evaluation of Force on a Coil Segment

The shortcoming in the previous analysis⁽¹⁻²⁾ is in the evaluation of the force, $f(S)$, on the coil. If the toroidal coil winding is continuous, without gaps between adjacent coils, the field variations across the winding cross section would be uniform and the assumption that the field is inversely proportional to toroidal radius would be correct. The reciprocal field plot (vs. toroidal radius) would be a straight line passing through the origin. For a bumpy toroidal coil configuration, however, the field variations across the winding cross section is not uniform. It varies along the coil. The toroidal field within the winding is not inversely proportional to the toroidal radius. In fact, as shown in Fig. E-2, the reciprocal field plot yields an almost straight line but it does not pass through the origin. Furthermore, the slope of the line changes when the number of coils change.

Consider a segment of the coil as shown in Fig. E-3. To obtain the force per unit coil length, it is necessary to integrate over the coil volume because the fields vary over the volume of the coil segment. Let t be the variable for the coil radial thickness measuring from the coil centerline, ϕ be the azimuthal angle of the segment and u be the variable for the coil axial thickness measuring from the coil centerline. Then the field within the winding, B , can be written as

$$B = B(u, t, \phi) .$$

Then the force per unit coil length is given by

$$f(S) = \frac{\hat{J} \times \int_{\phi}^{\phi + \Delta\phi} \int_{-T_0}^{+T_0} \int_{-W}^{+W} (\rho_{\phi} + t) \vec{B}(u, t, \phi) du dt d\phi}{\int_{\phi}^{\phi + \Delta\phi} \rho_{\phi} d\phi} \quad (E-10)$$

where ρ_{ϕ} is the radius of curvature of the coil centerline, $\hat{J}$ is the current density and the dimensions W and T_0 are shown in Fig. E-3. This expression cannot be integrated analytically. We have two methods to solve the integral expression.

Figure E-2. Reciprocal Field Plots for Bumpy Toroids

Figure E-3. Coil Differential Segment

(1) Method of Approximation to the Integral

The following assumptions are made in order to approximate the integral

- (1) The field $B(u, t, \phi)$ varies linearly across the coil cross section from $-T_0$ to $+T_0$ at a given toroidal radius. Namely,

$$B(u, t, \phi) = B_\phi(u, \phi) \left(1 + \frac{\gamma t}{T_0}\right) . \quad (E-11)$$

where γ is related to the slope of the field across the coil. B_ϕ is the field at coil centerline. If the field at the outer edge of the coil is B_e , then

$$\gamma = \frac{B_e - B_\phi}{B_\phi} . \quad (E-12)$$

- (2) Assume the field, $B(u, t, \phi)$, varies linearly from ϕ to $\phi + \Delta\phi$.
 (3) Assume the field, $B(u, t, \phi)$, is constant from $-W$ to $+W$ with t and ϕ fixed.

Assumptions 2 and 3 coupled with Eq. (E-11) yields the following integral

$$f(S) = \frac{2W\mu_0}{\rho_\phi} \int B_\phi \left(\phi + \frac{\Delta\phi}{2}\right) \left(1 + \frac{\gamma t}{T_0}\right) (\rho_\phi + t) dt . \quad (E-13)$$

Completing the integration yields the following approximation to the force per unit length

$$f(S) \approx \frac{IB_\phi}{\rho_\phi} \left(\rho_\phi + \frac{\gamma(r)T_0}{3} \right) . \quad (E-14)$$

Both B_e and B_ϕ vary along the coil and so does the $\gamma(r)$. From Fig. E-2, we can approximate B_ϕ as

$$B_\phi \sim (\alpha_1 + \alpha_2 + \alpha_3 r^2)^{-1} . \quad (E-15)$$

We can also approximate $\gamma(r)$ as

$$\gamma(r) = (\lambda_1 + \lambda_2 r + \lambda_3 r^2)^{-1} . \quad (E-16)$$

Combining Eq. (E-14) with Eq. (E-9), we have

$$IB_{\phi}(r) \left[\rho_{\phi} + \frac{\gamma(r)T_0}{3} \right] = \text{constant.} \quad (\text{E-17})$$

Substitute Eqs. (E-15) and (E-16) and recall that

$$\rho_{\phi}^{-1} = \frac{\pm \frac{d^2 z}{dr^2}}{\left[1 + \left(\frac{dz}{dr} \right)^2 \right]^{3/2}}.$$

We have

$$\frac{\pm \frac{dp}{dr}}{(1 + p^2)^{3/2}} = (\eta_1 + \eta_2 r + \eta_3 r^2)^{-1}, \quad (\text{E-18})$$

where $p \equiv dz/dr$ and η_1 , η_2 and η_3 are constant coefficients for a given coil system. This equation can be integrated to yield

$$\frac{\pm p}{\sqrt{1 + p^2}} = \frac{1}{\sqrt{\eta_2^2 - 4\eta_3\eta_1}} \ln \left[\frac{2\eta_3 r + \eta_2 - \sqrt{\eta_2^2 - 4\eta_3\eta_1}}{2\eta_3 r + \eta_2 + \sqrt{\eta_2^2 - 4\eta_3\eta_1}} \right] + C,$$

where C is the constant of integration. Let

$$\frac{\ln \phi(r)}{\delta} \equiv \frac{1}{\sqrt{\eta_2^2 - 4\eta_3\eta_1}} \ln \left[\frac{2\eta_3 r + \eta_2 - \sqrt{\eta_2^2 - 4\eta_3\eta_1}}{2\eta_3 r + \eta_2 + \sqrt{\eta_2^2 - 4\eta_3\eta_1}} \right]. \quad (\text{E-19})$$

So we can now express dz/dr in terms of r

$$\frac{dz}{dr} = \frac{\pm [\ln \phi(r)/\delta + C]}{\sqrt{1 - [\ln \phi(r)/\delta + C]^2}}. \quad (\text{E-20})$$

There are two points where dz/dr becomes infinite, as shown in Fig. E-4. We shall assume the dimension r_1 is specified and either r_2 or r_0 is also specified by the coil design. The right hand side of Eq. (E-20) diverges

$$\ln [\phi(r)]/\delta + C = \pm 1 \quad . \quad (E-21)$$

Now, the signs are chosen to assure that

$$\frac{dz}{dr} \geq 0 \text{ at } r = r_1 \quad , \quad (E-22)$$

$$\frac{dz}{dr} < 0 \text{ at } r = r_2 \quad . \quad (E-23)$$

Further, we require that

$$\ln [\phi(r_1)]/\delta + C = -1 \quad , \quad (E-24)$$

$$\ln [\phi(r_0)]/\delta + C = 0 \quad , \quad (E-25)$$

$$\ln [\phi(r_2)]/\delta + C = 1 \quad . \quad (E-26)$$

These requirements yield the expression

$$\frac{dz}{dr} = \frac{-[\ln[\phi(r)/\phi(r_1)]/\delta - 1]}{\{1 - [\ln[\phi(r)/\phi(r_1)]/\delta - 1]^2\}^{1/2}} \quad . \quad (E-27)$$

If r_2 is specified, the constant of proportionality in Eq. (E-9) is selected to satisfy the expression

$$\ln [\phi(r_2)/\phi(r_1)]/\delta - 1 = +1 \quad , \quad (E-28)$$

and r_0 is solved for via the requirement that

$$\ln [\phi(r_0)/\phi(r_1)]/\delta - 1 = 0 \quad . \quad (E-29)$$

Figure E-4. Constant Tension Coil Shape and Coordinate Identification

If r_0 is specified, then the constant of proportionality in Eq. (E-9) is chosen to satisfy

$$\ln [\phi(r_0)/\phi(r_1)]/\delta - 1 = 0, \quad (\text{E-30})$$

and r_2 is determined by the requirement of Eq. (E-28).

