THE TITAN REVERSED-FIELD PINCH FUSION REACTOR STUDY

**Scoping Phase Report
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1. EXECUTIVE SUMMARY

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1. EXECUTIVE SUMMARY

1.1 OBJECTIVES

The TITAN research program is a multi-institutional effort to determine the potential of the Reversed-Field Pinch (RFP) magnetic fusion concept as a compact, high-power-density, and "attractive" fusion energy system from economic, environmental, and operational view-points. The primary program objectives are:

1. Determine the technical feasibility and key developmental issues of an RFP fusion reactor, especially at high power density.
2. Determine the potential economics (cost of electricity), operational, safety, and environmental features of such a high-power-density RFP reactor.

Auxiliary objectives are:

1. Establish the major technical features of an RFP reactor.
2. Develop detailed conceptual designs for the major subsystems and components.
3. Assess the degree of extrapolation between the present data base in RFP physics and in technology and the physics/technology requirements of an RFP reactor.
4. Determine the technical features and parameters of RFP devices required at key steps in a development program.
5. Develop innovative design approaches for a high mass-power-density fusion system.

Mass-power-density (MPD) is defined [1] as the ratio of net electric power to the mass of the fusion power core (FPC), which includes the plasma chamber, first wall, blanket, shield, magnets, and related structure.

1.1.1. Design Goals

Fusion reactor conceptual design has become a mature research field, and results from systems studies research have greatly influenced the direction of the physics and technology elements of the fusion energy program [2]. The

reactor studies during 1970's were focused on central power stations with electric power outputs in the range 1000 to 2000 MWe. These designs were usually based on superconducting magnets to minimize the recirculating power and generally had low neutron wall loadings ($1-2 \text{ MW/m}^2$). They shared basic disadvantages of large stored magnetic energy and FPCs, which were very large in volume and heavy in mass. These early studies projected systems with large total power output, high direct capital cost and low power density, and generated the perception that fusion power, if feasible, would only come in units of large size and low power density and as a result would be expensive.

More recent reactor studies now seek ways to use the past experience and move toward a more affordable, competitive, and "attractive" fusion reactor. One of the approaches to the new generation of reactor design is the compact reactor option [1,3-5]. The main feature of a compact reactor is a FPC with a mass power density in excess of 100-200 kWe/tonne. The increase in mass power density is achieved by increasing the plasma power density and neutron wall loading, by reducing the size and mass of the FPC through decreasing the blanket and shield thicknesses and using resistive magnet coils, as well as by increasing the blanket energy multiplication ratio.

Even though compact designs push toward very high mass-power-density regimes, increasing realism in conceptual reactor design and costing has moved even the "conventional" designs toward smaller FPCs and higher mass-power-densities. As an example, one might begin in 1974 with UWMAK-I [8] at 20 kWe/tonne to STARFIRE [9] in 1980 and MARS [10] at 50 kWe/tonne to GEZEROMAK [5] at 100-200 kWe/tonne and compact reversed-field pinch reactor, CRFPR [3,6-7] at 800-1000 kWe/tonne. A compact reactor, thus, strives toward a system with FPCs comparable in mass and volume to the heat sources of alternative fission power plants with mass power densities in the range 500-1000 kWe/tonne and competitive cost of energy. These arguments have recently prompted the suggestion that a mass power density of 100 kWe/tonne be a threshold goal for fusion reactor design [1].

Other potential benefits for compact systems can be envisioned in addition to improved economics. The FPC cost in a compact reactor is a small portion of the plant cost and, therefore, the economics of the reactor would be less sensitive to changes in the unit cost of FPC components or the plasma performance. Moreover, since a high mass-power-density FPC is smaller and cheaper, a rapid development program at lower cost is possible, changes in FPC

design would not introduce large cost penalties, and the economics of learning curves can be readily exploited throughout the plant life.

Mass power density, however, is only one general measure of the potential economic competitiveness of a fusion reactor. Other factors should also be considered in the search for an optimum fusion reactor. One can summarize the general features of an "attractive" fusion reactor as:

1. Potential for a range of power output. Reduced net power output and associated lower capital investment (investment at risk) not only makes the plant more attractive, it can also permit an affordable development pathway to bring the fusion option to commercial fruition.
2. Affordable and competitive total cost, unit direct cost (UDC, \$/kWe), and cost of electricity (COE, mills/kWh). This goal can be achieved by:
 - A. increasing mass power density,
 - B. increasing overall plant efficiency (i.e., high thermal conversion efficiency and low recirculating power),
 - C. reducing or combining the functions of reactor subsystems and plasma support technologies.
3. Simplified overall FPC design (an obvious but usually unquantifiable operational benefit).
4. Reduced engineering constraints (e.g., magnetic fields, stresses, magnetic stored energy), simple subsystem design (e.g., large duct blanket, single coolant), and combined subsystem functions (e.g., integrated blanket/coil [11]) can lead to safe and reliable operation, reduce the forced outages, allow eased and rapid maintenance, and as a whole can drastically increase the plant availability.
5. Built-in enhanced safety and environmental features, which reduce the use of safety-specific systems and reduce the probability of accidents with either serious public health or capital cost consequences.
6. Reduced rad-waste disposal requirements. Use of low-activation materials reduces the quantity and quality of radioactive waste and eases the long term waste disposal issues.

It should be emphasized that some of these goals and features of an "attractive" fusion reactor may not be achievable simultaneously, and trade-offs are

required. The effect of these trade-offs can be assessed only through specific and detailed design.

The RFP has inherent characteristics which allow it to operate at high mass power densities (> 500 kWe/tonne). This potential is available because the main confining field in an RFP is the poloidal field, which is generated by the current flowing in the plasma. These inherent characteristics of the RFP allow it to meet, and actually far exceed, the threshold value of 100 kWe/tonne, while simultaneously meeting many of the desirable attributes for a fusion reactor, listed above. As a result, the TITAN study also seeks to find potentially significant benefits and to illuminate main drawbacks that can be obtained by operating well above the MPD threshold of 100 kWe/tonne. The program, therefore, has chosen a high neutron wall loading as the reference case in order to quantify the issue of engineering practicality of operating at high mass power density. However, the study has simultaneously put strong emphasis on safety and environmental features as well as maintainability, reliability, and availability issues. These features and constraints are incorporated into the FPC design from the beginning. For example, for the treatment of radioactive waste, TITAN aims at design concepts with 10CFR61 Class-C waste.

An important potential benefit of operating at very high mass power density is the possibility of a single-piece (or few-piece) maintenance scheme for the FPC. In such a maintenance scheme, the reactor torus is replaced as a single unit, including the plasma chamber, first wall, blanket, and possibly the shield and toroidal field coils. The potential benefits of such a replacement scheme as compared with a more "conventional" modular approach are:

1. The reactor torus is made of a few factory-fabricated pieces that are assembled on-site, in a non-nuclear environment, into a fully operational unit.
2. The FPC can undergo full operational, non-nuclear (possibly with hydrogen plasma) testing before installation in the reactor building.
3. The number of connections that must be made or broken in the nuclear environment is minimized.
4. The scheduled maintenance period is shortened because of reduced replacement time and shorter restart period with increased confidence level.
5. The procedure to recover from unscheduled events is more rapid using

more-or-less standard replacement techniques to install a stand-by and pretested torus.

6. This approach can also accommodate FPC improvement throughout the plant life and allows full benefit from learning curves economics.

These potential benefits of the single-piece maintenance approach should ultimately translate into an increase in plant availability and directly improve the economics of the plant. The TITAN study seeks to quantify and demonstrate these potential benefits.

1.1.2. Program Approach

To achieve the design objectives of the TITAN study, the program is divided into two phases, each roughly one year in length: the Scoping Phase and the Design Phase. The objectives of the scoping phase are: to define the parameter space for a high-MPD reactor; to explore a variety of approaches of major subsystems; to select at most two major design approaches consistent with high MPD; and to reach the intermediate stage of preliminary engineering design and integration. The two major approaches identified during the scoping phase would then be the subject of more detailed and in-depth analysis during the design phase.

The first half of the scoping phase was devoted to wide-range scoping studies of a large variety of different design concepts. The purpose of this period was to "let a thousand flowers bloom," and to encourage creativity and the generation of new ideas. The guidelines followed were to find concepts that held the potential to form the basis for an attractive compact RFP reactor. Those ideas and concepts that seemed promising were selected for more detailed analysis during the latter part of the scoping phase. The impact of various design options was routinely evaluated and analyzed through systems studies. At the end of the scoping phase, preconceptual design definitions of major reactor subsystems were available to initiate the design phase. This report contains the results of the scoping phase activities of the TITAN program.

1.2. RFP CONFINEMENT CONCEPT

The principles of the RFP confinement concept are summarized in this section. A more detailed description of RFP confinement is given in Ref. 12 and references contained therein.

The RFP, like the tokamak, belongs to a class of axisymmetric, toroidal confinement systems that utilize both toroidal (B_ϕ) and poloidal (B_θ) magnetic fields to confine the plasma. In the tokamak, stability is provided by a strong toroidal field ($B_\phi \gg B_\theta$) such that the safety factor exceeds unity, that is, $q > 1$, where $q(r) \equiv rB_\phi/R_TB_\theta$, and R_T and r_p are, respectively, the major and minor radii of the plasma. In the RFP, on the other hand, strong magnetic shear produced by the radially varying (and decreasing) toroidal field stabilizes the plasma with $q < 1$ and relatively modest B_ϕ . Theoretically, an electrically conducting shell surrounding the plasma is required to stabilize the long-wavelength MHD modes. In both the RFP and tokamak, equilibrium may be provided by either an externally produced vertical field, a conducting toroidal shell, or a combination of both. Figures 1.2.-1.a, b, and c respectively show, the radial variation of the poloidal and toroidal field and also the safety factor for tokamaks and RFPs.

The RFP relies strongly on the poloidal field generated by the current in the plasma. This feature has several reactor relevant advantages. The poloidal field decreases inversely with the plasma radius outside the plasma. The toroidal field is also very weak outside the plasma. The low magnetic field strength on the external conductors results in a high engineering beta (defined as the ratio of the plasma pressure to the magnetic field pressure at the magnets). Low-current-density, less-massive, resistive coils are, therefore, possible. Also, the RFP can operate at high total beta, RFPs, thereby, allowing operation at high power densities ($\propto \beta^2 B^4$). The experimentally measured poloidal beta values are in the range 10-20%, which is the range used in reactor studies. Furthermore, the RFP relies on the magnetic shear to stabilize the plasma; RFPs can, therefore, operate with a large ratio of plasma current to toroidal field, and stability constraints on the aspect ratio, R_T/r_p , are removed. High-current-density operation and ohmic heating to ignition are possible and the choice of the aspect ratio can be made solely on the basis of engineering constraints.

The fundamental property of the RFP is that the field configuration and toroidal-field reversal is the result of the relaxation of the plasma to a near-minimum-energy state, as proposed by Taylor [13-15]. These relaxed states can be described by the following dimensionless quantities: the pinch parameter, $\Theta \equiv B_\theta(r_p)/\langle B_\phi \rangle$, and the reversal parameter, $F \equiv B_\phi(r_p)/\langle B_\phi \rangle$, where $\langle B_\phi \rangle$ is the average toroidal field. The locus of relaxed states then forms a curve in F - Θ space, as is shown in Fig. 1.2.-2 (labeled as BFM). In the same figure, the

Fig. 1.2.-1. Magnetic field distribution for tokamak (a) and RFP (b) and the q profiles for tokamak and RFP (c).

Fig. 1.2.-2. Locus of operating points on the $F-\Theta$ diagram [16]. The solid line (BFM) is the curve predicted by Taylor's theory and the data points are from several RFP experiments.

corresponding experimental data are also shown which lie to the right of Taylor's model. These experimental equilibria differ from Taylor's model since the plasma has a finite pressure, and a perfectly conducting wall is not used. The experimental points in Fig. 1.2.-2 represent "near-minimum-energy" states with finite plasma beta.

The theory of relaxed states has two important consequences. First, the theory predicts that if the current and toroidal flux are maintained constant in time (i.e., constant Θ), then the relaxed state equilibrium will be sustained. Experimentally, RFPs are observed to exist for times much larger than the decay time of the field profile due to resistive diffusion. This process involves continuous generation of toroidal field within the plasma, which compensates for the resistive decay of the toroidal field and maintains the field profile. This toroidal-flux generation process is called the RFP "dynamo".

Second, there is a strong coupling between the toroidal and poloidal fields; the toroidal field can be generated by driving toroidal current with external poloidal field circuits. This strong coupling offers the possibility of a novel and efficient steady-state current drive system through the "helicity injection" technique such as the Oscillating-Field Current Drive (OFCD) technique [17-19], which is based on low-amplitude, low-frequency oscillation of the main confining fields.

High-temperature plasmas are routinely produced in many intermediate-size RFP machines such as ETA-BETA-II in Padova [20-22], TPE-1R(M) at ETL, Sakura-Mura [23,24], ZT-40M at Los Alamos [25-27], HBTX1A at Culham [28-29], and OHTE/RFP at GA Technologies [30,31]. The plasma parameters obtained in these experiments have been improving steadily. Values of poloidal beta, β_θ , in the range 0.1 to 0.2 are routinely achieved; these values are adequate for a reactor. Electron temperatures in the range 0.4-0.6 keV, densities up to about 10^{20} m^{-3} , and energy confinement times of a few tenths of millisecond are typical of these intermediate-size experiments. Data from a number of machines indicate a linear temperature-current scaling and both experimental and theoretical evidence suggests a strong scaling of nT_e with the plasma current.

Some theoretical models for the transport in RFPs have been proposed, although a detailed transport model is not yet available for RFPs. One can use an empirical approach to evaluate present experimental results and form a basis for the extrapolation of these results to reactor regimes. Extensive measurements of the dependence of the temperature to the current indicate that the on-axis electron temperature increase with I_ϕ as $T_e(0) \propto I_\phi^{\nu'}$, where ν' is in

the range of 0.5-1.0. For several experiments, $\nu' \approx 1$ up to plasma currents of 500 kA. More recent results [32,33] suggest that the temperature-current scaling might be better described by postulating a constant beta, $nT_e(0) \propto I_\phi^2$. Evidence from a number of experiments indicates that β_θ varies relatively little over a range of conditions and from one machine to another.

Estimates have been made of the energy confinement time, τ_E , in various RFP experiments, but only a limited amount of scaling information is available. Specifically, quantitative data on the variation of τ_E with machine radius is not available. The experimental value of τ_E is generally obtained from the ratio of plasma energy to the heating power, which is assumed to be the ohmic dissipation of the plasma current.

Under the assumptions of $T_e \propto I_\phi^{\nu'}$, the plasma electrical conductivity, $\sigma \propto T_e^{3/2}$, and $\tau_E \propto r_p^2$, the following "ohmic" scaling law can be deduced:

$$\tau_E \propto \frac{I_\phi^{5/2-\nu'}}{Z_{\text{eff}}} r_p^2 f(\beta_\theta, I_\phi/N), \quad (1.2.-1)$$

where N is the plasma line density, and the τ_E dependence on β_θ and I_ϕ/N have been incorporated into the function $f(\beta_\theta, I_\phi/N)$. In Fig. 1.2.-3, the inverse of plasma diffusivity, $1/\chi_E \propto \tau_E/r_p^2$ is plotted as a function of I_ϕ using the data from several experiments. Two analytical curves that fit the data are also included. The design point for a RFP reactor is also shown. In the case where β_θ is approximately constant, then $T_e \propto I_\phi$ ($\nu' = 1$), and ohmic scaling Eq. (1.2.-1) yields $\tau_E \propto I_\phi^{3/2} r_p^2$ and $n\tau_E \propto I_\phi^{5/2}$, provided that Z_{eff} does not vary. A similar conclusion was also reached in OHTE/RFP where a value of $n\tau_E \sim 10^{17} \text{ s/m}^3$ was recorded.

In conclusion, the theoretical and experimental data base for RFPs is less extensive than that of tokamaks and, thus, requires a larger extrapolation to reactor relevant regimes. Modern RFP experiments, however, have all demonstrated the robustness of the RFP dynamo, and a common understanding of the basic physical processes operative in RFPs is emerging. The largest uncertainties in the existing RFP data base remain in the confinement physics and, in particular, in the mechanism and magnitude of cross-field transport. Experiments with higher currents (and possibly higher current densities) and variable plasma size are needed to distinguish between different possible

Fig. 1.2.-3. Variation of the global energy confinement time, τ_E , with the plasma current with data from several experiments [3].

scaling laws. Data from large multi-mega-ampere experiments are expected in the early 1990s [16,34]. These data are of the utmost importance in resolving some of the key physics requirements and uncertainties for an RFP reactor. Furthermore, these next-step experiments can provide valuable technological insight for devising a development path towards RFP fusion reactors.

1.3. PARAMETRIC SYSTEMS STUDIES

Parametric systems studies were performed to identify "strawman" design points and to establish the context of the design by means of sensitivity and trade-off studies. These cost-optimized strawman design points provide the starting point of a set of activities that comprises the TITAN study. First, magnetics calculations produce a realistic design for magnet coil sets needed for confinement, equilibrium, and start-up of the fusion core. Also, fusion-core plasma/circuit simulations result in detailed evaluation of key plasma parameters. These data are used to study and design the plasma support subsystems. With this detailed description of the fusion core, the engineering design activities are initiated. The neutronics, thermal-hydraulic, structural, material, and safety analyses are performed to assess the engineering performance of the key subsystems. These subsystems are then integrated into the reactor design. At each step of the analysis, feedback is provided to the systems analysis activity to improve parametric systems models which are then used to generate new, cost-optimized strawman designs for further conceptual design.

A parametric-systems-analysis (PSA) computer code is used for the sensitivity and trade-off studies. The code was originally developed for use in the CRFPR frame-work studies [2,6] and upgraded for the TITAN study. The PSA code searches for design-points with minimum COE. Parametric systems studies were performed to assess RFP reactors with a range of power outputs and neutron wall loadings, with the results shown in Fig. 1.3.-1. The dependence of COE on net plant capacity, shown in Fig. 1.3.-1, is typical of the nuclear economy of scale.

The most prominent feature of Fig. 1.3.-1 is the shallowness of the minimum of COE versus the plasma radius, r_p (and, hence, the neutron wall loading, I_w), although the compressed COE scale should be noted. This relative insensitivity is partly a result of the FPC cost being a small portion of the overall plant direct cost. In principle, other developmental and operational incentives, not

Fig. 1.3.-1. The Cost of electricity, COE, for a compact RFP reactor with PbLi/HT-9 blanket as a function of plasma radius, r_p , for a range of P_E values. The COE of a 1000 MWe RFP reactor with Li/V blanket is also shown. Contours of constant I_w as well as the condition where $\tau_E(\text{OPT}) = \tau_E(\text{PHYS})$ are shown assuming $\tau_E(\text{PHYS}) \propto I_p^{\nu} r_p^2 f(\beta_g)$ scaling and a range of ν values.

included in the present costing model, may make the minimum of COE as a function of the wall loading more pronounced. An example is the issue of the single-piece maintenance (Sec. 1.7) and its impact on plant availability. If this approach results in a smaller plant down time compared with a modular maintenance scheme, the maximum weight and size of the FPC that can be maintained as a single-piece may introduce an abrupt change in plant availability and, hence, in COE. The TITAN study seeks to quantify potentially significant benefits or drawbacks that results from operating at very high wall loading and mass power densities well above the threshold of 100 kWe/tonne.

The CRFPR framework studies [3,4,6,7] focused on a design with a neutron wall loading of $I_w \approx 20 \text{ MW/m}^2$, high-coverage (poloidal) pump limiters, Oscillating-Field Current Drive (OFCD) for steady-state operation [17-19], and single-piece FPC maintenance. The reactor featured a water-cooled copper first wall, a self-cooled $\text{Pb}_{83}\text{Li}_{17}$ /ferritic-steel (HT-9) blanket, and thin (0.10-m) steel shielding. Also, resistive copper-alloy toroidal-field (TFC), ohmic-heating (OHC), and equilibrium-field coils (EFC) were used.

The present focus of the TITAN study is a 1000 MWe (net) reactor. Typical physics, engineering, and costing parameters are listed in Table 1.3.-I and compared with the CRFPR study. The TITAN design is a divertor-based [35], high-neutron-wall-loading ($10\text{-}20 \text{ MW/m}^2$) reactor that also invokes OFCD for steady-state operation, retaining the motivation of high power density, compact fusion. A range of pool- and loop-type blanket concepts is being considered. The TITAN design features superconducting EFCs in order to eliminate steady-state power consumption in the resistive EFC, combined with a desire for a more open FPC geometry for the ease of maintenance. The OHCs and TFCs in TITAN, however, have remained as resistive-coil systems in order to retain a compact reactor torus, with the OHC being used only for start-up. An elevation view of TITAN is shown in Fig. 1.3.-2 which illustrates the "openness" of the TITAN design for maintenance purposes.

1.4. PLASMA ENGINEERING

The plasma engineering effort starts with the TITAN "strawman" designs (Table 1.3.-I), which are generated by the parametric systems analysis. Then, magnetics calculations produce a realistic design for magnet coil sets needed for confinement, equilibrium, and start-up of the fusion core. Also, plasma/circuit simulations result in detailed evaluation of key plasma

TABLE 1.3.-I
SUMMARY AND COMPARISON OF 1000-MWe TITAN STRAWMAN DESIGN-POINT

	<u>TITAN</u>	<u>CRFPR [6]</u>
Neutron wall loading, $I_w(\text{MW}/\text{m}^2)$	18.	19.
First-wall minor radius, $r_w(\text{m})$	0.65	0.75
Plasma minor radius, $r_p(\text{m})$	0.60	0.71
Plasma major radius, $R_T(\text{m})$	3.90	3.90
Average plasma density, $n(10^{20}/\text{m}^3)$	4.35	6.55
Average plasma temperature, $T(\text{keV})$	20.	10.
Poloidal beta, β_θ	0.20	0.23
Plasma current, $I_\phi(\text{MA})$	17.75	18.4
Energy Confinement Time, $\tau_E(\text{s})$	0.27	0.23
Pinch parameter, Θ	1.55	1.55
Reversal parameter, F	-0.10	-0.12
Poloidal field at plasma surface, $B_\theta(\text{T})$	5.2	5.2
Reversed-toroidal field during burn, $-B_{\phi R}(\text{T})$	0.36	0.40
Engineering Q-value, $Q_E = 1/\epsilon$	7.84	5.0
Fusion power, $P_F(\text{MW})$	2,261.	2,733.
Total thermal power, $P_{TH}(\text{MW})$	2,866.	3,472.
System power density, $P_{TH}/V_{FPC}(\text{MWt}/\text{m}^3)$	12.8	9.7
Mass power density, $1000P_E/M_{FPC} = \text{MPD}(\text{kWe}/\text{tonne})$	644.	800.
Cost of electricity, $\text{COE}(\text{mills}/\text{kWeh})$	25.	37.

parameters. These data are used to study and design the plasma support subsystems. As a whole, the fusion core physics activity provides a detailed description of the fusion core for all engineering activities and design efforts. Feedback is also provided to the systems analysis activity to improve parametric systems models which are then used to generate new, cost-optimized strawman designs for further conceptual engineering design.

1.4.1. Magnet Configuration

The magnet configuration consists of a poloidal-field coil (PFC) set, a toroidal-field coil (TFC) set, a divertor coil set, and an Oscillating-Field Current-Drive (OFCD) coil set. The divertor and the OFCD analyses have not progressed sufficiently to yield specific coil designs.

Fig. 1.3.-2. The elevation views of the TITAN reference case and CRFPR [3,6].

1.4.1.1. Poloidal-Field Coil (PFC) System

The PFC set performs both an equilibrium and an ohmic-heating (start-up) function. The equilibrium function requires that a vertical field of a certain magnitude and index, related to the plasma current and beta, be imposed over the plasma cross section in order to maintain the plasma against the outward expansive forces arising from plasma and poloidal-field pressure. The ohmic-heating function provides the poloidal-flux swing required to establish the steady-state plasma current, which is then subsequently sustained by OFCD. Since the ohmic-heating function is required only during start-up and the equilibrium function is required continuously, the PFC set is naturally, but not necessarily, split into two coil sets: an equilibrium-field coil (EFC) set and an ohmic-heating coil (OHC) set.

Equilibrium-Field Coils (EFCs). Since the EFCs are continuously active, the recirculating power can be minimized by using superconducting EFCs. Superconducting EFCs, however, require ≥ 1.5 m of blanket and shielding between the coils and plasma compared with ≤ 0.8 m for resistive EFCs; hence, more current is needed to produce the same field resulting in a more massive and expensive coil set. The trade-off between normal-conducting and superconducting EFCs was examined and found to weigh somewhat in favor of superconducting EFCs (Sec. 5.3). Consequently, the use of superconducting EFCs was adopted for this study. A more detailed analysis of the superconducting EFC performance during the plasma transients are underway. An additional constraint is imposed to use only a single pair of EFCs positioned not to interfere with vertical or horizontal movement of the first wall, blanket, shield, and TFC assembly during maintenance procedures. The PFC arrangement for TITAN is shown in Fig. 1.3.-2, which generally meets the above requirements.

Ohmic-Heating Coils (OHCs). An efficient coupling of OHCs to the plasma is obtained with the "close-fitting" OHC configuration shown in Fig. 1.3.-2. Such a configuration requires the removal of most of the OHCs in the upper-half plane to gain access to the reactor torus for (single-piece) maintenance purposes. In order to eliminate the need for coil movement for maintenance purposes, the OHCs can be arrayed into two vertical stacks with one stack positioned inboard of the torus and one positioned outboard (Fig. 4.4.-2).

The OHC set should be designed to couple efficiently with the plasma in order to minimize the start-up power and voltage requirements and the

engineering issues associated with the OHC magnet design (e.g., stresses). An additional constraint on the OHC design is the maximum level of the stray vertical field during breakdown.

Both the "close-fitting" and the "vertical-stacks" configurations were analyzed in the scoping phase. Given practical limits on start-up power and voltages, only the close-fitting configuration was found to comply with the stray-vertical-field constraint. This configuration was adopted, and a reference PFC design was produced which is shown in Fig. 1.3.-2.

1.4.1.2. Toroidal-Field Coil (TFC) System

Two options are being considered for the TFC set. The first is a resistive, copper TFC set, positioned outside the blanket and shield. In order to permit service access to the reactor torus, the TFCs must be discretized rather than forming a continuous toroidal shell. The discrete TFC set, however, introduces a toroidal-field ripple which can adversely affect the confinement. Therefore, the management of the ripple is a major factor in the design of the TFCs.

An accurate assessment of island widths can be obtained from three-dimensional field-line tracings which simulate the toroidal, radial, and poloidal components of the magnetic field. Although such simulations remain to be done for this study, previous simulations [3] indicate that islands can be kept acceptably small if $\Delta B_R/B_\theta < 0.003$, which is the criterion used for the ZT-H design [16]. Scaling the number of TFCs from that design has resulted in a preliminary TFC design with 28 TF coils.

The design issues associated with the toroidal-field ripple has led, in part, to the consideration of the integrated blanket/coil (IBC) concept [11] (Sec. 8.2.5) as a second TFC option. The IBC concept combines blanket and TFC functions by using a liquid metal, which breeds tritium to fulfill the blanket function, flows so it can remove the energy deposited within it, and conducts electricity to fulfill the TFC function. The combination of functions eliminates the need for coolant penetrations through the conductor. With the major penetrations eliminated, the TFC current channel approaches a continuous toroidal shell which in principle introduces no toroidal-field ripple. However, the IBC has a number of potential problems such as high-current (1-2 MA), low-voltage (8-10 V) power supplies, field errors produced in the vicinity of the current leads, and the trade-offs between thermal-hydraulics flow paths and electrical flow paths.

1.4.2. Plasma/Circuit Simulation

It became evident towards the end of the early RFP reactor studies [3,6] and during the earliest phase of the TITAN study that both the TFC and PFC (OHC + EFC) design limits would be determined more by the plasma breakdown, formation, and ramp-up transients than by the steady-state operational phase. Both the desire to use the RFP dynamo to generate internal toroidal flux, rather than injecting all the toroidal flux by the TFCs, and the OHC back-bias stress and power strongly influence the TFC and OHC designs. Furthermore, the PFC configuration determines the coupling of OHC with the plasma, the magnitude of the stray vertical field, and the degree of multipolarity of field nulls in the plasma chamber. These in turn influence the breakdown and RFP formation conditions through the amount of initial (vacuum) toroidal field, $B_{\phi 0}$, and ultimately affect the TFC design.