The value of z can be found by numerically integrating the expression

$$z = \int_{r_2}^r \frac{[\ln(\phi(r)/\phi(r_1))/\delta - 1] dr}{\{1 - [\ln \phi(r)/\phi(r_1)]/\delta - 1\}^{1/2}} \quad (\text{E-31})$$

(2) Numerical Integration of the Forces

Assumptions 1 and 3 of Section 1-a-(1) may be eliminated by numerically integrating Eq. (E-10). A Gaussian integration is used. We shall break the integral in Eq. (E-10) into two parts.

$$\begin{aligned} f(S) = & J \int_{\phi}^{\phi + \Delta\phi} \int_{-T_0}^{+T_0} \int_{-W}^{+W} B(u, t, \phi) du dt d\phi \\ & + \frac{J}{\rho_{\phi}} \int_{\phi}^{\phi + \Delta\phi} \int_{-T_0}^{+T_0} \int_{-W}^{+W} B(u, t, \phi) t du dt d\phi. \end{aligned} \quad (\text{E-32})$$

Writing Eq. (E-32) in a more abbreviated form, we have

$$f(S) = H(r) + G(r)/\rho_{\phi}. \quad (\text{E-33})$$

Combining Eq. (E-9) with Eq. (E-33), we have

$$\rho_{\phi} H(r) + G(r) = \text{CONST.} \quad (\text{E-34})$$

Yielding the equation

$$\frac{\pm \frac{d^2 z}{dr^2}}{\left[1 + \left(\frac{dz}{dr}\right)^2\right]^{3/2}} = \frac{H(r)}{\text{CONST.} - G(r)}. \quad (\text{E-35})$$

again, we shall assume that

$$H(r) = (a_1 + a_2 r + a_3 r^2)^{-1} \quad (\text{E-36})$$

Further, we shall assume

$$G(r)/H(r) \approx \lambda_1 + \lambda_2 r + \lambda_3 r^2, \quad (E-37)$$

yielding an equation of the same form as Eq. (E-18). The coefficients α_1 , α_2 , α_3 , λ_1 , λ_2 and λ_3 are obtained through a least-squares fit of $H(r)$ and $G(r)/H(r)$ values generated by a numerical integration over the cross section of the coil. The accuracy of the fit can only be determined by comparing the numerical results with the values generated by the function. The numerical integration is accomplished via a three-point Gaussian integration from $-W$ to $+W$ and then a three-point Gaussian from $-T_0$ to $+T_0$.

$$H(r) = \sum_{L=1}^3 \left[\sum_{j=1}^3 \frac{I}{4} B(TX_L + \rho_\phi, WX_j, \phi) W_j \right] W_L, \quad (E-37)$$

$$G(r) = \sum_{L=1}^3 \left[\sum_{j=1}^3 \frac{I}{4} B(TX_L + \rho_\phi, WS_j, \phi) W_j \right] TX_L W_L, \quad (E-38)$$

where X_j are the Gaussian points, and W_j are the Gaussian weights.

b. Method of Generating the Coil Shape

The method of generating the coil shape is based upon an iterative process. The field generated by the Princeton "simple D" is used as a first approximation to the field generated by a pure-tension coil. The fitting parameters, α_1 , α_2 , α_3 , λ_1 , λ_2 and λ_3 are generated via a least squares fit of thirty points calculated along the coil from r_1 to r_2 . These coefficients are then used in Eq. (E-18) to generate a new shape. The field generated by the new shape is then used to calculate new fitting parameters and the process is repeated. Convergence is achieved in five trials. The difference in coil height using methods 1-a-(1) and 1-a-(2) is about 5 cm for the Argonne TEPR toroidal coil.

2. Vertical-Field Coil Interaction With the Toroidal-Field Coils*

a. Basic System

Some preliminary calculations of the interaction of the pulsed VF coils with the steady-state TF coils have been made for an earlier iteration

* John E. C. Williams, National Magnet Laboratory-MIT, contributed the material in this section.

of the TEPR design. Of the five vertical field (VF) coils, only one coil is considered in the estimation of the interaction. The toroidal-field (TF) coils are D-shaped and in order to reduce the calculation to simple form, the TF coils will be assumed square, as shown in Fig. E-5. The winding section of the TF coils will be assumed to be 0.349 m^2 which will give 6.98 MA at 2000 A cm^{-2} current density.

The effect of the fringe field from the vertical-field coils will be to produce a) a couple on the TF coil, and b) dissipation in both conductor and structure.

b. Forces

The couple on the conductor about the point of zero vertical field is:

$$M = \frac{\mu_0 I_1 I_2 \ell^2 d \ell}{2\pi (R^2 + \ell^2)}$$

where I_1 and I_2 are the VF- and TF-coil currents and the lengths are as shown in Fig. E-5. M is obtained by integration from $-L_1$ to L_2 . That is

$$M = \frac{\mu_0 I_1 I_2}{2\pi} \left\{ L - R \tan^{-1} \frac{L}{R} \right\}_{-L_1}^{L_2},$$

where L_1 and L_2 are the horizontal distances from the VF coil to the straight inner section of the TF coil and to the outer radius of the TF coil. For the case in point L/R is about 6 so that $\tan^{-1} L/R \approx \pi/2$, and the total couple is thus independent of R . Hence $M \approx 2 \times 10^{-7} I_1 I_2 [L_1 + L_2]$. If I is in amps, L_1, L_2 in meters, then M is in Newton-meters. In the present case $I_1 = 3.35 \text{ MA}$, $I_2 = 6.98 \text{ MA}$, $L_1 = 4.76 \text{ m}$, $L_2 = 3.5 \text{ m}$. So,

$$M = 38.4 \times 10^6 \text{ Newton-meter} = 28.3 \times 10^6 \text{ ft-pounds.}$$

This couple is balanced by one of equal magnitude but opposite in sign on the lower half of the TF coil.

Figure E-5. TF Coil Approximation

In the present simple model, these couples balance through reaction forces P and Q. These forces give rise to bending in the vertical arms of the idealized, square outlined TF coils of Fig. E-5, which in turn produce torsion in the horizontal member. $P = 3.39 \text{ MN}$, $Q = 2.50 \text{ MN}$.