A body of experimental data is beginning to accumulate, which better defines the formation "window" and associated PFC/TFC circuit requirements for the TITAN RFP reactor. Although much of this information is not theoretically understood fully and extrapolation from ZT-40M-class experiments to a reactor is uncertain, this information and experience nevertheless is assimilated for the first time and used as part of the TITAN study. The formation phase of the RFP is characterized by the following experimentally observed behaviors:

- ♦ upper and lower density limits define a region outside of which poor or no RFP formation occurs.
- ♦ minimum plasma current (or possibly current density, in that size variations are limited in present-day experiments) below which robust RFPs cannot be formed.
- ♦ minimum limit on the toroidal electric field, E_{ϕ} , or ratio of E_{ϕ} to initial filling pressure, E_{ϕ}/P_0 , to ensure break-down.
- ♦ upper limit on the formation time, τ_R .
- ♦ limits imposed on initial (vacuum) toroidal magnetic field, $B_{\phi 0}$.

In addition to setting windows for RFP formation, relationships between these variables and the poloidal-flux and energy consumption during formation have been derived [36]. These constraints are summarized in Sec. 4.5.1 and formulated into a simplified breakdown and formation model that in turn is evaluated to provide initial conditions for the simulation of plasma start-up, ignition and burn.

An important result of the foregoing analysis is the evaluation of the impact of stray vertical field on the RFP formation requirements. Any increase in the stray vertical field produced by the OHC set at the back-bias, results in increases in the flux, energy, and power consumption during the RFP formation phase. Since the resistive poloidal-flux consumption during the full ramp-up to ignition and burn is ~ 25 Wb ($\sim 10\%$ to total), an additional flux consumption during formation much above this value becomes a concern from the viewpoint of back-bias stress in the OHCs. As a result, a maximum value of 2.5 mT for the stray vertical field was adopted for the OHC design.

Analysis of plasma circuit interactions determines the plasma response to the externally applied fields. Such analyses are required so that appropriate switching sequences and voltages can be applied to the external circuitry (e.g., PF and TF coils) for various transient plasma operations, such as start-up and shut-down, fractional power operation, and Oscillating-Field Current Drive (OFCD). This simulation is performed through a plasma/circuit interaction code. The preliminary results given for the RFP formation phase have been used to estimate initial conditions for the simulation of the post-formation fast current ramp (few seconds to $I_\phi = 10$ MA) followed by a slower ramp driven from the grid to ignition and burn at $I_\phi = 18$ -20 MA. Typical results of this simulation are given in Fig. 1.4.-2.

The time-varying electromagnetic fields during the plasma transients induce eddy currents in all conducting material in the vicinity of the FPC such as the first wall, liner/conducting shell, vacuum vessel, blanket, shield, structures, etc. These eddy currents retard and modify the plasma response to externally applied fields. Furthermore, these eddy currents give rise to magnetic fields affecting the plasma equilibrium, to electromagnetic forces on all conducting materials which carry the eddy currents, and to additional energy drain from the external circuits to compensate for Joule losses by eddy currents. The eddy-current modeling is, therefore, the most critical and usually the most difficult component of plasma circuit interaction analyses. To study the impact of eddy currents on the plasma response of the TITAN reactor, a stand-alone eddy-current circuit analysis code has been developed. Application of this code to the TITAN reactor start-up and transients is guiding the coil design, FPC engineering analyses, and FPC design integration effort.

Fig. 1.4.-2. Results of a zero-dimensional plasma/circuit simulation for TITAN $I_v \approx 18 \text{ MW/m}^2$ "Strawman" design.

1.4.3. Current Drive

After considering a number of current-drive options during the scoping phase of the TITAN study, the Oscillating-Field Current Drive (OFCD) system was chosen for further evaluation. This choice was based on the projected efficiency of the drive, its relative simplicity, and the uniqueness of this scheme to the RFP.

Unlike the tokamak, the toroidal and poloidal currents in the RFP are closely coupled, since the RFP plasma profiles represent a near-minimum-energy state. If an external circuit parameter (e.g., voltage applied to TFC, V_θ) is varied to change the toroidal flux external to the plasma, intrinsic plasma processes related to turbulence and/or resistive instabilities generate voltages and currents within the plasma and increase or reduce poloidal flux in order to maintain the magnetic helicity constant and the plasma in a near-minimum-energy state. This nonlinear coupling between plasma and magnetic fields through the Ψ - θ diagram, like that shown on Fig. 1.2.-2 or Fig. 1.4.-2, can be used to "rectify" current oscillations created at external coils into a net steady-state current within the plasma [17-19]. This " Ψ - θ pumping" is envisaged to transform toroidal magnetic flux (poloidal currents) into toroidal currents (poloidal magnetic flux) through the plasma relaxation which maintains the near-minimum-energy configuration. The result is an efficient inductive but oscillatory (i.e., with no loss of electromagnetic flux) mean of steady-state current drive.

Although some experimental and theoretical basis exist, substantial current driven by OFCD has not yet been demonstrated in the laboratory and, therefore, represents a main issue for the TITAN design. Given that the OFCD principle can be fully demonstrated experimentally, the design of OFCD coils (e.g., location, sizes) and associated circuitry remains to be completed. Therefore, a plasma/circuit model for OFCD was used to identify and assess, parametrically, the potential design, power engineering, and magnetics problems. Although this model and analysis represents the first attempt to integrate circuit effects into the OFCD plasma modeling, the TITAN layout must be evolved further in other respects before a clear-cut assessment of design, power engineering, and magnetics problems can be made. This work remains for the design phase of the TITAN study. Nevertheless, the following interim observations and conclusions can be made.

The drive coils can be located outside the FPC and can probably be incorporated as a subset of windings on the main coil sets. The eddy currents induced in the material surrounding the plasma chamber distort wave-forms and

generates phase-shifts, giving rise to complications. Small plasma current swings ($\delta I_\phi / I_\phi = 0.02$) are sufficient to drive steady-state currents in the 18-20 MA range with resistive powers in the plasma of ~ 8 -10 MW. Large reactive power (Poynting vector) appears to flow across the plasma surface (20-30 MJ at a frequency of 60 Hz); however, the perturbation to the plasma (as measured in terms of field energy, magnetic field and current fluctuations, and fusion-power oscillations) is small. Energy flow across plasma surface (20-30 MJ) is negligible (1-2%) compared with plasma magnetic energy and is small compared with plasma kinetic energy ($\sim 10\%$). Finally, transfer occurs on a time scale (~ 60 Hz) that is 5-10% of τ_E .

The impact of the driving field oscillations on the RFP dynamo, MHD behavior, and beta remain as unresolved issues. In addition, the maintenance of plasma equilibrium during the OFCD cycle, the impact of reactive power flows on the EFC, and overall energy balance remain to be resolved.

1.4.4. One-Dimensional Core Plasma Simulations

Recent experimental evidence suggests that RFPs operate at a soft beta limit [12,16,37]. Under such a constant β_θ postulate, the transport would adjust itself by MHD activities, radiation, or any other mechanism to lose just enough energy to keep β_θ constant. In particular, f_{RAD} , the fraction of radiative power losses from the plasma to the total losses (radiation plus transport) could be controlled through impurity injection with the only penalty being a modest increase in the plasma resistance (i.e., voltage and power requirement to maintain a given current). This characteristic of the RFP soft beta limit is in marked contrast with other confinement schemes such as the tokamak, where increasing the impurity content would increase the total energy loss rate and, therefore, degrade the plasma pressure. Enhanced radiation from a (high) beta-limited plasma is important because it permits first-wall designs to receive a higher average (but more uniform) heat flux and thereby reducing the divertor (or limiter) power loads, thereby optimizing the overall design for the maximum power density while maintaining realistic engineering constraints on all systems.

Strong experimental evidence exists for a beta limit on RFPs (Sec. 4.3.6). First, a linear scaling is observed between $n_e T_e(0)$ and I_ϕ^2 for currents in the range 60-400 kA. Second, at a given plasma current level, $T_e(0)$ is found to be inversely proportional to n_e . Finally, a set of experiments was performed on ZT-40M by adding trace quantities of krypton as an impurity [16,37] to enhance

the radiative losses of the plasma. The choice of krypton was made to maximize the ratio of radiated power to the ohmic heating input. It was found that as the impurity was injected, the radiation losses were increased (with f_{RAD} as high as 95% being reported). At the same time, the ohmic input power only slightly increased, and most importantly, the poloidal beta remained constant. It follows that as radiation losses increased, the non-radiative losses decreased to preserve the constant beta.

It is important to point out that while these results are suggestive of the beta limit hypothesis, they are not conclusive. Further, it appears that far more power is being supplied to the discharge than is needed to maintain the plasma at its beta limit [16,37] and, therefore, these experiments are not expected to show the magnitude of any underlying transport which is not affected by the beta limit hypothesis.

The RFPBURN [38] one-dimensional transport model has been used to examine some of the properties of a beta-limited and radiation-dominated reactor-grade plasma. For no impurities other than a ~ 4% alpha particle ash, a minimum $f_{\text{RAD}} = 0.12$ is obtained. While low-Z impurities such as carbon can radiate the necessary power, a high impurity level is required, resulting in a high value of $Z_{\text{eff}} = 2$ for the carbon impurity, which would double the current-drive power requirements. High-Z impurities such as xenon require a much smaller impurity concentration and produce lower values of Z_{eff} (e.g., $Z_{\text{eff}}(0) = 1.3$ for Xe and the Z_{eff} decreases with radius as T_e decreases). Therefore, the high-Z impurities are favored for enhancing the plasma radiation fraction.

In conclusion, based on both experiment and theory, it is possible that RFP reactors may exhibit a soft β limit. Such a β limit was assumed in choosing the ZT-H experimental parameters [16]. If such β limits exist, it may be possible to adjust f_{RAD} to any level between 0.12 and 0.95 with only a minor increase (10-30%) in plasma resistance by injecting high-Z impurities into the plasma core. Only small variations in the impurity fraction are required to significantly vary f_{RAD} . In practice, the maximum operating f_{RAD} will be determined by the level of intrinsic transport. If the intrinsic transport mechanisms are classical, then the f_{RAD} upper limit could be higher than 0.99. Finally, it is noted that the impurity fraction of Xe required for $f_{\text{RAD}} = 1$ in the plasma core is two orders of magnitude smaller than that required for $f_{\text{RAD}} = 1$ in the divertor chamber.

1.5. DIVERTOR ENGINEERING

A considerable effort on the impurity control system has been made during the scoping phase of the TITAN study. To avoid the problems of erosion and contamination of the plasma core, associated with limiters, the use of a divertor is proposed. In choosing the type of divertor to be used, a strong preference exists for selecting a configuration in which the minority magnetic field is nulled. This choice minimizes the perturbations to the core plasma and reduces the engineering requirements in terms of coil currents, stresses, and power and energy requirements. In an RFP the toroidal field is weaker than the poloidal field at the plasma surface and bundle divertors or toroidal-field divertors are the main options, whereas a poloidal-field divertor is more appropriate for a tokamak. In the CRFPR study [6,7] it was found for the bundle divertor that the field line connection length was too long, resulting in excessive cross-field diffusion to the first wall. On the other hand, the poloidally-symmetric toroidal-field divertor was considered a feasible design approach worthy of more detailed investigation. As the reactor parameters for TITAN are similar to those of CRFPR this recommendation has been followed and the symmetric toroidal divertor has been selected as the focus of the effort on impurity control for TITAN.

1.5.1. Divertor Configuration

A typical coil layout for the divertor is shown in Fig. 1.5.-1. For TITAN the number of divertors has been set at 4 for the scoping phase, as a compromise between reducing the heat loading on the divertor target, preventing excessive Ohmic losses in the divertor coils, or paying too large a penalty on the tritium breeding ratio. The current in the central nulling coil opposes that of the toroidal field coils, while the flanking coils are present to ensure that the sum of the divertor coil currents is zero, thus minimizing the effect of the divertor on the global magnetic configuration. The field-line tracings in Fig. 1.5.-1 were obtained with a two-dimensional magnetics analysis, including the toroidal and radial components of the magnetic field. Three-dimensional modeling, which also includes the poloidal field produced by the plasma and the EFCs, is necessary to ensure that flux-surface broadening and the formation of magnetic islands because of the divertor coils do not create an unacceptable perturbation to the magnetic configuration near the plasma surface. This simulation will be performed in the design phase of the study.

Fig. 1.5.-1. A plan view of a typical coil layout for a symmetric toroidal-field divertor showing the TF coils, divertor coils and diverted field lines on the inboard and outboard sides (generated with a 2-D magnetics analysis).

A rather closed divertor geometry is naturally obtained for the toroidal-field divertor (unlike the open geometry found with poloidal divertors in tokamak reactors), due to the proximity of the divertor coils to the plasma. This configuration allows the divertor chamber to be decoupled from the plasma chamber, and leakage or backflow from the divertor to the main plasma should, therefore, be minimal. The closed configuration also tends to cause the flux surfaces in the divertor to be compressed, increasing the heat load on the divertor plate. A more open divertor configuration is under study which seems to expand the flux surfaces in the divertor chamber, thereby reducing the heat load on the divertor plate as compared with the closed-divertor configuration.

The Integrated-Blanket-Coil (IBC) [11] approach has been considered for the divertor of the liquid-metal-cooled blanket design, which provides several advantages over a divertor design with conventional copper coils. The main benefit is that the reduction in the tritium breeding ratio and energy multiplication factor is less severe because of the greater blanket coverage obtained. A further advantage is that the coils can be located closer to the plasma, as radiation damage to the conductor and insulator poses less of a problem than for a copper coil design. The coil currents are thereby reduced, offsetting the tendency toward higher Ohmic losses caused by the high electrical resistivity of liquid lithium.

1.5.2. Edge-Plasma Modeling

Both analytic models and one-dimensional radial transport codes have been used to model the TITAN edge-plasma. These models indicate that the scale length for the radial decay of power flow in the scrape-off layer will be small, ~ 1 cm, implying that the power loads on the divertor target will be high. In an effort to reduce the heat flux to acceptable levels, the injection of high Z impurities into the divertor plasma to radiate the incident power over a wider area has been examined. A simplified analytic model has been used to show that impurity fractions on the order of a few per cent are necessary to radiate a large fraction of the power transported to the divertor. To avoid contamination of the plasma core these impurities must be efficiently confined within the divertor chamber. A criterion for the entrainment of impurities in the background plasma flow [39] has been applied to the divertor plasma but preliminary results suggest that it will be difficult to ensure that adequate retention of the impurities will be obtained.

In the design phase of the study the feasibility of confining the injected impurities in the divertor plasma will be examined with more detailed models of the edge-plasma. These improved models will allow the flows in the scrape-off layer to be studied and enable more accurate predictions of plasma parameters at the first wall and divertor target to be made, including estimates of the erosion rate due to sputtering. Improved neutral particle models will be incorporated to simulate recycling in the divertor. The core and edge-plasma models will be coupled to ensure self-consistency of heat and particle fluxes.

1.5.3. Divertor Target Cooling

Several cooling options for the divertor have been examined. Liquid metal cooling is attractive for the Li/Li/V blanket design because of its compatibility with the overall thermal cycle. For the closed-divertor configuration, the component of the magnetic field perpendicular to the coolant flow path is rather strong (~ 1 T compared with ~ 0.4 T for the first wall tubes), which limits the coolant velocity because of large MHD pressure drop. If the coolant tube walls are not electrically insulated, then the maximum wall temperature limit for vanadium restricts the acceptable heat flux to about 3 MW/m^2 for tube walls of 1 mm thickness. If insulated tube walls are used together with a coolant velocity high enough to enter the turbulent regime, heat loads of up to 9 MW/m^2 can be accommodated. For the open-divertor configuration, the perpendicular field strength is much smaller (a few tenths of a Tesla) and, therefore, the maximum acceptable heat flux for this configuration is estimated at about $6\text{--}8 \text{ MW/m}^2$.

Water-cooling is a natural choice for high-heat-flux components, although safety considerations prohibit its use in conjunction with the lithium-cooled blanket design. To maximize the heat-removal capability, the use of swirl flow in the forced convection sub-cooled boiling heat transfer regime has been considered. Copper alloy coolant tubes (of 1 mm wall thickness) allow a heat flux of about 20 MW/m^2 before the maximum allowable temperature limit is encountered for water cooling.

Helium cooling of the divertor is compatible with any of the blanket concepts considered. The very high temperature capability of SiC allows heat fluxes of up to 10 MW/m^2 to be attained for wall thicknesses of 1 mm. Similar heat loads can be accommodated with vanadium, although the coolant outlet temperature is lower. Copper has a higher thermal conductivity, suggesting its use if thicker walls are required. However, because of the lower temperature

limit of copper alloys compared with vanadium or SiC, the heat is not removed at temperatures of interest for power generation.

A brief investigation of innovative concepts has been made. Spreading the heat load by vaporization and remote condensation of a liquid metal has been shown to be not feasible because of the resulting high pressure of the vaporized material. The use of a cloud of lithium droplets to intercept the divertor plasma may be possible but the droplet must have a high velocity to reduce its temperature rise.

As the divertor design for TITAN progresses, and the expected loadings on the divertor are better defined, more detailed calculations on the thermal hydraulics and stress analysis of the divertor cooling will be made.

1.6. FUSION-POWER-CORE ENGINEERING

During the first half of the scoping phase, the TITAN design team members were encouraged to participate in a period of "concept brainstorming," and a very large number of ideas were put forward. Several of these first-wall, blanket, and shield concepts were sufficiently attractive in the context of high power density to warrant detail consideration during the scoping phase. These design concepts can be loosely categorized by the general FPC design as loop-type, pool-type, and loop-in-pool.

The loop-type concept has coolant flow in "loops" around the plasma chamber. The major feature of the loop is the efficient removal of thermal power and the ability to handle high surface heat loads. In the pool-type configuration the plasma chamber and first wall are submerged in a pool of coolant. This configuration is in principle simple, since the pool acts as a replenishable blanket and shield. Furthermore, the pool design promises the potential for inherent safety due to the large heat capacity of the pool. The major disadvantage of the pool concept is the difficult and uncertain flow configuration, which limits the high-heat-flux capabilities of the first-wall and divertor design. The loop-in-pool concept is the synthesis of several attractive ideas. The large heat capacity of the pool-type reactor promises a passively safe reactor with limits on the heat-flux capabilities of the first wall. The loop-in-pool concept submerges a loop-type first wall and blanket in a pool which acts as a heat sink during off-normal events.

The various coolant, breeder and structure options considered for the TITAN FPC are summarized in Table 1.6.-I. Following the initial period of the scoping

TABLE 1.6.-I
FPC CONCEPTS CONSIDERED FOR TITAN

LOOP-TYPE CONCEPTS

Li/Li/V: Adaptation of the BCSS [40] Li/Li/V concept to the RFP. Weak toroidal field of the RFP confinement eases the MHD effects compared with tokamaks. This concept offers the promise of high wall load, good thermal efficiency, good TBR and simple configuration. A modification of this concept is to employ the Integrated Blanket Coil (IBC) concept [11]. The IBC concept uses the blanket lithium as an electrical conductor to provide the toroidal field requirements of the RFP. External power supplies are attached to the blanket coolant headers. The IBC simplifies the FPC design by eliminating the separate TF coils and the shielding required.

He Cooled, Direct Cycle: High temperature, low activation materials are used (e.g., SiC or C/C composites) as the structure. Low activation and low afterheat could lead to an inherently safe FPC design.

PbLi: Thermal-hydraulic limitations require operation at reduced wall load or a dual media cooling system (see below).

H₂O/PbLi: This dual media design has been studied in earlier RFP designs [3,6] and offers the advantage of good first-wall cooling at high heat loads and good neutron energy recovery. Two entirely separate cooling loops are required and the reactivity between water and PbLi is a safety concern.

H₂O/K: In this dual media design, water is used to cool the first wall, but potassium is the blanket coolant. Lithium or lithium-bearing compounds, encased in metal cladding would breed tritium. The potassium would boil within the blanket and be used in a direct cycle, gas turbine. The reactivity of the water with potassium interaction is a safety concern.

TABLE 1.6.-I (continued)
FPC CONCEPTS CONSIDERED FOR TITAN

POOL-TYPE CONCEPTS

Liquid-Metal Pool: Similar concept to the French Phénix and Superphénix LMFBRs. Eddy currents produced in the pool will interfere with plasma transient operations and would require resistive baffles in the pool. Such a modification would also inhibit free coolant flow in the pool and reduce the attractiveness of this design.

FLiBe Pool: High-temperature and low-pressure operation are possible. The low electrical conductivity of FLiBe also reduces MHD and eddy current problems.

Water Pool: High pressure operation is necessary and, because of the size of the pool containment, a massive structure is required.

LOOP-IN-POOL CONCEPT

Aqueous Blanket: Lithium bearing salts (e.g., LiNO_2 , LiNO_3) are dissolved in water and used in a high pressure loop for cooling the FPC. The entire primary loop is submerged in a low pressure pool of pure water. The water pool acts as a heat sink in the event of a primary loop rupture. Beryllium is required to achieve adequate tritium breeding ratio, TBR.

phase, four FPC designs were selected for detailed engineering analysis. The final four designs are:

1. A self-cooled, lithium loop with a vanadium-alloy structure.
2. An aqueous, self-cooled design with a copper-alloy first wall, beryllium neutron multiplier and PCA structure.
3. A self-cooled FLiBe pool using a vanadium alloy structure.

4. A helium-cooled ceramic design using silicon carbide as the structure and a solid breeder.

1.6.1. Self-cooled Lithium Vanadium Design

The self-cooled, lithium loop is an adaptation of the BCSS [40] Li/Li/V design for a tokamak or tandem mirror first wall and blanket. BCSS rated this concept as the top prospect. The TITAN version of this design is illustrated in Fig. 1.6.-1 which shows the poloidal cross section and an isometric view. The isometric view shows a single quadrant of which four would be attached to form the torus. Between each quadrant would be a divertor occupying $\sim 10^\circ$ toroidally. The reactor design characteristics are given in Table 1.6.-II. The first wall of this design is made of 1.05 cm diameter tubes of the vanadium alloy, V-3Ti-1Si. The flow in these tube is poloidal, which is parallel to the stronger, poloidal field.

Because the flow is normal to the toroidal field ($B_\phi = 0.39T$), MHD effects must be considered. Analysis has shown that with high velocity lithium cooling, the first wall can handle surface heat fluxes in the range of 4 to 5 MW/m². The

TABLE 1.6.-II
OPERATING PARAMETERS FOR THE LITHIUM DESIGN

FIRST WALL

Description: Bank of lithium-cooled, seamless circular tubes; poloidal single pass flow; inter-tube welds for toroidal electrical circuit and to reduce fretting.

Structural material	V-3Ti-1Si
Tube o.d. (mm)	10.5
Tube i.d. (mm)	8.0
Erosion allowance (mm)	0.25
Design lifetime (full power year, FPY)	1.
Poloidal radius (m)	0.68
Number of tubes	2,440.
First wall area (m ²)	166.
Surface heat flux, peak (MW/m ²)	4.7
Volumetric heat generation (MW/m ³)	
- Lithium	76.
- Vanadium	107.
Inlet temperature (°C)	300.
Outlet temperature (°C)	393.
Mass flow rate (kg/s)	1,460.
Volume flow rate (m ³ /s)	3.08
Velocity, peak (m/s)	22.5
Pressure drop (MPa)	11.2

TABLE 1.6.-II (cont.)
OPERATING PARAMETERS FOR THE LITHIUM Design

BLANKET

Description: 4 rows of varying cross section, seamless tubes increased structural fraction with depth into blanket to maximize shield lifetime.

Structural material	V-3Ti-1Si
Tube o.d. (mm)	75.
Tube wall thickness (mm)	
- Row 1	2.75
- Row 2	3.25
- Row 3	4.00
- Row 4	4.50
Blanket thickness (m)	0.30
Volume fractions,	
- Lithium	0.64
- Vanadium	0.14
- Void	0.22
Volumetric heat generation, peak (MW/m ³)	
- Lithium	65.
- Vanadium	102.
Inlet temperature (°C)	300.
Outlet temperature (°C)	681.
Mass flow rate (kg/s)	667.
Volumetric flow rate (m ³ /s)	1.4
Velocity, peak (m/s)	0.3
Pressure drop (MPa)	1.2

SHIELD

Description: Lithium cooled, 2-piece hot shield; double-pass poloidal flow.

Structural material	V-3Ti-1Si
Moderator/absorber	HT-9
Volume fractions	
- Lithium	0.445
- Vanadium	0.044
- HT-9	0.511
Lifetime (dpa)	200.-250.
Damage rate, peak (dpa/FPY)	47.2
Inlet temperature (°C)	300.
Outlet temperature (°C)	681.
Mass flow rate (kg/s)	667.
Volumetric flow rate (m ³ /s)	1.4

Fig. 1.6.-1a. The poloidal cross section of the Lithium design.

Fig. 1.6.-1b. Isometric view of one quadrant of the Lithium design.

blanket design consists of a bank of vanadium pipes with poloidal lithium flow. The blanket pipes are stacked to form a ~ 30 cm thick blanket. The shield is also lithium cooled with a vanadium structure and HT-9 neutron moderator/reflector/absorber. The shield operates at a high temperature since ~ 40% of the thermal energy is deposited within it.

The neutronics performance of the Lithium design is quite good, with a tritium breeding ratio, $TBR > 1.2$ and a blanket energy multiplication ratio, $M \sim 1.2$. Further neutronics optimization will continue during the design phase and it is expected that the neutronics performance can be enhanced. Specific areas of optimization include; thinner blanket/shield, higher M and reduced rad-waste volume.

Cooling the first wall in a high power density device is a key issue in the FPC design. As previously stated, the lithium cooled first wall is viable with surface heating as high as 5 MW/m^2 . High velocity flow is required, leading to a small temperature rise in the first wall coolant. This small temperature rise is offset by a large ΔT in the blanket and shield coolant streams. The temperature of the mixed lithium is about 600°C . The gross thermal efficiency of the steam cycle is about 40%.

1.6.2. Aqueous Blanket Design

The aqueous "loop-in-pool" design is the extension of the design proposed by Steiner [41], in which a high pressure primary loop including the RFP torus and heat exchangers, operating at 15.8 MPa, is submerged in a pool of water at 0.1 MPa. The general arrangement of the reactor are shown in Fig. 1.6.-2. The design characteristics of this concept are given in Table 1.6.-III.

To breed tritium, a lithium compound (e.g., LiNO_2 or LiNO_3) is dissolved in the hot water loop. The water enters the bottom of the torus at 291°C and exits at the top at 326°C . A large number of lithium bearing salts were considered, but many resulted in solutions that were either too alkaline or had induced radioactivity problems. The nitrate and nitrite salts are the most promising, with a resulting pH of between 7 and 7.25 and a high solubility limit of between 4.5 and 6.4 a/o. High solubility is required to attain high TBR and even at the maximum lithium concentration a neutron multiplier is still required.

The water-cooled copper-alloy first wall is ideally suited for the high heat flux environment encountered in high power density devices. Subcooled flow boiling will adequately cool the first wall with 5 MW/m^2 heat flux ($f_{\text{RAD}} = 1$ for

Fig. 1.6.-2a. The aqueous loop-in-pool blanket design.

a. TOP VIEW, DISTRIBUTOR SCHEMATIC

b. SIDE VIEW, INBOARD / OUTBOARD MODULES

Fig. 1.6.-2b. The coolant flow paths in the aqueous loop-in-pool blanket design.

TABLE 1.6.-III
DESIGN CHARACTERISTICS OF THE AQUEOUS BLANKET DESIGN

Major radius (m)	3.9
Minor first wall radius (m)	0.65
Neutron wall loading (MW/m^2)	20.0
Surface heat loading (MW/m^2)	5.0
Thermal power (MW)	2,948.
Net electric power (MW)	1,000.
Tritium breeder	LiNO_2 or LiNO_3
Neutron multiplier	Be
Tritium breeding ratio	1.25
Blanket energy multiplication	1.39
Coolant	water at 15.8 MPa
Inlet temperature ($^{\circ}\text{C}$)	291.
Outlet temperature ($^{\circ}\text{C}$)	326.
First wall material	Cu-Al25
Structural material	PCA or HT-9
First wall thickness (mm)	1.5
First wall temperature, peak ($^{\circ}\text{C}$)	425.
Gross thermal efficiency	35%

neutron wall loading of $20 \text{ MW}/\text{m}^2$). The structural material in the first wall is a high strength copper alloy, Cu-25Al (Cu-0.25% Al_2O_3) and is mechanically attached to a PCA support structure within the blanket.