It is assumed that the TF coil is housed in a square section tube of internal side s such that $s^2 = 0.349 \text{ m}^2$, or $s = 0.59 \text{ m}$. Unless the TF coils are reinforced by cross-bracing between neighboring coils, the wall thickness of this square section must withstand either the torsion in the horizontal member or the bending stress in the vertical member, whichever is greater.

The calculation of the maximum torsional or bending stress is complex, but for a simple approximation, it will be assumed that both the torque in the horizontal member and the bending moment in the vertical member are equal to $PH = 17 \times 10^6 \text{ Nm}$. Then the shear stress due to torsion is

$$\tau_{st} = \frac{T}{(w-s)(w+\frac{1}{2}s)^2}.$$

The bending stress is

$$\tau_{sb} = \frac{6Tw}{(w^4 - s^4)}.$$

If τ_{sb} and τ_{st} are to be limited to $4 \times 10^8 \text{ Nm}^{-2}$ (60,000 psi), then $w = 0.783 \text{ m}$ due to bending. If only torsion were a criterion, $w = 0.64 \text{ m}$.

c. Swept-Field Dissipation

While the TF-coil current is constant at 6.98 MA, the vertical-field coil is swept to 3.35 MA from 0 MA in 1 second. The cycle is repeated every 30 seconds. This generates eddy-current losses in the support structure of the coil and hysteresis losses in the wire.

The field at the TF coil due to the VF coil has orientations varying from parallel at the point closest to the VF coil, to perpendicular at points remote from the VF coil.

Only the perpendicular component generates significant hysteresis losses in the wire, but the modulus of the field alone is of most importance in generating eddy-current losses in the structure.

d. Power Dissipation in the Structure

Figure E-6 shows the general type of flux penetration into the TF coils containing structure. For simplicity of calculations, the losses due to parallel- and perpendicular-flux penetration will be calculated as if independent and then added. The error so introduced is not great for these two losses are not coincidentally large along the TF coil structure. The dimensions and designation appear in Fig. E-6

(1) Transverse-Field Penetration

This loss is given for a field rise from 0 to B_m in τ seconds by:

$$Q_T = \frac{2\pi}{6} \cdot \frac{\dot{B}_m^2 \tau w^3 t R}{\rho} \quad J,$$

where t is the wall thickness ($\ll w$)

$$\tan^{-1} \left(\frac{L_1}{R} \right) \sim \tan^{-1} \left(\frac{L_2}{R} \right) \sim \frac{\pi}{2},$$

and B_m is the field on the TF coil closest to the VF coil,

$$B_m = \mu_0 \frac{T_{VF}}{2\pi R}.$$

where T_{VF} is the torque on the vertical-field coil.

(2) Parallel-Field Penetration

This loss is given for the same condition as above by:

$$Q_p = \frac{\pi}{8} \cdot \frac{\dot{B}_m^2 \tau w^3 t R}{\rho}.$$

Adding these two losses

$$Q = \left(\frac{2\pi}{6} + \frac{\pi}{8} \right) \cdot \frac{\dot{B}_m^2 \tau w^3 t R}{\rho}.$$

For a TF structure shown in Fig. E-5, this loss is

$$Q = 0.25 \text{ MJ}.$$

Figure E-6. General Flux Penetration Into TF Coil

This is the loss during 1/4 cycle in one of two regions of one of 16 TF coils. So the total loss is 32 MJ. If this is repeated every 30 seconds, the power dissipation would be 1 MW at 4.2 K.

It should be noted that the expression for the total loss scales down to a tube that might contain a superconducting cable cooled by helium circulation. If 3600 tubes of 1 cm side and 1 mm wall were used, the dissipation would be 160 watts.

(3) Shielding Loss

The expression for this loss is obtained by integrating the basic shielding loss expression from $-L_1/R$ to $+L_2/R$. If these ratios are large so that $\tan^{-1} L/R \approx \pi/2$ then,

$$Q_s = \frac{\pi}{24} \frac{B_m^2 \ell_p^2 d_w^2}{\rho_m \tau} R ,$$

where

ℓ_p, d_w, ρ_m are the usual wire parameters,

τ is the rise time from 0 to B_m ,

B_m is the maximum field at the TF coil due to the peak current in the VF coil.

This is the loss for one wire. There will be an array of wires within the cross section of the TF coil. The total loss will then be given approximately by

$$\frac{\pi}{6} \frac{B_m^2 \ell_p^2}{\tau \rho_m} \lambda s^2 R \quad J/\text{cycle},$$

where

s is the internal dimension of the TF-coil structure, and

λ is the filling factor of the TF coil averaged over 30 seconds. This gives

$$w_s \approx 75 \text{ W}$$

$$\text{if } \rho_m = 15 \times 10^{-8} \Omega\text{m and } \ell_p = 10^{-2} \text{ m.}$$

Thus a tightly-twisted composite with a partly approximated matrix has been assumed. If the matrix is all copper, then

$$w_s \approx 11.2 \text{ KW} .$$

e. Shielding

Without shielding, a TF conductor containing fine filaments and a cupro-nickel matrix, will be needed if dissipation is to be kept below a few kilowatts.

If a simpler conductor with a copper matrix and filaments of up to 100 μ -diameter is used, it must be shielded against field fluctuations from the VF coil. Such a shield can be either a superconducting coil, passive or active, or a passive eddy-current shield.

(1) Eddy-Current Shield

This has the advantages of simplicity and an exact current distribution. The attenuation is however limited.

Consider as a typical shield, a copper or aluminum tube of square cross section, which is also the room-temperature wall of the TF-coil cryostat. Figure E-7 shows the cross section of such a shield.

The parallel component of flux will enter the shield with a time constant

$$T = \frac{\mu_o w b}{4\rho} .$$

If $w = 0.7 \text{ m}$ and $b = 0.05 \text{ m}$ and $\rho = 1.8 \times 10^{-8} \Omega\text{m}$, then $T = 0.61 \text{ sec}$. The field inside the shield would thus lag somewhat the external field but the maximum value of the internal flux will eventually equal the external level, unless the time constant is much greater than the cycle time of ~ 30 seconds.

The passive eddy-current shield of reasonable size slightly decreases the structural and shielding-current dissipation but has negligible effect on filament loss.

Figure E-7. Schematics of Field Shielding for TF Coils

(2) Superconducting-Shield Coils

Superconducting coils can be used to shield out a large fraction of the VF field incident on the TF coils. These shield coils must, of course, be wound from a low-loss wire having fine filaments and a cupro-nickel/copper matrix. The positioning of the coils is shown in Fig. E-7.

f. Conclusion

Although only approximate expressions for hysteresis and eddy-current losses have been used, it has been shown that significant dissipation will occur in an unshielded TF-coil structure by the pulsed VF coils.