One of the attractive features of this FPC is the surrounding pool of low-pressure, low-temperature water. If a leak occurs anywhere in the primary loop, the release of tritiated steam will be instantly condensed by the cold pool. If the entire primary loop inventory of hot, tritiated water were mixed with the cold pool, the resulting pool temperature would be $\sim 50^{\circ}\text{C}$. Tritium containment will also be enhanced if the HTO and T_2O remain in the liquid state. Since the torus is submerged in the pool, the FPC will remain covered with coolant in the event of a primary pipe rupture, and will act as a heat sink for decay heat removal. This concept will be subject to detailed study in the design phase.

1.6.3. FLiBe Pool Blanket Design

The pool concept differs from the loop-in-pool concept in that no high-pressure loop is required. The concept is similar to the French Phénix and Superphénix fast (fission) breeder reactor designs in which the primary pumps and IHXs are all contained within the pool. The first wall/vacuum vessel assembly is supported in a low pressure pool of FLiBe [42]. Figure 1.6.-3 illustrates the FLiBe pool configured for the TITAN reactor. The minimum amount of structure between the plasma and the FLiBe enables the reactor to meet the

Fig. 1.6.-3. General layout of the FLiBe pool concept.

1-D neutronics requirement of $TBR = 1.2$. Because the FLiBe has a low electrical conductivity MHD or eddy-current effects are not expected.

The first-wall cooling is an inherent problem associated with the pool configuration, since it is difficult to force the coolant towards the first wall in such an open geometry. A baffled first wall can be used to enhance the first wall cooling. In such a configuration the FLiBe first-wall coolant flows through an array of orifices to allow preferential flow to the first wall. With such a configuration, the FLiBe-pool first wall could handle a surface heat flux of about 1.3 MW/m^2 , which corresponds to f_{RAD} of about 0.3 at a neutron wall loading of 20 MW/m^2 ; the divertors in this design are required to handle a significant heat load ($> 10 \text{ MW/m}^2$).

1.6.4. Helium-Cooled Ceramic Design (FISC)

The Fusion Inherently Safe Ceramic (FISC) design [43] uses only low-activation materials, which exhibit only a low level of short-lived activation. The unique idea of the FISC is to place the entire FPC and high-pressure helium primary heat-transport loop inside a prestressed concrete reactor vessel (PCRVR) filled with pressurized helium, as shown in Fig. 1.6.-4. Typical operating parameters for the FISC design are summarized in Table 1.6.-IV. This places the first wall torus under a compressive load. Furthermore, it places the entire primary loop under the same compressive load that balances the tensile load

TABLE 1.6.-IV
DESIGN CHARACTERISTICS OF FISC DESIGN

Major radius (m)	3.9
Minor first wall radius (m)	0.65
Neutron wall loading (MW/m^2)	20.0
Surface heat loading (MW/m^2)	1.5
Thermal power (MW)	2,351.
Net electric power (MW)	1,000.
Tritium breeder	Solid breeder
Neutron multiplier	BeO
Tritium breeding ratio	1.17
Blanket energy multiplication	1.20
Coolant	Helium at 5 MPa
Inlet temperature ($^{\circ}\text{C}$)	540.
Outlet temperature ($^{\circ}\text{C}$)	800.
First wall material	SiC
Structural material	SiC
First wall thickness (mm)	1.9 to 2.5
First wall temperature, peak ($^{\circ}\text{C}$)	1,198.
Power conversion system thermal efficiency	40%

Fig. 1.6.-4a. The TITAN helium-cooled ceramic (FISC) design.

Fig. 1.6.-4b. The ceramic first wall of the TITAN helium-cooled ceramic (FISC) design, which is compressively-loaded.

created by the high pressure, high temperature helium coolant. The result is a ceramic design with only compressive primary stresses.

A closed-cycle, gas turbine power conversion system is located inside the PCRV. High temperature operation is possible with a helium outlet temperature of 800 °C and the power conversion efficiency of about 40%. As with the FLiBe-pool design, there are limitations on first-wall cooling. The surface heat flux limit is roughly 1.5 MW/m^2 , corresponding to $f_{\text{RAD}} = 0.35$ at neutron wall loading of 20 MW/m^2 ; again the divertors in this design are required to handle a significant heat flux.

1.7. MAINTENANCE APPROACH

A potential advantage of high mass power density systems is the feasibility of single-piece maintenance for these systems and possible improvements in the plant availability. Specifically, single-piece FPC maintenance of a totally operational and pre-checked FPC may be possible above a power-density or below a total FPC mass threshold. Above this threshold, more than a single piece would be removed, ranging from few-piece maintenance to fully modular maintenance. The power density corresponding to the minimum-COE design (Table 1.3.-I) may shift once these issues are quantified into an availability model that is more elaborate than used so far. Single-piece maintenance of the FPC is expected to reduce maintenance time and risk and to increase reliability relative to the modular approach. The estimate of time-reduction is based on the elimination of component fit-up and sealing in an activated assembly. These operations can be performed on the replacement FPC in the shop prior to FPC replacement while the plant is generating power. The financial risk associated with remote operations is also reduced with the single-piece approach. Complex maintenance procedures can result in extended outages, particularly if FPC parts have been deformed, while the single-piece approach establishes a limit on the time required to recover from any failure (the time to replace an expended FPC in toto with one that has undergone full non-nuclear testing in conditions that can be more severe, other than radiation, than those encountered in actual nuclear service). An improvement in reliability is achieved by the complete assembly and testing of the FPC prior to installation. The combined improvements in reliability and maintainability can result in improved plant availability.

A major goal of the TITAN study is to quantify the expected availability advantage of the compact reactor approach using single-piece maintenance.

Scheduled maintenance for the balance of plant alone is estimated to require an annual shut-down of 25 days. Typical unplanned outage periods are 50-60 days/year for existing large U.S. plants and this value is also adopted by fusion reactor studies. For discussion, a typical plant with an aggressive availability goal is assumed to require 40 days/year for planned maintenance and 60 days/year for forced outages, resulting in overall availability of 73%. There is a considerable incentive to design a fusion system that can be maintained in an annual shutdown of 25 days, and this goal appears to be credible in the scoping phase for single-piece maintenance [3,6]. The reduction in unscheduled maintenance time, because of FPC pre-testing, and the upper limit on single failure downtime, cannot be quantified without the development of an integrated design and associated equipment specification. If a reduction from 60 to 40 days were achievable, corresponding to a scheduled outage reduction from 40 to 25 days, then the availability would increase from 73% to 82%. This reduction in availability of approximately 10% is a major motivation for further development of the single-piece maintenance approach.

Single-piece maintenance requires that the size and mass of the replaceable unit allow routine transport within the reactor cell and maintenance areas. The heaviest single piece considered in conceptual tokamak designs is the TF coil, and reactor cell crane capacities of 600 tonnes are specified by STARFIRE [9]. This value is several times the capacity of standard cranes, but the larger cranes can be supplied at a cost of about 5 M\$. An upper limit on crane capacity will be determined by economic trade studies, considering the building space and structural requirements, as well as the crane cost. The trade studies must also consider special horizontal transporters for the heavier components, and lifts on the order of 1000 tonnes can be performed with gantry cranes. For guidance during the scoping phase, the mass at which single-piece maintenance may become unattractive is assumed to be about 500 tonnes.

A general plant arrangement was developed that takes advantage of the simplicity of the single-piece maintenance approach. A central reactor building containing an enclosed reactor cell connects the shop area at one end to the hot cells and waste processing areas at the other. A straight-through process is envisioned for the FPC replacement, in which the expended FPC is taken to the hot cell for disassembly, and the complete new FPC is brought in from the shop. The intent is to minimize the operations that require the re-assembly of activated or contaminated parts. This approach is expected to simplify the

remote maintenance equipment, since the only operations required in the reactor cell deal with making and breaking of external connections.

The TITAN study scoping phase considered a broad range of blanket and coolant options, rather than aiming at a complete integrated design. The general issues of the definition of the FPC compatible with the maintenance approach and the PF coil structural design were addressed during this phase. The PF coils of the RFP are massive, designed for the life of the plant, and not connected to the rest of the FPC, so they are not considered to be a part of the replaceable FPC. The TITAN study has considered configurations using an "open" PF coil set, but some of the OH coils must be removed or relocated before the replaceable FPC can be removed (Fig. 1.3.-2).

In the CRFPR study [3,6], the replaceable FPC weight (first wall, blanket, shield and TFCs, but not PFCs) is 300 tonnes and is removed as a unit. The blanket breeder, coolant, and part shield is PbLi. The PbLi blanket, which is unique in its large drainable mass and good reflecting/shielding properties, was not selected as an option in the TITAN scoping phase. The preliminary shield specified for the lithium blanket option weighs about 400 tonnes more than that of the PbLi design. The total removable FPC mass (not including PFCs) is greater than 600 tonnes; separation of at least part of the shield from the rest of the FPC may be preferred to single-piece removal. For designs with a lower wall loading, the FPC weight can become so large (e.g., FPC weights over 1000 tonnes for 10 MW/m^2 case) that partitioning of the shield must be considered. The split-shield design would be simplified if the integrated blanket coil concept is used, so that separate TF coils do not need to be removed to gain access to the shield. Detailed design of the service connections and of the structural supports will be required to determine whether the advantages of single-piece maintenance can be retained with a split-shield design.

1.8. CONCLUSIONS OF THE SCOPING PHASE

During the scoping phase, the TITAN design team has succeeded in its interim objectives: to define the parameter space for a high mass power density (MPD) RFP reactor; to explore a variety of approaches to the design of major subsystems; to narrow to two major design approaches consistent with high MPD and low COE; and to reach an intermediate stage which includes preliminary engineering design and integration. The program has retained a balance in its

approach to investigating high MPD systems. On the one hand, parametric investigations of both subsystems and overall system performance are performed. On the other hand, more detailed analysis and engineering design and integration are performed, appropriate to determining the technical feasibility of the high MPD approach to RFP fusion reactors. Because of this balance between parametric system studies and detailed subsystem design, we have come to refer to the TITAN effort as a "PARAPOINT" study.

Detailed technical conclusions are given in individual sections. The physics issues for compact RFP reactors are discussed separately in Sec. 4.8. Major technical results at this interim stage can be summarized as follows:

1. Parametric systems studies continue to suggest a shallow minimum in cost of electricity (COE) versus neutron wall loading, extending from about 10 MW/m² to 20 MW/m² with the minimum COE at 18 MW/m². Reversed-field pinch reactors in this range have MPD values well in excess of 100 kWe/tonne. The TITAN reference design at a neutron wall loading of 18 MW/m² has a MPD of 640 kWe/tonne.
2. Reversed-field pinch systems with high MPD at 15-20 MW/m² neutron wall loading are physically compact systems. The cost of the FPC is a small fraction of plant cost (< 10%), which means that small units can be used to minimize the cost of a development program.
3. Single-piece maintenance of the entire reactor torus (first wall, blanket, divertor sections, with or without the shield) is feasible for high MPD systems. At 18 MW/m² neutron wall loading, the entire reactor torus, drained of coolant, can be vertically lifted with a crane and replaced with a complete and pre-tested unit with a minimum amount of down time and start-up time. The full impact of single-piece maintenance and the ability to pre-test the entire reactor torus as a unit on reliability and availability is not yet determined. The shallow minimum in COE largely results from the assumption that the availability is not a strong function of neutron wall load, wall lifetime, and of the maintenance concept, at least at the level of single-piece versus modular approach to design and testing.

4. Reversed-field pinch experiments appear to operate well even when the dominant core plasma loss mechanism is radiation rather than conductive energy transport. This is particularly advantageous for high wall loading systems, as it distributes the plasma energy loss uniformly on the walls. For TITAN, this approach has been adopted, along with four toroidal field divertors as the particle removal system.
5. The dominance of the poloidal field and the relatively high beta make the RFP particularly well suited to liquid-metal cooling. One design approach being pursued uses liquid lithium as coolant and breeder, and vanadium alloy (V-3Ti-1Si) as the structural material. The first-wall, blanket, shield, and divertor cooling are accomplished using lithium throughout this design. No other coolant is needed except for the magnets. This simplifies system integration and design. At 18 MW/m^2 neutron wall loading, the fluid pressure in the first-wall tubes is estimated to be about 10 MPa, but this level is reasonable since the stresses and pumping power requirements associated with this high pressure are modest. The coolant pressure in the blanket is much lower at about two MPa.
6. The integrated blanket-coil concept (IBC) is significantly better suited to the RFP than to the tokamak concept. This is largely due to the lower value of the magnetic field that the coil must produce. The IBC is especially advantageous, perhaps uniquely so, for use as the main divertor field coil in an RFP. The IBC can also be used to generate the toroidal field. In TITAN, the IBC has been adopted for both divertor and TF coils, in order to examine this innovation in depth. In the former case, it truly improves the RFP as a reactor. In the case of IBC as a TF coil, the advantages over a copper TF coil system are less clear. Since the copper TF coil approach appears certain to work, the TITAN study has also chosen to pursue the TF IBC approach.
7. The aqueous loop-in-pool blanket has emerged as an alternative for high-MPD RFP systems. This design incorporates a water-cooled copper first wall and steel structural material for blanket and shield. The cooling is achieved with a loop design, while the FPC as a whole is submerged in a low-pressure water pool to achieve a high level of passive safety. Tritium breeding is achieved using a lithium salt dissolved in the water, while controlling the

pH of the solution to minimize corrosion. Work on this design has been at a less advanced state within the scoping phase, but detailed analysis of this design will commence during the design phase, upon completion of the lithium design.

1.9. DIRECTIONS FOR THE DESIGN PHASE

During the scoping phase of the TITAN study a large number of design concepts and options were considered. Of particular importance are the four blanket concepts reported in Sec. 8. The number of FPC designs to be pursued during the design phase was narrowed to two. This decision was necessary because of inadequate resources to pursue all four designs. The selection of the two concepts to pursue was difficult to make. All four concepts have attractive features. The lithium-loop design promises excellent thermal performance and is one of the main concepts being pursued by the U.S. blanket technology program. The water design promises excellent safety features and use of more developed technologies. The helium-cooled ceramic design offers inherent safety and excellent thermal performance. The molten-salt pool design is the only low-pressure blanket and promises passive safety. In the end, the lithium-loop concept and the aqueous loop-in-pool concept were chosen for detailed conceptual design and evaluation in the design phase of the TITAN study. The choice was made primarily on the capability of each concept to operate at high neutron wall load and high surface heat flux. The choice not to pursue the helium-ceramic and molten-salt designs should in no way denigrate these concepts, since each offers high performance and attractive features when used at lower wall loads; these concepts should be pursued in other design studies.

In the design phase, therefore, the TITAN study will emphasize engineering design and complete technical evaluation of the high-MPD approaches based first on the lithium-loop system and then on the aqueous loop-in-pool concept. Approximately half of the duration of the design phase will be devoted to complete the Li/V design, devoting essentially the full resources of the program. Major efforts will be made to provide the technical material needed to establish engineering feasibility and the design integration. In addition, safety and environmental tasks will receive special attention, and work on the plasma modeling, first wall design, and divertor system will continue. The area

of high-heat-flux components is the most difficult physics-engineering interface.

Once the Li/V design has been brought to a reasonable completion, the TITAN team will concentrate on establishing the feasibility and examining key issues of the aqueous blanket design. All of the major subsystem design and analysis will be addressed along with the assessment of safety and environmental impact. Our philosophy is to establish the technical feasibility and key issues for high-MPD RFP reactors, and having more than one design approach strengthens the case.

Finally, parametric studies will continue so one can better understand the changes in system design in going to lower wall loadings (e.g., about 10 to 12 MW/m²), and in using high-MPD RFP systems in a development program.

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2. PROGRAM OBJECTIVES

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2. PROGRAM OBJECTIVES

2.1. INTRODUCTION

Fusion reactor conceptual design has become a mature research field and results from system studies research have greatly influenced the direction of the physics and technology elements of the fusion energy program. The reactor studies during 1970's were focused on central power stations with electric power outputs of 1000 to 2000 MWe. These designs were usually based on superconducting magnets to minimize the recirculating power. They shared basic disadvantages of large stored magnetic energy and fusion power cores (FPC) which were very large in volume and heavy in mass (FPC comprising of first wall, blanket, shield, magnets, plasma support systems, vacuum vessel, and related structures). These resulted in systems with large total power output, high direct capital cost, and low power density. These designs raised the perception that fusion power, if feasible, would only come in units of large size and low power density and as a result would be quite expensive.

More recent reactor studies now seek ways to use the past experience and move toward a more affordable, competitive, and "attractive" fusion reactor. One of the approaches to the new generation of reactor design is the compact reactor option [1-3]. The main feature of a compact reactor is a FPC with a high mass power density (MPD). MPD is defined as the ratio of the net generated electric power to the total mass of the FPC (in units of kWe/tonne). The increase in mass power density is achieved by increasing the plasma power density and neutron wall loading, by reducing the size and mass of the FPC using a thin blanket and resistive magnet coils, and by increasing the blanket energy multiplication ratio.

Even though compact designs push toward very high mass-power-density regimes, increasing realism in conceptual reactor design and costing has moved even the "conventional" designs toward smaller FPCs and higher mass-power-densities. As an example, one might begin in 1974 with UWMAK-I [4] at 20 kWe/tonne to STARFIRE [5] in 1980 and MARS [6] at 50 kWe/tonne to GENEROMAK [3] at 100-200 kWe/tonne and compact reversed field pinch reactor, CRFPR [2] at 800 kWe/tonne. The mass power density and FPC power density of several of these and other conceptual fusion reactor designs, together with fission PWRs, are shown in Fig. 2.1.-1 [1]. A compact reactor thus strives toward a system with FPCs comparable in mass and volume to the heat sources of alternative fission power

Fig. 2.1.-1. The mass power density and FPC power density of several conceptual fusion reactors and fission PWR [1].

plants with mass power densities in the range 500-1000 kWe/tonne. These arguments have recently prompted the suggestion that a mass power density of 100 kWe/tonne be a threshold goal for fusion reactor design [1].

There are other potential benefits for compact systems. In addition to improved economics, the FPC cost in a compact reactor is a small portion of the plant cost, and therefore, the economics of the reactor would be less sensitive to large changes in the unit cost of FPC components or the plasma performance. Moreover, since a high mass-power-density FPC is smaller and cheaper, a rapid development program at lower cost is possible, changes in FPC design would not introduce large cost penalties, and the economics of learning curves can be exploited.

Mass power density however, is only one general measure of the potential economic competitiveness of a fusion reactor. Other factors should also be considered in the search for an optimum and "attractive" fusion reactor. One can summarize the general features of an "attractive" fusion reactor as:

1. Potential for a range of power output. Reduced net power output and associated lower capital investment (investment at risk) not only makes the plant more attractive, it can also permit an affordable development pathway to bring the fusion option to commercial fruition.
2. Affordable and competitive total cost, unit direct cost (\$/kWe), and cost of electricity (mills/kWh). This can be achieved by:
 - A. increasing mass power density,
 - B. increasing overall plant efficiency,
 - C. reducing or combining the functions of reactor subsystems and plasma support technologies.
3. Simplified overall FPC design.
4. Reduced engineering constraints (e.g., magnetic fields, stresses, magnetic stored energy), simple subsystem design (e.g., large duct blanket, single coolant), and combined subsystem functions (e.g., integrated blanket/coil) can lead to safe and reliable operation, reduce the forced outages, allow eased and rapid maintenance, and as a whole can drastically increase the plant availability.
5. Built-in enhanced safety and environmental features. This reduces the use of safety-specific systems and reduced probability of accidents with either serious public health or capital cost consequences.
6. Reduced rad-waste disposal requirements. Use of low-activation

materials reduces the quantity and quality of radioactive waste and eases the long term waste disposal issues.

It should be emphasized that some of these goals and features of an "attractive" fusion reactor may not be achievable simultaneously, and trade-offs are required. The effect of these trade-offs can be assessed only through specific and detailed design.

2.2. OBJECTIVES

The TITAN program is a multi-institutional effort to determine the potential of the Reversed Field Pinch (RFP) magnetic fusion concept as a compact, high-power-density, and "attractive" fusion energy system. The primary program objectives are:

1. Determine the technical feasibility and key developmental issues of an RFP fusion reactor, especially at high power density.
2. Determine the potential economics, cost of electricity, safety and environmental features of such a high power density RFP reactor.

Auxiliary objectives are:

1. Establish the major technical features of an RFP reactor.
2. Develop detailed conceptual designs for the major subsystems and components.
3. Assess the degree of extrapolation between the present data base in RFP physics and in technology and the physics/technology requirements of an RFP reactor.
4. Determine the technical features and parameters of RFP devices required at key steps in a development program.
5. Develop innovative design approaches for a high mass-power-density fusion system.

The RFP has inherent characteristics which allow it to operate at very high mass power densities. This potential is available because the main confining field in a RFP is the poloidal field, which is generated by the large toroidal current flowing in the plasma. This feature results in low field at the

external magnet coils, high plasma beta, and very high engineering beta (defined as the ratio of the plasma pressure to the square of the magnetic field strength at the coils) as compared to other confinement schemes. Furthermore, sufficiently low magnetic fields at the external coils permits the use of normal coils while Joule losses remain a small fraction of the plant output. This option can permit a thinner blanket/shield. In addition, high current density in the plasma allows ohmic heating to ignition, eliminating the need for auxiliary heating equipment. Also, RFPs promise the possibility of efficient current drive systems based on low frequency oscillation of poloidal and toroidal voltages and the RFP relaxed states theory. Finally, RFP confinement concept allows arbitrary aspect ratio, and the circular cross section of plasma eliminates the need for plasma shaping coils.

These inherent characteristics of the RFP allow it to meet, and actually far exceed, the threshold value of 100 kWe/tonne. As a result, the TITAN study also seeks to find potentially significant benefits or drawbacks that can be obtained by operating well above the mass-power-density threshold of 100 kWe/tonne. Therefore, the program has chosen a high neutron wall loading as the reference case in order to force the issue of engineering practicality at high mass power density. However, the program has put strong emphasis on safety and environmental features and maintainability, reliability, and availability issues. These features and constraints are incorporated into the FPC design from the beginning.

2.3. SAFETY AND ENVIRONMENTAL ASPECTS

The TITAN study is aiming towards a fusion reactor with four major features: minimum cost of electricity (COE), high availability, design simplicity, and improved safety. These goals and features may not be achieved simultaneously and trade-offs are required. For example, if add-on safety equipment are needed, the design can become more complex and have lower availability and higher cost. On the other hand, if the safety features are incorporated in the design from the beginning, the reactor can potentially be inherently safe, simpler, and have higher availability and lower cost. The TITAN study, therefore, has designated the safety as an integral part of the design activity with the safety features built in the design from the beginning.

In order to define a clear direction for the TITAN design approach in terms

of improved safety, we have adopted the following definition of levels of safety assurance, suggested by S. Piet of EG&G [7] and adopted by ESECOM [8]:

- Level 1. "Inherent Safety". The public is protected from prompt or early fatalities even under incredible events. The approach to this level can be by controlling the radiological hazard inventory and/or by controlling energy sources existing to disperse the inventory even under incredible events.
- Level 2. "Large-Scale Passive Protection". This approach relies only on natural properties (e.g., natural circulation) and the integrity of large scale components (e.g., containment building integrity, the pool for pool designs). The public is adequately protected even if some severe phenomenon breaks all the cooling pipes and other small scale components.
- Level 3. "Small-Scale Passive Protection". This approach relies on natural properties (e.g., natural circulation) if the small-scale geometry of the reactor is maintained. Seismic qualification will be needed to ensure that such behavior as natural circulation continues as planned in the event of an accident.
- Level 4. "Active Safety". Active engineered systems must function to prevent off-site prompt or early fatalities in the event of a major accident. Safety analysis, testing, and qualification of safety equipment is needed to ensure the safety of public is adequately protected at all times.

Based on the above discussion, the TITAN study focuses on design concepts that can achieve passive safety, aiming at the level 2 of safety assurance (large-scale passive safety).

For the treatment of radioactive waste, TITAN aims at design concepts with 10CFR61 Class-C waste rating (i.e., qualified for shallow land burial). For a Class-C rating, the dose delivered to an individual inadvertently constructing a house on the waste-disposal site and living there continuously starting at any time more than 100 years after the wastes were generated (the "intruder dose") must never exceed 500 mrem/yr. The intruder dose includes contributions from the consumption of vegetables, meat, and milk produced on the site.

2.4. MAINTENANCE APPROACH AND AVAILABILITY

An important potential benefit of operating at very high mass power density is the possibility of single-piece maintenance scheme. In such a maintenance scheme, the FPC is replaced as a single torus including the plasma chamber, first wall, blanket, shield, and toroidal field coils. The potential benefits of such a replacement scheme as compared to a more "conventional" modular approach are:

1. The reactor torus is made of a few factory-fabricated pieces that are assembled on-site, in a non-nuclear environment, and into a fully operational unit.
2. FPC can undergo full operational, non-nuclear testing before installation in the reactor building.
3. The number of connections that must be made or broken in the nuclear environment is minimized.
4. The scheduled maintenance period is shortened because of reduced replacement time and shorter restart period with increased confidence level.
5. The recovery procedure from unscheduled events is more rapid and standard with the replacement of the entire reactor torus with a stand-by pretested torus.
6. This approach can also accommodate FPC improvement throughout the plant life and allows full benefit from learning curves economics.

These potential benefits of the single-piece maintenance approach should ultimately translate into an increase in plant availability and directly improve the economics of the plant. The TITAN program seeks to quantify and demonstrate these potential benefits.

2.5. PROGRAM APPROACH

To achieve the design objectives of the TITAN study, the program was divided into two phases, each roughly one year in length: the Scoping Phase and the Design Phase. The objectives of the scoping phase were: to define the parameter space for a high-MPD RFP reactor; to explore a variety of approaches of major subsystems; to select at most two major design approaches consistent

with high MPD; and to reach an intermediate stage which includes preliminary engineering design and integration. The two major approaches identified during the scoping phase would then be the subject of more detailed and in-depth analysis during the design phase.

The first half of the scoping phase was devoted to wide-range scoping studies of a large variety of different design concepts. The purpose of this period was to "let a thousand flowers bloom," that is to encourage creativity and the generation of new ideas. The guidelines followed were to find concepts that held the potential to form the basis for an attractive compact RFP reactor. Those ideas and concepts that seemed promising were selected for more detailed analysis during the latter part of the scoping phase. The impact of various design options were routinely evaluated and analyzed through system studies. At the end of the scoping phase, good preconceptual design definitions were in hand for reactor subsystems to initiate the design phase.

Section 3 discusses the scoping phase activities. Detailed technical analysis and results of plasma engineering, system analysis, divertors, reactor engineering, and FPC engineering efforts are given respectively in Sec. 4 through 8. Finally, Sec. 9 contains a summary of results of the scoping phase and directions for the design phase activities.

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3. SCOPING PHASE ACTIVITIES

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3. SCOPING PHASE ACTIVITIES

3.1. INTRODUCTION

The TITAN study is striving towards a fusion reactor with four major features: minimum cost of electricity (COE), high availability, design simplicity, and improved safety and environmental features. To achieve these design objectives, the program was divided into two phases, each roughly one year in length: the Scoping Phase and the Design Phase. The objectives of the scoping phase were: to define the parameter space for a high mass power density (MPD) RFP reactor; to explore a variety of approaches of major subsystems; to select at most two major design approaches consistent with high MPD; and to reach an intermediate stage which includes preliminary engineering design and integration. The major two approaches identified during the scoping phase then would be the subject of more detailed and in-depth analysis during the design phase.

The first half of the scoping phase was devoted to wide-ranging scoping studies of a large variety of different design concepts. The purpose of this period was to "let a thousand flowers bloom," that is, to encourage creativity and the generation of new ideas. The guidelines followed were to find concepts that held the potential to form the basis for an attractive compact RFP reactor. During this period, the TITAN design team members were encouraged to participate in "brain-storming" and a very large number of ideas were put forward. For example, twelve different fusion power core (FPC) designs were proposed (Sec. 3.2), different magnetics approaches were considered, and various options for impurity control and current drive systems were studied.