A TF-coil structure, strong enough to be self-supporting to the bending and torsional forces resulting from the VF field, would be massive and have unacceptable eddy-current losses. Furthermore, if cross-bracing between TF coils were used to reduce the weight of the structure, the braces would have some composite or other nonmonolithic form to reduce eddy-current losses to an acceptable level.

Because the TF coils will be subjected to pulsed fields from the VF coils, whatever the OH system used, it will be necessary to examine the use of composite superconductors capable of operating under incident pulsed fields.

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APPENDIX F

MATERIALS RESEARCH AND DEVELOPMENT REQUIRED FOR THE TEPR

The materials selection for Stage I of the TEPR is based, as much as possible, on available materials technology, with a minimum extrapolation of existing data and a minimum of additional materials development. The materials problems for Stage I of the TEPR are further reduced by the omission of a tritium-breeding blanket. However, considerable materials research and development still will be required to verify critical materials design decisions for the TEPR. Moreover, the advanced blanket concepts to be tested in Stage II require a substantial materials development program. The materials considerations and candidate materials for the primary-energy conversion system have been discussed in Chapter VI and in Appendix C. The recommended materials research and development for Stage I and Stage II TEPR is categorized below in terms of surface effects, bulk radiation effects, materials development, and materials engineering. Other required materials research and development is identified in Appendices G (Superconducting Magnets), H (Blanket and Shield) and I (Tritium Control).

1. Stage I Materials Research and Development

(a) Surface Effects

The surface effects, which relate primarily to the plasma interactions with the first wall, include (1) sputtering caused by energetic ions, neutrons, electrons, and photons incident on the first wall, (2) blistering, spalling, and gas release caused by injection of energetic atoms (primarily helium and hydrogen isotopes) into the first wall, (3) chemical interactions involving primarily the hydrogenous plasma ions with the first-wall materials, and (4) vaporization, outgassing, and desorption of impurities from the first wall.

- (1) Additional sputtering data are required, particularly for the potential low-Z materials coatings or liners, e.g., graphite, beryllium, and silicon carbide coatings on stainless steel. Important aspects of sputtering investigations on low-Z coatings are the type of coating, method of coating application, treatment of metal surface prior to coating, and post-coating heat-treatment.

For maximum productivity, it is essential that the sputtering program on potential liner materials be closely coordinated with other aspects of liner development; primarily fabrication technology and materials compatibility, since these factors have important implications in the selection of a liner.

(2) The surface erosion produced by gas implantation in the surface region of candidate low-Z coatings on stainless steel should be investigated for wall temperatures up to 600°C. This effort should be coordinated with the effort on the fabrication technology of the low-Z coatings.

(3) The thermodynamics and kinetics of potential chemical interactions between the first-wall materials and plasma (hydrogenous) environment should be investigated for selected materials systems. Included in this is the chemical sputtering of various species.

(4) The outgassing characteristics of porous nonmetallic coatings should be investigated.

(b) Bulk Radiation Effects

(1) Additional data are required on the radiation swelling characteristics of the candidate coated composite materials. The effect of any volume change on the integrity of the coating-stainless steel bond should be investigated. This work should include the bulk radiation effects on any phases formed at the metal-coating interface, e.g., metal carbides.

(2) The effect of gaseous transmutation products on the mechanical integrity of candidate coated composite materials should be investigated.

(3) Bulk radiation damage of proposed blanket materials, viz., tungsten and B_4C should be investigated in more detail.

(c) Materials Development

Considerable effort will be necessary to provide the data base required to confidently assess the materials limitations for a total reactor system. Much of this work is related to the chemical, thermal, and mechanical effects on materials properties and to the synergistic effects of these and the radiation effects.

(1) Possible chemical interactions between the stainless steel vacuum-wall and liner material must be assessed and the resultant effects on the integrity of both should be investigated for the appropriate temperature ranges.

(2) The fatigue behavior of stainless steel with candidate coatings should be investigated for the temperature range of interest, since both the radiation and chemical, e.g. carburizing, environment can significantly alter the mechanical properties of stainless steel. Thermal fatigue in the vacuum wall may be particularly critical because of the cyclic nature of the plasma burn in the TEPR and because of the relatively large thermal-gradient stresses present in the first wall.

(3) A detailed analysis of the compatibility problems in both the coolant and plasma systems should be conducted. The major concern is the radioactive mass transfer from the first wall to the primary vacuum system and from the blanket/shield to the unshielded portions of the coolant system.

(d) Materials Engineering

Considerable effort relating to the fabrication of selected materials will be required.

(1) Additional work on the application of potential low-Z coatings on stainless steel will be required. This effort will include method of application, surface treatment before application, and heat treatment after the coating is applied. Investigations on the integrity of candidate coating materials should be coordinated with the surface effect studies.

(2) Fabrication problems relating to the non-stainless steel portions of the blanket/shield system should be examined further, particularly with respect to joining and coolant containment.

2. Stage II Materials Research and Development

(a) Surface Effects

Additional sputtering and spalling (blistering) data should be obtained for potential low-Z coatings that are compatible with other candidate vacuum-wall materials, probably selected vanadium-base alloys. The potential chemical interactions and outgassing characteristics of these alternate

first-wall systems should be examined.

(b) Bulk Radiation Effects

Bulk radiation damage, vis., embrittlement and swelling, should be examined at appropriate radiation levels for selected vanadium-base alloys and candidate structural materials for the solid breeding-blanket concept. The bulk radiation-damage studies should include potential solid breeding materials, e.g. lithium aluminate. The effect of gaseous transmutation products is of particular concern.

Table F-1. Five-Year Plan for TEPR First Wall and
Blanket Structural Materials R & D

RESEARCH & DEVELOPMENT ITEMS	FY-76	FY-77	FY-78	FY-79	FY-80	Total
	(Dollars in Thousands)					
I. <u>Surface Radiation Effects</u>	\$1325	\$1875	\$2575	\$2875	\$2975	\$11625
A. Sputtering						
Scientific Manpower	475	675	750	750	575	
Equipment & Facilities	200	250	300	300	250	
B. Blistering and spalling						
Scientific Manpower	175	225	300	375	350	
Equipment & Facilities	150	200	200	200	150	
C. Particle Emission from Photons						
Scientific Manpower	25	50	75	100	100	
Equipment & Facilities	-	-	-	-	-	
D. Synergistic Radiation Effects						
Scientific Manpower	200	325	650	750	1100	
Equipment & Facilities	100	150	300	400	450	
II. <u>Bulk Irradiation Effects</u>	\$ 600	\$1050	\$1450	\$2150	\$2200	\$7450
A. Displacement Damage						
Scientific Manpower	150	300	400	600	700	
Equipment & Facilities	100	150	200	300	250	
B. Transmutation Effects						
Scientific Manpower	150	300	400	600	700	
Equipment & Facilities	100	150	200	300	250	
C. Property Effects						
Scientific Manpower	50	100	150	200	250	
Equipment & Facilities	50	50	100	150	50	