Those ideas and concepts that seemed promising were selected for more detailed analysis during the latter part of the scoping phase. For example, of the twelve proposed FPC designs, four were selected for further study. These four were the lithium loop with integrated-blanket-coil (IBC) design, the aqueous blanket, the helium-cooled ceramic reactor, and the molten salt pool approach. Simultaneously, scoping phase activities were carried out to analyze the plasma fusion core, to design plasma support technologies (e.g., magnets, impurity control system), and to investigate reactor engineering issues such as power conversion systems, maintainability, and availability. The impact of various design options were routinely evaluated and analyzed through system studies. At the end of the scoping phase, preconceptual design definitions were

sufficient for major reactor subsystems to initiate the design phase. Detailed technical analysis and results are given in the Sec. 4 through 8 of this report.

Of particular importance are the FPC design concepts for high power density systems that were proposed during the scoping phase. These concepts are summarized in the following sections.

3.2. FPC CONCEPTS

During the first half of the Scoping Phase, the TITAN design team members were encouraged to participate in the "concept brainstorming," and a very large number of ideas were put forward. Several of these were sufficiently attractive to warrant consideration as a distinct scoping study FPC design concept. These design concepts can be loosely categorized by the general FPC design as:

1. Loop-type,
2. Pool-type,
3. Loop-in-pool.

Major features of these concepts are presented in the following subsection.

3.2.1. Loop-Type Concepts

The coolant in these concepts flows in "loops" around the plasma chambers. The major feature of these designs is the capability to efficiently remove the thermal power.

3.2.1.1. Lithium Coolant

The Blanket Comparison and Selection Study (BCSS) [1] identified the combination of liquid-lithium coolant and breeder with vanadium-alloy structure as the most attractive blanket concept for both tokamaks and tandem mirrors. The functions of the coolant and breeder are combined in this design. To achieve a high thermal efficiency, a Li/Li/V blanket is designed such that the primary coolant outlet temperature at the heat exchanger is in the neighborhood of 550°C. Because of the favorable heat-transfer characteristics of liquid lithium, high neutron wall loadings can be effectively handled in such a design. Several design variations were considered for TITAN.

Liquid-Lithium Loop. This approach is a straightforward adaptation of the BCSS Li/Li/V concept to the RFP. This concept offers the promise of high

neutron wall load, good efficiency and a simple configuration. Lithium flow parallel to the dominant poloidal magnetic field eases the concerns of MHD effects compared with tokamak designs. Good tritium breeding, control and recovery are expected. A variant of this concept, the liquid lithium IBC loop, was selected for more detailed evaluation.

Liquid-Lithium IBC Loop. The Integrated Blanket Coil (IBC) concept proposed by Steiner [2] matches well with the liquid-lithium loop design. By simply flowing current through the lithium in the blanket loop, the blanket can serve the function of the toroidal-field (TF) coils. This combination simplifies the reactor by eliminating a separate TF coil and also recovers the coil ohmic power for conversion to electricity. This concept was selected as one of the four concepts for more thorough evaluation during the scoping phase, and is described in Sec. 8.2.

Beryllium-Lithium-Tritium Zone (BLiTz). A variant of the IBC concept is called BLiTz [3]. In this concept, the toroidal (and poloidal) field coils could be of a fairly conventional solid copper conductor construction, but would be cooled by liquid lithium. Beryllium could be added to the coil, possibly even replacing the copper completely, to achieve adequate tritium breeding. This approach offers lower coil resistance and power consumption and better tritium breeding than the lithium IBC. Preliminary investigation of this concept for TITAN indicated that the overall performance was about the same as the liquid-lithium IBC loop design. The additional complication and materials uncertainty of the BLiTz concept were not warranted, and this approach was not considered further.

3.2.1.2. Lithium-Lead Loop

Lithium-lead alloy ($\text{Pb}_{83}\text{Li}_{17}$) is much less chemically reactive than lithium and offers excellent neutron and energy multiplication for good tritium breeding and energy performance. The self-cooled lithium-lead blanket was rated second blanket for tandem mirrors in the BCSS [1], and was selected for the blanket (but not first wall) of the Compact Reversed Field Pinch Reactor (CRFPR) design [4]. However, the large MHD pressure drops and modest heat transfer prevented the use of PbLi to cool the TITAN first wall at high neutron wall loads (20 MW/m^2). To avoid the complexity of different coolants for the blanket and first wall, and the safety concerns of water and liquid metals in proximity, the PbLi blankets were not pursued for TITAN.

3.2.1.3. Helium-Gas-Cooled Direct Cycle

This concept relies on a high temperature, low-activation structural material (e.g., SiC or carbon/carbon composites) in the first wall and blanket. The first wall and blanket are cooled by a high pressure, high-temperature helium-gas cooling medium. The objective of this design is to achieve good thermal efficiency in a direct gas cycle, thus eliminating the need for an intermediate heat exchanger (IHX). Helium cooling, therefore, offers a number of advantages for fusion and several helium-cooled blankets scored well in the BCSS.

Several "conventional" helium-cooled high temperature solid breeder blankets were considered. In addition, an innovative design concept emerged that uses a helium-cooled high-temperature solid breeder blanket with all ceramic structure. This approach would achieve a truly low activation design with excellent safety characteristics. It appears this concept would meet level 1 of safety assurance (i.e., inherent safety). This concept was called the Fusion Inherently Safe Ceramic (FISC) Reactor, was evaluated during the TITAN scoping phase, and is described in Sec. 8.5.

3.2.1.4. Water Loop

Many previous reactor designs have selected water cooling. Water has the benefit of an extensive technology base, derived from the fission LWR industry and is the coolant best suited to high-heat-flux conditions. A disadvantage is the modest thermal efficiencies (30-35%) that are accessible using water because of pressure limitations. Two advanced dual-media system were considered in order to increase the efficiency of the water design.

Dual Media. The idea of using water coolant with another working fluid as a topping or bottoming cycle was considered as a possible means to improve the efficiency. Freon or ammonia can be used as a bottoming cycle to squeeze a few more points of efficiency from a water system, but the extra capital cost of the bottoming cycle is not justified; COE actually increases. A major limitation of this cycle is best demonstrated by the shape of the thermodynamic cycle (T-S) diagram. A condensing bottom cycle fits better with a cycle like the gas turbine (Rankine cycle) that rejects heat over a range of temperature than a condensing water cycle.

Binary-Vapor-Cycle Loop. In this design, water is used in a separately cooled first wall. In the blanket, a lithium or lithium-bearing compound is encased in metal cladding for tritium breeding. Potassium coolant is used as a

heat transfer medium. The potassium coolant leaves the blanket in the vapor phase and goes directly to a gas turbine, thus eliminating the need for an IHX and also reducing the MHD pressure drop. The water provides heat for the standard Rankine cycle, and the potassium vapor provides the working fluid for the topping part of the binary cycle. High thermal efficiencies (42%) can be achieved in this binary cycle. Major disadvantages are the complexity of the combined K/H₂O system and the potential safety concerns from using liquid metal and water in the blanket, which together led to the rejection of this concept.

3.2.2. Pool-Type Concepts

In the pool-type configuration, the plasma chamber and the first wall are submerged in a pool of coolant. This configuration is simple since the pool acts as a replenishable blanket and shield. Furthermore, the pool design promises the potential for inherent safety due to the high heat capacity of the pool. The major disadvantage of the pool concepts is the difficult and uncertain coolant flow configuration, which limits the heat-flux capabilities of the first wall. Several pool-type concepts were evaluated for TITAN design.

3.2.2.1. Liquid-Metal Pool

This concept is fashioned after the liquid-metal pool design of the Phénix and Superphénix French reactors and shows all of the potential benefits of the pool-type concepts. However, the eddy currents induced in the electrically-conducting coolant in the pool interferes with the plasma transient operations (e.g., startup, oscillating-field current-drive). Although resistive baffles can be used in the pool to reduce the eddy currents, such a modification would also inhibit free coolant flow in the pool and would significantly negate the advantageous features of a pool-type design.

3.2.2.2. Molten-Salt Pool

Molten lithium salts offer the potential for a self-cooled blanket that can operate at high temperature and low pressure. They are non-conducting and thus avoid MHD pressure drop concerns. Mixtures of LiF and BeF₂ ("FLiBe") have been used for several fusion reactor designs. The FPC is submerged in a pool of FLiBe. The FLiBe will fill the space between the first wall and magnets and will, therefore, provide adequate magnet protection. A FLiBe-to-FLiBe intermediate heat exchanger (IHX) is also located in the pool. The IHX is needed for reasons of safety and tritium containment. Since the working fluid

is FLiBe on both sides of the IHX, the IHX can continue to operate with small leaks. If pumps are also submerged in the pool to generate a FLiBe flow upward around the first wall and downward through the IHX for the purpose of heat transport, the need for a primary loop is eliminated. The molten salt pool concept was selected for evaluation and is described in Sec. 8.4.

3.2.2.3. Water Pool

The water-pool concept is the synthesis of several attractive ideas. Steiner proposed the use of an aqueous solution of a lithium compound as the breeder and coolant [5]. The pool reactor concept has emerged as an option with potential for passive safety. The TITAN program investigated combining these to obtain an aqueous solution pool. The initial idea was to place the entire reactor within a high pressure pool, with the high-temperature lithium-solution coolant/breeder separated from the low-temperature pure water pool by an insulated but non-pressure-bearing flow loop. The major disadvantage of this concept was the structural requirements of the high-pressure water pool. To reduce this requirement, the design evolved into a "loop-in-pool" design with a high-pressure, high-temperature loop located within a low-pressure, low-temperature pool (Sec. 3.2.3 and Sec. 8.3).

3.2.3. Loop-in-Pool Concept

The need for a pool is dictated by the desire to mitigate the effects of severe accidents (i.e., loss-of-coolant accident, LOCA, and loss-of-flow accident, LOFA). However, the use of a coolant pool is limited by the maximum heat flux that can be handled by the first wall. The working fluid must be pumped at high velocity (hence, high pressure) near the first wall. This approach has severe limits on the maximum first-wall surface heat flux as compared with a loop-type design.

One viable approach is to combine the desirable features of both pool-type and loop-type concepts into one design, leading to the aqueous-solution blanket design. In this approach, an aqueous solution of lithium nitrite is used as the coolant and breeder medium. The aqueous lithium salt blanket allows convenient external control of the tritium in the blanket. During the initial system-commissioning phase, pure water could be used to allow plasma and heat transport system testing without the complicating effects of tritium production. The salt could then be gradually added to achieve the desired operating levels of tritium production. This concept promises good safety characteristics, simple design

features, and capability for high power density. The aqueous-solution design was selected for more detailed evaluation and described in Sec. 8.3.

3.3. FPC CONCEPT EVALUATION

During the first half of the scoping phase of the TITAN study, several design concepts were considered which were listed in the previous sections. Of those designs, four were selected for more detailed evaluation. These four concepts were those that appeared to offer the greatest promise to achieve the program goals in an optimum way. These four are the lithium-loop IBC design, the aqueous-solution pool concept, the helium-cooled ceramic reactor, and the molten salt pool design.

More detailed designs were then undertaken for each of the four concepts. Self-consistent mechanical configurations were laid out, and analysis was performed to determine thermal and structural design windows for each concept. The maximum neutron wall load and first-wall surface heat flux was determined for each design approach. The tritium breeding and energy multiplication characteristics were determined and optimized. The thermal efficiency and recirculating-power requirements were determined. Critical issues were identified for each concept. At the end of the scoping phase, a good preconceptual design definition was available for each of the four concepts. The results of this evaluation are summarized in Sec. 8.

At the end of the scoping phase, a program consensus was reached to narrow the number of designs to be pursued during the design phase of the study to two. The decision was necessary because of inadequate resources to pursue all four designs. The decisions on which of the two concepts to pursue were very difficult to make, since all four concepts have attractive features. The lithium-loop design promises excellent thermal performance and is one of the main concepts being developed by the US/DOE blanket technology program. The water design promises excellent safety features and use of more developed technologies. The helium-cooled ceramic design offers inherent safety and excellent thermal performance. The molten-salt pool design is the only low pressure blanket and promises passive safety.

Ultimately, the IBC lithium-loop and the aqueous loop-in-pool concepts were selected for detailed conceptual design and evaluation in the design phase of the TITAN program. The choice was made primarily on the capability to operate at high neutron wall load and high surface heat flux. The lithium-loop and

aqueous loop-in-pool designs appear able to operate at a neutron wall loading of 20 MW/m^2 and a surface heat flux of 5 MW/m^2 . The helium-ceramic and molten-salt designs appear to be limited to a maximum first-wall heat flux of no more than 1.5 to 2 MW/m^2 corresponding to a neutron wall load of 7 MW/m^2 at f_{RAD} of unity (i.e., all of the alpha power is radiated and deposited uniformly on the first wall). While this latter value is a significant power density, a major element of the charter of the TITAN program is to investigate engineering approaches to and the technical feasibility of high-power-density fusion reactor designs. Therefore, the choice not to pursue the helium-ceramic design and molten-salt designs should in no way denigrate these concepts. These concepts offer high performance and attractive features when used at lower wall loads. The TITAN program recommends that these concepts be pursued in other design studies.

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4. PLASMA PHYSICS AND ENGINEERING

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4. PLASMA PHYSICS AND ENGINEERING

4.1. INTRODUCTION

The objectives of the fusion-core physics effort is to identify and study fusion-core plasma physics parameters of the TITAN RFP reactor, to develop operational scenarios (e.g., start-up, burn), to analyze the fusion-core plasma behavior and performance (e.g., equilibrium and stability, transport), and to study key plasma support subsystems (e.g., current drive, fueling, impurity control and particle exhaust system). As a whole, the fusion-core physics activity provides a detailed description of the fusion core for all engineering activities and design efforts. Feedback is also provided to the system analysis activity to improve parametric system models which are then used to generate new, cost-optimized "strawman" designs for further conceptual engineering design.

This activity starts with the TITAN "strawman" designs, given in Table 4.1.-I, which are provided by the parametric system analysis (Sec. 5). Then, magnetic calculations produce a realistic design for magnet coil sets needed for confinement, equilibrium, and start-up of the fusion core. Also, fusion-core plasma/circuit simulations result in detailed evaluation of key plasma parameters. These data are used to study and design the plasma support subsystems.