Table F-1 Contd.	FY-76	FY-77	FY-78	FY-79	FY-80	Total
	(Dollars in Thousands)					
<u>III. Compatibility</u>	\$ 250	\$ 475	\$ 625	\$ 750	\$ 800	\$2,900
A. Chemical Interactions						
Scientific Manpower	50	100	150	200	200	
Equipment & Facilities	0	0	50	100	100	
B. Physical Interactions						
Scientific Manpower	50	100	150	200	300	
Equipment & Facilities	50	100	100	100	50	
C. Outgassing & Desorption						
Scientific Manpower	50	100	100	100	100	
Equipment & Facilities	50	75	75	50	50	
<u>IV. Mechanical Properties of Materials</u>	\$ 400	\$ 800	\$1350	\$1850	\$2000	\$6,400
A. Elevated Temperature Effects						
Scientific Manpower	100	200	400	500	700	
Equipment & Facilities	50	100	200	200	200	
B. Irradiation Effects						
Scientific Manpower	50	100	200	400	500	
Equipment & Facilities	50	100	100	200	100	
C. Effects of Nonmetallic-Element Reactions & Redistribution						
Scientific Manpower	100	200	300	400	400	
Equipment & Facilities	50	100	150	150	100	
<u>V. Fabrication</u>	\$ 300	\$ 550	\$ 700	\$1000	\$1050	\$3,600
A. Low-Z Coatings on First Wall						
Scientific Manpower	100	200	200	300	300	
Equipment & Facilities	50	100	100	150	50	
B. Secondary Fabrication of Selected V Alloys						
Scientific Manpower	100	200	300	400	500	
Equipment & Facilities	50	50	100	150	200	
<u>VI. Materials Performance</u>	\$170	\$290	\$470	\$650	\$650	\$2,230
A. Modeling Material Property Effects						
Scientific Manpower	50	100	150	200	200	
Equipment & Facilities	10	20	30	40	40	
B. Translate Combined Property Changes into Performance Characteristics						
Scientific Manpower	50	100	200	300	300	
Equipment & Facilities	10	20	40	60	60	
C. Econ. Considerations, Materials Avail.						
Scientific Manpower	50	50	50	50	50	
Equipment & Facilities	-	-	-	-	-	
<u>TOTAL MATERIALS R & D</u>	\$3045	\$5040	\$7170	\$9275	\$9675	\$34,205

APPENDIX G

SUPERCONDUCTING MAGNET SYSTEMS RESEARCH AND DEVELOPMENT REQUIRED FOR THE TEPR

As the designs study summarized in Chapter IV progressed, we were able to define many of the elements of the extensive research and development program that will be required to support the engineering effort on TEPR superconducting magnet systems.

Table G-1 contains an itemized estimate covering known areas of major research and development required for the magnet systems. This breakdown was developed as a result of the design effort done to date; an allowance is included for additional undefined tasks, not known at present, but which can reasonably be expected to come to light during our further design effort. A five-year funding profile is presented for the known research and development requirements; this is believed to be reasonable on the basis of past experience with magnet technology development.

The estimates in Table G-1 do not include a contingency factor and are based upon the following additional assumptions: (1) the cost of one man-year of scientific effort is \$50 K; and (2) most of the facilities required for this development program already exist in U. S. laboratories. Descriptions of program content and direction for the major research and development items in Table G-1 are summarized below:

1. Toroidal-Field Coil System

a. Magnet Stability Study and Testing Program

In the TEPR, there is sufficient winding space to build fully stabilized coils. However, the stability of even fully stabilized coils is not completely understood at this time. Cooling along the conductor and between the adjacent turns has never been seriously considered in the analysis of stability. Data on normal front propagation velocity versus the conductor current density level, the magnet field, and the associated cooling provisions are generally lacking or unreliable. An analytical study and a good experimental testing program on conductor stability and coil stability must be done before the multi-million-dollar TF-coil system is built.

b. Pulsed-Field Shielding

No matter how one designs them, the vertical field and poloidal field will interact with the TF coil. They will produce a large ac loss dissipation in the TF coil system. A technique using normal metal shielding or superconducting shielding must be developed to provide the basis for a credible shielding design.

c. Conductor Development Program

If complete shielding cannot be achieved, the matrix loss and the filament loss of fully stabilized conductors of conventional design are significant. To reduce these losses, the conductor may have to be made of a cable consisting of transposed multiple strands. Such a construction would require a development effort. Aluminum-stabilized conductors must also be evaluated, as it may be worthwhile to develop such a conductor.

d. Coil-Forming Modeling

Using copper as the dummy conductor, the coil-forming technique and winding facility must be developed in preparation for coil fabrication.

e. Radiation-Damage Testing

Data on radiation-damage effects to the superconductor, stabilizer, insulation material, and structural material at 4°K must be available before a TEPR magnet system is designed and built.

2. Ohmic-Heating Coils and Equilibrium Coils

A TEPR will have a better chance to produce net electrical power if these coils are superconducting. Development of both coil systems are very similar. These coils store a large amount of energy and must be cycled rapidly. This will require extending the present state-of-the-art of pulsed-superconducting magnet technology by a considerable amount. This should be started as early as possible. Modeling will be a useful approach. Large and fast charge and discharge magnets, having stored energies of the order of 100 MJ, need to be built to study the problems associated with large ac superconducting magnets as soon as possible. A parallel development program on the energy storage and power supply systems for these magnets must be started as soon as possible. The requirements upon the power supply and upon the pulsed superconducting coils are so interrelated that the development program for the two must be closely integrated.

a. AC Losses of Superconductor

Analytical calculations and experimental measurements of ac losses in superconductors must be started as soon as possible to gain the knowledge and collect the data required for ac magnet design.

b. AC Conductor Development

Industry must be involved in this effort.

c. High Current AC Cabling and Testing

Industry must be involved in this effort. It requires cable-making facilities and an experimental program to test ac cable stability and energy dissipation.

d. 100 MJ AC Solenoid Modeling

This is a parallel experimental program to understand fully the behavior of superconductors in a rapidly-pulsed mode and to allay any doubts about superconducting ohmic-heating and equilibrium coils.

e. Power Supplies and Energy Storage Development Program

A concentrated program, involving industry, to develop a cheap and reliable power supply and energy storage system for the TEPR ohmic-heating system is essential. Superconducting homopolar generators are one possibility.

3. Structural Material Development

There is a general lack of reliable cryogenic data on some of the materials used in superconducting magnets. In particular, the measurements of mechanical properties, such as stress and strain under cyclic loading, must be done. Data on thermal, electrical and mechanical properties of all structural materials must be available before we can design and build a TEPR magnet system.

Table G-1. Five-Year Plan for TEPR Superconducting Magnet Technology

RESEARCH AND DEVELOPMENT ITEMS	FY-76	FY-77	FY-78	FY-79	FY-80	Total
	(Dollars in Thousands)					
I. <u>Toroidal-Field Coil System</u>	\$ 600	\$ 950	\$1,100	\$1,300	\$1,100	\$5,050
A. Magnet Stability Study & Testing (Laboratory & University)						
Scientific Manpower	200	200	200	100	50	
Equipment & Facilities	100	100	100	100	50	
B. Pulsed-Field Shielding (Laboratory & University)						
Scientific Manpower	100	100	200	200	100	
Equipment & Facilities	100	100	100	100	100	
C. Conductor Development Pro- gram (Laboratory & Industry)						
Scientific Manpower	-	100	100	200	100	
Equipment & Facilities	-	100	100	200	200	
D. Coil-Forming Modeling (Laboratory & Industry)						
Scientific Manpower	-	-	-	50	100	
Equipment & Facilities	-	-	-	50	100	
E. Radiation Damage Testing (Laboratory)						
Scientific Manpower	100	150	200	200	200	
Equipment & Facilities	-	100	100	100	100	
II. <u>Ohmic-Heating Coils</u> <u>& Equilibrium Coils</u>	\$ 540	\$2,570	\$3,300	\$1,720	\$ 520	\$8,650
A. Superconductor AC Loss Study (Laboratory & University)						
Scientific Manpower	200	200	400	200	100	
Equipment & Facilities	40	20	100	20	20	
B. AC Conductor Development (Laboratory & Industry)						
Scientific Manpower	50	400	400	200	50	
Equipment & Facilities	-	200	200	100	-	
C. High-Current AC Cabling & Testing (Laboratory, Industry & University)						
Scientific Manpower	50	100	200	200	100	
Equipment & Facilities	-	50	200	200	50	

Table G-1. Five-Year Plan for TEPR Superconducting Magnet Technology (Contd.)

RESEARCH AND DEVELOPMENT ITEMS	FY-76	FY-77	FY-78	FY-79	FY-80	Total
	(Dollars in Thousands)					
D. 100 MJ AC Magnet Model (Laboratory)						
Scientific Manpower	-	100	400	100	-	
Equipment & Facilities	-	500	500	100	-	
E. Power Supplies & Energy Storage (Laboratory & Industry)						
Scientific Manpower	200	500	400	200	100	
Equipment & Facilities	-	500	500	400	100	
III. <u>Structural-Material Development</u>	\$ 200	\$ 300	\$ 300	\$ 300	\$ 200	\$ 1300
(Laboratory & University)						
Scientific Manpower	150	250	200	250	150	
Equipment & Facilities	50	50	100	50	50	
Subtotals	\$1340	\$3820	\$4700	\$3320	\$1870	\$15000
IV. <u>Additional Tasks Not Yet Defined</u>						
	(estimated at ~50% of the above)					7500
Total	\$2010	\$5730	\$7050	\$4980	\$2730	\$22500

APPENDIX H

BLANKET AND SHIELD TECHNOLOGY RESEARCH AND DEVELOPMENT REQUIRED FOR THE TEPR

During the course of the studies described in Chapter VI, it has become abundantly clear that a substantial, properly-focused, technology-development program will be required to support the design of a credible primary energy-conversion system for the TEPR. Research and development (R and D) areas that apply to the first-wall system are largely oriented in the direction of radiation effects and are treated in Appendix F. This appendix summarizes the early stages of a program that could fulfill the R and D requirements for the TEPR blanket/shield system. Table H-1 contains an itemized breakdown of major R and D areas for the blanket/shield system, together with a five-year funding profile that is considered to be reasonable, based on insights gained from comparisons with comparable ongoing programs in support of fission-reactor development. The guiding assumptions applied in forming Table H-1 were that (1) \$50 K is equivalent to one scientific man year, (2) very little effort is directed toward parallel development of two or more approaches to the same R and D item (unless so specified), (3) major facilities service three or more related R and D items (the equipment and facilities costs have been prorated accordingly), and (4) programs associated with each R and D item will still be escalating as they enter the 1980's. This program plan represents current thinking based on preliminary TEPR design scoping studies; hence, there may be important R and D items which have been omitted or underestimated. As the TEPR design study progresses over the coming years, a continuing effort will be made to expand and upgrade the program plan for the blanket/shield in keeping with the status of the design itself. Descriptions of program content and direction for the major R and D items in Table H-1 are summarized below:

1. Neutronics

A broad nuclear data base for materials that are identified as being applicable to the construction of a TEPR must be established. The methodology and computational tools needed to perform accurate neutronic-photonic analyses for the actual reactor geometry must be developed. Nuclear data and model sensitivity analyses must be performed to assess the uncertainty margin that

should be employed in the nuclear design of the TEPR. The nuclear data and computational methods used in the TEPR design must be verified by benchmark and mockup integral experiments.

2. Coolant Technology

Pressurized water and pressurized helium appear to be suitable primary coolants for Stage I operation of a TEPR, while liquid lithium is a prime candidate for the Stage II modular inserts. Important early needs include development of computational methods and experimental assemblies to quantify the thermal fluid behavior of each coolant (e.g. heat transfer coefficients, effective thermal conductivities). Of special importance to Stage II operation is experimental confirmation that circulating liquid lithium in tokamak-type magnetic field configurations will not compromise either plasma stability (by perturbing the magnetic field) or the coolant performance of the lithium. It will be essential to establish an understanding of the interactions between each candidate coolant and likely containment materials. Included in the compatibility program are (1) effects due to impurities, temperature gradients, thermal and mechanical stresses, short-cycle fatigue phenomena, and magnetic fields; (2) development of the required impurity monitoring and remote-sensing techniques and instrumentation, and (3) establishment of an extensive physical-, chemical-, and mechanical-property data base. Ultimately the coolant technology program will require a number of major facilities to investigate promising coolant geometries, test coolant compatibility in realistic environments, support the development of key components, and verify overall coolant-system performance.

3. Mechanics

Results of the study described in this report show that unique fabrication and remote maintenance procedures are very likely to be essential to the sustained operation of a TEPR. A program should begin immediately (and escalate rapidly) to develop and test methodology for remote assembly and disassembly of the PECS and other major reactor-support systems (e.g. vacuum pumps, neutral beams). The impact of fabrication and maintenance requirements on the reactor and overall plant designs should be established at the earliest possible time to permit reliable design descriptions for all major components. Computational methods (including code development) and other categories of

engineering support (e.g. mechanical-property testing, major system layout, component assembly) should be constantly upgraded to guarantee a firm technology base in advance of the final stages of the TEPR design.

4. Shield Technology

Aspects of the shield design (other than its radiation-attenuation properties) that relate to thermal energy removal, performance monitoring, assembly, processing, interfacing with penetrations, etc. must come under study. Verification of shield design methods for the very complicated geometry of the TEPR will require a variety of special radiation shielding experiments.

5. Instrumentation and Control

The scope of the instrumentation and control requirements for the primary energy-conversion system remains to be investigated in detail. Programs to develop remote-sensing systems to monitor neutron and gamma fluxes, temperature, stress, fluid flow, vibration, mechanical and electrical performance, and impurities are clearly needed. In addition, the integrated operation of all subsystems of the PECS will undoubtedly necessitate the use of a computer to accept, process, and respond to signals from the various sensing locations. Development of reliable instrumentation and circuitry with adequate redundancy and identification and verification of appropriate response technology are also needed.

TABLE H-1. Five Year Plan for TEPR Blanket and Shield Technology

RESEARCH & DEVELOPMENT ITEMS		FY-76	FY-77	FY-78	FY-79	FY-80	Total
		(Dollars in Thousands)					
I.	<u>Neutronics</u>	\$ 450	\$ 750	\$1350	\$1900	\$2700	\$7150
	A. Nuclear Data						
	Scientific Manpower	100	200	500	600	800	
	Equipment & Facilities	-	50	100	100	100	
	B. Method & Code Development						
	Scientific Manpower	150	200	300	400	500	
	Equipment & Facilities	-	-	-	-	-	
	C. Sensitivity & Analysis						
	Scientific Manpower	150	200	200	300	300	
	Equipment & Facilities	-	-	-	-	-	
	D. Integral Experiments						
	Scientific Manpower	50	100	200	400	800	
	Equipment & Facilities	-	-	50	100	200	
II.	<u>Coolant Technology (Li)</u>	\$ 500	\$1200	\$1950	\$2750	\$3800	\$10,200*
	A. Thermal Fluid Analysis						
	Scientific Manpower	50	200	300	400	500	
	Equipment & Facilities	-	-	50	150	200	
	B. Purification & Conditioning						
	Scientific Manpower	50	150	200	300	400	
	Equipment & Facilities	-	50	100	200	200	
	C. Materials Compatibility						
	Scientific Manpower	150	250	250	300	550	
	Equipment & Facilities	-	50	50	50	100	
	D. Code Development & Analysis						
	Scientific Manpower	100	150	200	300	300	
	Equipment & Facilities	-	-	-	-	-	
	E. Heat Transfer						
	Scientific Manpower	50	100	200	350	500	
	Equipment & Facilities	-	-	100	150	200	
	F. Component Development (Pumps, Heat Exchangers)						
	Scientific Manpower	-	100	250	250	400	
	Equipment & Facilities	-	-	50	100	200	
	G. Physical & Chemical Property Data						
	Scientific Manpower	100	150	200	200	250	
	Equipment & Facilities	-	-	-	-	-	
III.	<u>Mechanics</u>	\$ 150	\$ 350	\$ 950	\$2150	\$3050	\$6650
	A. Mechanical Property Data						
	Scientific Manpower	-	100	200	300	300	
	Equipment & Facilities	-	50	100	100	50	

* For Lithium development (liquid metal or molten salt). For helium, cost would be ~20% of this figure.

TABLE H-1. Five Year Plan for TEPR Blanket and Shield Technology (cont'd)

RESEARCH & DEVELOPMENT ITEMS	FY-76	FY-77	FY-78	FY-79	FY-80	Total
	(Dollars in Thousands)					
<u>III. Mechanical (cont'd)</u>						
B. Fabrication Technology						
Scientific Manpower	-	-	150	400	700	
Equipment & Facilities	-	-	-	200	400	
C. Maintenance Technology						
Scientific Manpower	50	50	150	300	400	
Equipment & Facilities	-	-	100	300	500	
D. Layout & Assembly						
Scientific Manpower	-	-	100	250	400	
Equipment & Facilities	-	-	-	100	100	
E. Structural Analysis Code Development						
Scientific Manpower	100	150	150	200	200	
Equipment & Facilities	-	-	-	-	-	
IV. <u>Shield Technology</u>	\$ 100	\$ 200	\$ 500	\$ 800	\$1150	\$2750
A. Physical & Chemical Property Data						
Scientific Manpower	50	100	150	150	100	
Equipment & Facilities	-	-	-	-	-	
B. Processing & Conditioning						
Scientific Manpower	50	100	200	200	250	
Equipment & Facilities	-	-	50	50	100	
C. Simulation Experiments						
Scientific Manpower	-	-	100	300	400	
Equipment & Facilities	-	-	-	100	300	
V. <u>Instrumentation & Control</u>	\$ -	\$ 100	\$ 400	\$ 500	\$ 700	\$1700
A. Blanket						
Scientific Manpower	-	50	150	200	250	
Equipment & Facilities	-	-	50	50	100	
B. Coolant						
Scientific Manpower	-	50	150	200	250	
Equipment & Facilities	-	-	50	50	100	
Totals	\$1200	\$2600	\$5150	\$8100	\$11,400	\$28,450*

* With lithium coolant. The total with helium coolant would be \$20,300.

APPENDIX I

TRITIUM CONTAINMENT AND CONTROL TECHNOLOGY RESEARCH AND DEVELOPMENT REQUIRED FOR THE TEPR

Chapter VII of this report contains a preliminary design description for the TEPR tritium-handling facility. It is expected that this conceptual-design activity will continue through FY-1977 and will culminate in the achievement of a detailed conceptual design for a fully-integrated network of systems to contain and control tritium in a TEPR. However, it is very likely that even after two to three years of rigorous parameter analysis and design study there will still remain a significant level of uncertainty concerning the actual operating characteristics of many of the tritium-handling systems and components. In order to minimize this uncertainty in the coming years and to insure that the final TEPR design description has a sound technological basis, it is essential that an intensive technology-development program, amply funded and properly focused, be initiated immediately in parallel with the conceptual-design effort.

Items related to the TEPR tritium-handling facility that are in need of significant research and development (R and D), are indicated throughout Chapter VII, and major R and D items that are vital even to the conceptual-design studies are highlighted in Section VII-C. Some consideration has been given to a balanced, and properly-escalated program plan for tritium-handling technology to be executed over the next five years. This program plan, which is directed primarily at R and D in support of the TEPR, is given in Table I-1. In reviewing Table I-1, several factors should be borne in mind: (1) the program in Table I-1 is believed to represent the absolute minimum effort that could achieve the desired goals; (2) no consideration is given to parallel development of two or more alternative approaches for a given R and D item unless so specified; (3) the costs for major equipment and facilities are prorated over each item and it is assumed that most major facilities will service three or more related R and D items; (4) \$50 K was assumed to be equivalent to one scientific man-year; (5) no provisions were made for contingencies or inflation; and (6) because the plan in Table I-1 is based largely on the preliminary TEPR design activity carried out during FY-1975, there are undoubtedly a number of R and D items that have been omitted or underestimated. The total program plan included in Table I-1 calls for expenditures in excess of \$16 M over the next five years; nonetheless, it is a certainty that the entire tritium handling

effort will continue to expand as the program enters the 1980's. As the TEPR design activity progresses over the coming years, a continuing effort will be made to upgrade and expand Table I-1 in keeping with the status of the design itself. Descriptions of program content and direction for the major R and D items in Table I-1 are summarized below:

1. Fueling Technology

The technology for fueling tokamaks prior to and during the burn cycle is essential to operation of a TEPR with meaningful power-performance characteristics. Development of gas-injection methods to provide uniform and timely delivery of D-T mixtures in a gas-blanket fueling mode should be undertaken. In the event that gas-blanket fueling is not workable for a TEPR, the technology required to form, accelerate, steer, and deliver frozen D-T pellets should be developed in parallel with the gas-blanket fueling mode. Alternative fuel schemes for small tokamaks should be identified and investigated. It seems imperative that fueling of tokamak plasmas during burn cycles be demonstrated in advance of the final fueling-system design for a TEPR.

2. Tritium Storage and Transportation

There is obvious need, from the standpoint of fuel-cycle economy and overall plant safety, to develop fail-safe shipping and storage procedures for tritium. Included in this development program should be some emphasis on concentration, immobilization, and long-term storage of tritiated waste from a TEPR.

3. Fuel Conditioning and Purification

Development of technology to remove all non-hydrogenous impurities from the fuel stream under closed-cycle conditions is needed. (Minimum tritium inventory in the impurity-removal system would be desirable.) Isotopic enrichment technology capable of upgrading fuel quality (increasing T/H ratios) by four to five orders of magnitude appears desirable from the standpoint of fuel cycle economy and maximized tritium recycle. Methods to minimize protium insertion into the fuel stream and all levels of purge operation should be identified and developed.

4. Tritium Recycle Technology

Detailed assessments of state-of-the-art tritium-handling and monitoring technology should be completed. Experimental evaluations of equipment and construction materials that are unique to fusion reactors should be initiated. The technology for complete closed-cycle operation of high-level tritium systems should be firmly established. Reliable pump, compressor, valve, structural containment, and maintenance methods should be verified. Programs to supplement the existing data base for tritium-related phenomena should be expanded and upgraded.

5. Tritium Control Technology

State-of-the-art assessments in this area should be completed at the earliest possible time. Efforts currently aimed at the development of barriers to tritium migration (by permeation, leakage, etc.) should be continued and should increase as the tritium containment requirements of a TEPR are more clearly established. The pertinent data base which includes measurements of hydrogen isotope permeation rates, solubilities, effects on structural and mechanical properties, and chemical interactions should continue to be expanded. The technology needed to operate and process whole-room purge streams should be established, and methods for efficient recycle of tritium recovered from these streams should be developed.

6. Blanket Processing Technology

Optimum methods for tritium recovery from liquid and solid breeder blanket materials must be identified, developed, and verified. This program should include considerations related to (1) minimum tritium-inventory blankets, (2) minimum protium insert into the blanket system, (3) impurity effects on tritium recovery, (4) monitoring of recovery process operation, (5) minimum impact on reactor neutronics, and (6) minimum impact on thermal performance. In parallel with the development programs, there should be an extensive effort to provide needed physical- and chemical-property data related to tritium recovery and to overall breeder-blanket performance.

TABLE I-1. Five Year Plan for TEPR Tritium Handling Technology

RESEARCH & DEVELOPMENT ITEMS	FY-76	FY-77	FY-78	FY-79	FY-80	Total
	(Dollars in Thousands)					
I. <u>Fueling Technology</u>	\$ 100	\$ 100	\$ 300	\$ 350	\$ 450	\$1300
A. Pellet Injection						
Scientific Manpower	(Developed under Plasma Engineering)					
Equipment & Facilities						
B. Gas Injection						
Scientific Manpower	50	50	150	200	200	
Equipment & Facilities	-	-	-	-	50	
C. Neutral Beams						
Scientific Manpower	(Developed under Neutral Beam Program)					
Equipment & Facilities						
D. Instrumentation & Control						
Scientific Manpower	50	50	100	100	150	
Equipment & Facilities	-	-	50	50	50	
II. <u>Storage & Shipping of Tritium</u>	\$ 50	\$ 100	\$ 150	\$ 300	\$ 400	\$1000
A. Fail-Safe Storage						
Scientific Manpower	25	50	100	150	200	
Equipment & Facilities	-	-	-	50	50	
B. Shipping Safe-Guards						
Scientific Manpower	25	50	50	100	100	
Equipment & Facilities	-	-	-	-	50	
III. <u>Fuel Conditioning & Purification</u>	\$ 150	\$ 400	\$ 750	\$1100	\$1250	\$3650
A. Ash & Impurity Removal						
Scientific Manpower	50	100	150	200	200	
Equipment & Facilities	-	-	100	100	50	
B. Isotope Separation						
Scientific Manpower	100	200	300	500	600	
Equipment & Facilities	-	50	150	200	300	
C. Instrumentation & Control						
Scientific Manpower	-	50	50	100	100	
Equipment & Facilities	-	-	-	-	-	
IV. <u>Tritium Recycle Technology</u>	\$ 50	\$ 200	\$ 550	\$ 850	\$1050	\$2700
A. Materials Development & Optimization						
Scientific Manpower	50	100	150	250	300	
Equipment & Facilities	-	-	50	100	100	
B. Hardware Department						
(Pumps, Compressor, etc.)						
Scientific Manpower	-	100	250	300	400	
Equipment & Facilities	-	-	100	200	250	

cont'd

TABLE I-1. Five Year Plan for TEPR Tritium Handling Technology (cont'd)

RESEARCH & DEVELOPMENT ITEMS	FY-76	FY-77	FY-78	FY-79	FY-80	Total
	(Dollars in Thousands)					
V. Tritium Control Technology	\$ 350	\$ 700	\$1000	\$1350	\$1650	\$5050
A. High Temp. Barrier Methods						
Scientific Manpower	100	150	150	200	200	
Equipment & Facilities	-	-	50	100	100	
B. Low Temp. Containment Tech.						
Scientific Manpower	50	100	100	150	150	
Equipment & Facilities	-	-	50	50	50	
C. Purge Development						
Scientific Manpower	50	150	200	300	400	
Equipment & Facilities	-	50	50	100	250	
D. Recovery and Recycle						
Scientific Manpower	50	100	150	200	200	
Equipment & Facilities	-	-	50	50	100	
E. Physical & Chemical Data Measurements						
Scientific Manpower	100	150	200	200	200	
Equipment & Facilities	-	-	-	-	-	
VI. Recovery from Lithium Blankets	\$ 300	\$ 550	\$1050	\$1550	\$2200	\$2300*
A. Lithium (Liquid)						
Scientific Manpower	100	150	250	400	600	
Equipment & Facilities	-	50	100	300	400	
B. Molten Salts						
Scientific Manpower	50	100	200	200	300	
Equipment & Facilities	-	-	50	100	150	
C. Solid Materials						
Scientific Manpower	50	100	200	250	400	
Equipment & Facilities	-	-	50	100	150	
D. Supporting Data Measurements						
Scientific Manpower	100	150	200	200	200	
Equipment & Facilities	-	-	-	-	-	
Totals	\$1000	\$2050	\$3800	\$5500	\$7000	\$16,000

* Assuming that not all of these blanket concepts will be fully developed, and that only 40% of the total amount shown under VI will be needed.