In this section, the work of the fusion-core physics activity during the scoping phase of the TITAN study is presented. Because of its impact on the design, the impurity-control and particle-exhaust subsystem is discussed in a separate section (Sec. 6). The theoretical and experimental principles of the RFP confinement scheme are presented in Sec. 4.2 and 4.3, respectively.

Section 4.4 contains the magnet configuration analysis. The design of the Oscillating-Field Current Drive (OFCD) coil set and that of the divertor coil set are not finalized and await engineering analysis of the design point as suggested by the work performed to date. At this time, two options for the toroidal-field (TF) coil set are pursued: (a) normal copper TF coils and (b) the integrated blanket-coil (IBC) concept. The poloidal-field (PF) coil set features normal copper ohmic-heating (OH) coils and superconducting equilibrium-field (EF) coils.

The fusion core simulation effort is discussed in Sec. 4.5. Strong emphasis is placed on the question of plasma start-up from gas breakdown and RFP

TABLE 4.1.-I
SUMMARY OF 1000-MWe TITAN STRAWMAN DESIGN-POINT

Neutron Wall Loading, $I_w(\text{MW}/\text{m}^2)$	18.1
First-wall minor radius, $r_w(\text{m})$	0.65
Plasma minor radius, $r_p(\text{m})$	0.60
Plasma major radius, $R_T(\text{m})$	3.90
Plasma volume, $V_p(\text{m}^3)$	27.7
Plasma density, $n(10^{20}/\text{m}^3)$	4.35
Plasma temperature, $T(\text{keV})$	20.0
Poloidal beta, β_θ	0.20
Plasma current, $I_\phi(\text{MA})$	17.75
Toroidal current density, $j_\phi(\text{MA}/\text{m}^2)$	15.7
Energy confinement time, $\tau_E(\text{s})$	0.25
Thermal diffusivity, $\chi_E(\text{m}^2/\text{s})$	0.27
Pinch Parameter, Θ	1.55
Reversal parameter, F	-0.10
Poloidal field at plasma surface, $B_\theta(\text{T})$	5.2
Reversed-toroidal field during burn, $-B_{\phi R}(\text{T})$	0.36
Fusion power density, $P_F/V_p(\text{MW}/\text{m}^3)$	81.6
Plasma ohmic dissipation, $P_Q(\text{MW})$	8.0
Engineering Q-value, $Q_E = 1/\epsilon$	7.84
Fusion power, $P_F(\text{MW})$	2,261.0
Total thermal power, $P_{TH}(\text{MW})$	2,866.0
System power density, $P_{TH}/V_{FPC}(\text{MWt}/\text{m}^3)$	12.8
Mass power density, $1000P_E/M_{FPC} = \text{MPD}(\text{kWe}/\text{tonne})$	644.0
Cost of electricity, COE(mills/kWeh)	35.2

formation leading to the slow current ramp phase and eventually a steady state burn. It is found that the TF and PF coil design limits are more affected by the plasma breakdown, RFP formation, and subsequent current-ramp transients than by the steady-state burn phase. Furthermore, the desire to eliminate on-site power supplies for start-up and use the power grid for this purpose strongly impact the start-up sequence. Section 4.5 discusses these tradeoffs in detail.

A current-drive system is required for steady-state operation. Various options for this system are discussed in Sec. 4.6. The primary current-drive

option for the TITAN design is based on the OFCD scheme. The circuit modeling and simulation effort used to guide the OFCD design is also presented.

One-dimensional fusion-core simulations are also performed to supplement the zero-dimensional simulations in order to examine 1-D aspects of local transport assumptions, impurity radiation with beta limits, pellet fueling, RFP dynamo, and current drive. These 1-D simulations are also coupled to edge-plasma models (described in Sec. 6) to provide a self-consistent picture of edge-plasma/core-plasma interface. These 1-D simulations are included in Sec. 4.7.

Conclusions of the fusion-core physics activities during the first phase of the TITAN program, as well as the future directions, are discussed in Sec. 4.8.

4.2. RFP THEORY

The theoretical principles and understanding of RFP confinement scheme are described in this section. Those features that are relevant to a fusion reactor are emphasized. The experimental data base is summarized in Sec. 4.3. A more detailed description of the RFP confinement concept is given in Ref. 1 and references contained therein.

The RFP, like tokamak, belongs to a class of axisymmetric, toroidal confinement systems that utilizes both toroidal (B_ϕ) and poloidal (B_θ) magnetic fields to confine the plasma. In the tokamak, stability is provided by a strong toroidal field ($B_\phi \gg B_\theta$) such that the safety factor exceeds unity, that is, $q > 1$. Here, q is defined as $q(r) = rB_\phi / R_T B_\theta$, and R_T and r_p are respectively, the major and minor radii of the plasma. In the RFP, on the other hand, strong magnetic shear produced by the radially varying (and decreasing) toroidal field stabilizes the plasma with $q < 1$ and relatively modest B_ϕ . Theoretically, an electrically conducting shell surrounding the plasma is required to stabilize the long-wave-length MHD modes. In both the RFP and tokamak, equilibrium may be provided by either an externally produced vertical field, a conducting toroidal shell, or a combination of both. Figures 4.2.-1.a, b, and c respectively show, the radial variation of the poloidal and toroidal field and also the safety factor for tokamaks and RFPs.

The RFP relies strongly on the poloidal field generated by the current in the plasma. This feature has several reactor-relevant advantages. The poloidal field decreases inversely with the plasma radius outside the plasma. The toroidal field is also rather weak outside the plasma. The magnetic field

Fig. 4.2.-1. Magnetic field distribution for tokamak (a) and RFP (b) and the q profiles for tokamak and RFP (c).

strength at the external conductors, therefore, is small, resulting in a high engineering beta (defined as the ratio of the plasma pressure to the magnetic field pressure at the magnets). Low-current-density, less-massive, and resistive coils are possible. Also, the RFP can operate at high total beta. The experimentally measured beta values are in the range 10% to 20%, which is the range used in reactor studies. Further, the RFP relies on the magnetic shear to stabilize the plasma. Thus, it can operate with a large ratio of plasma current to toroidal field, and stability constraints on the aspect ratio, R_T/r_p , are removed. The choice of the aspect ratio can be made solely on the basis of engineering constraints. High-current-density operation and ohmic heating to ignition are possible.

The fundamental property of the RFP is that the field configuration and toroidal-field reversal is the result of the relaxation of the plasma to a minimum-energy state. Taylor's theory of relaxed states [2,3] postulates that all pinch configurations will relax to a state determined by minimizing the magnetic energy subject to some constraints on allowed motion or magnetic field variation. Taylor then considers the relaxation of a plasma with small but finite resistivity in a flux conserving cylinder, subject to the invariance of the magnetic helicity, $K \equiv \int \vec{A} \cdot \vec{B} dV$ where $\vec{B} \equiv \nabla \times \vec{A}$. The relaxed state was found to be force-free and described by $\nabla \times \vec{B} = \mu \vec{B}$, where μ is uniform across the plasma. The solution to this equation in cylindrical geometry gives the "Bessel-Function Model" (BFM), with $B_\phi \propto J_0(\mu r)$ and $B_\theta \propto J_1(\mu r)$ where J_0 and J_1 are the Bessel functions of the first kind. These relaxed states can be described by the dimensionless quantities, the pinch parameter, Θ , and the reversal parameter, F :

$$\Theta \equiv \frac{B_\theta(r_p)}{\langle B_\phi \rangle}, \quad (4.2.-1)$$

$$F \equiv \frac{B_\phi(r_p)}{\langle B_\phi \rangle}, \quad (4.2.-2)$$

where $\langle B_\phi \rangle$ is the average toroidal field,

$$\langle B_\phi \rangle = \frac{2}{r_p^2} \int_0^{r_p} B_\phi(r) r \, dr. \quad (4.2.-3)$$

The locus of relaxed states then form a curve in F- Θ space as shown in Fig. 4.2.-2 (labeled as BFM). In the same figure, the corresponding experimental data are also shown. These data lie to the right of the curve predicted by Taylor's BFM model. These experimental equilibria differ from Taylor's model, since plasma has finite pressure, μ is not uniform across the plasma, and a perfectly conducting wall is not used. Thus, they represent "near-minimum-energy" states. However, Taylor's concept of a preferred locus of relaxed states in F- Θ space remains valid.

The theory of relaxed states has several important consequences. First, the theory predicts that the relaxed states depend only on the pinch parameter, Θ , and these states are independent of initial conditions provided that time scale is sufficiently large for the relaxation process to take place. Second, if the current and toroidal flux are maintained constant in time (i.e., constant Θ) then, the relaxed state equilibrium will be sustained. Experimentally, RFPs are observed to exist for times much larger than the resistive decay time of the field profile. This process involves continuous generation of toroidal field within the plasma, often called the RFP "dynamo", to compensate for the resistive decay of the toroidal field and maintains the field profile.

Finally, the F- Θ relationship implies a strong coupling between the toroidal and poloidal fields; the toroidal field can be generated by driving toroidal current with external poloidal-field circuits. Indeed, such a relaxation-assisted plasma current ramp is used in experiments and is envisioned for RFP reactors. Furthermore, the strong coupling of the poloidal and toroidal fields also offers the possibility of a steady-state current drive system through the "helicity injection" technique. Current drive through "electrostatic" helicity injection has been experimentally demonstrated in spheromaks [4], which is also a relaxed-state system. Another helicity injection technique is the Oscillating-Field Current Drive (OFCD). In this scheme, oscillating voltages are applied to the toroidal and poloidal circuits in the appropriate phase to drive a DC toroidal current in the plasma with the plasma in effect behaving as a nonlinear rectifier. The experiments on OFCD are yet not conclusive but are encouraging [5,6]. Helicity injection techniques are discussed in more detail in Sec 4.6.

Fig. 4.2.-2. Locus of operating points on the F- Θ diagram. The solid line (BFM) is the curve predicted by Taylor's theory and the data points are from several RFP experiments.

4.2.1. Equilibrium

The analysis of equilibrium and stability in RFPs usually invoke the high-aspect-ratio (straight cylinder) approximation. Such a model encompasses z-pinch ($q = 0$), θ pinches ($q \rightarrow \infty$), large-aspect-ratio tokamaks ($q > 1$) and RFPs ($q < 1$). The radial pressure balance in the screw pinch is described by

$$\frac{d}{dr} \left[p + \frac{B_{\theta}^2 + B_{\phi}^2}{2\mu_0} \right] + \frac{B_{\theta}^2}{\mu_0 r} = 0, \quad (4.2.-4)$$

where p is the plasma pressure and μ_0 is the permeability of free space. This equilibrium pressure balance is then subjected to stability analysis via the energy principle [7] or normal-mode technique.

In principle, some information on the current and pressure profiles is required to find the equilibrium magnetic field profiles. Because of the strong tendency for RFPs to relax, the field distributions obtained in modern experiments are near-minimum-energy states. The theory of relaxed states predicts the condition, $\nabla \times \vec{B} = \mu \vec{B}$ with $\mu \equiv \mu_0 j_{\parallel} / B$ spatially uniform across the plasma, leading to BFM field profiles. However, a constant μ (or constant $j_{\parallel} / B$) implies large parallel current density near the wall in a region of cold, resistive plasma. A μ profile that is nearly constant over the bulk of the plasma, and decreases in the outer region to match the practical $\mu(r_p) \approx 0$ condition eliminates the unphysical features of the BFM. Examples are $\mu/\mu(0) = 1 - (r/r_p)^8$ or a μ profile which is constant for $r < r_r$ (the radius of reversal surface) and decreases uniformly to zero at r_p .

For a toroidal system, equilibrium also requires the compensation of the outward force from the plasma pressure and the plasma current (poloidal field pressure). Either a perfectly conducting wall, or a vertical field produced by the external circuits, or a combination of both, is necessary for equilibrium. The required value of this vertical field is given by Shafranov [8]

$$B_v = \frac{\mu_0 I_{\phi}}{4\pi R} \left[\ln \left(\frac{8R_r}{r_p} \right) + \beta_{\theta} + \frac{l_i}{2} - 1.5 \right], \quad (4.2.-5)$$

where l_i is the plasma internal inductance per unit length.

It follows from the static ideal MHD equations,

$$\nabla p = \vec{J} \times \vec{B} , \quad (4.2.-6)$$

$$\nabla \times \vec{B} = \mu_0 \vec{J} , \quad (4.2.-7)$$

$$\nabla \cdot \vec{B} = 0 , \quad (4.2.-8)$$

that the current and the magnetic field lines lie on constant plasma pressure surfaces. For axisymmetric, toroidal, current carrying plasma, the equilibrium consists of toroidal flux surfaces, nested about a magnetic axis. Each surface is generated by helical field lines. If the toroidal symmetry is violated, for example because of errors in magnet coils, the ideal nested toroidal surfaces break up and a more complicated structure emerges which includes helical magnetic islands. The existence and interaction of these islands are believed to have an important impact on plasma transport. Although both tokamaks and RFPs are susceptible to magnetic island formation, the number and location of the resonant flux surfaces is significantly different in the two concepts, making field errors a greater concern in RFPs.

4.2.2. Stability

The Suydam criterion [9] gives a necessary condition for stability against ideal MHD pressure-driven modes for a straight cylinder.

$$\frac{r}{4} \left(\frac{1}{q} \frac{dq}{dr} \right)^2 + \frac{2\mu_0}{B_\phi^2} \frac{dp}{dr} > 0 , \quad (4.2.-9)$$

where $q(r)$ is a relative measure of field line pitch at position r . This criterion simply states that the negative pressure gradient associated with the confinement of a hot plasma has a destabilizing effect and can be compensated only by a sufficiently large radial variation in the field line pitch (i.e., large magnetic shear).

By including first-order toroidal effects, Eq. (4.2.-9) becomes the Mercier criterion [10].

$$\frac{r}{4} \left(\frac{1}{q} \frac{dq}{dr} \right)^2 + \frac{2\mu_0}{B_\phi^2} \frac{dp}{dr} (1 - q^2) > 0, \quad (4.2.-10)$$

which shows that if $q(r) > 1$ everywhere, the system is stable even without the magnetic shear. This is the primary approach to stability used in the tokamak. Physically, the $q > 1$ condition forces the wave length of potentially unstable $m = 1$ kink mode to exceed the major circumference of the torus ($2\pi R_T$). In RFPs, $q < 1$ and RFPs must operate with sufficient shear to satisfy the Mercier criterion. Moreover, a pitch minimum, $dq/dr = 0$, should be avoided within the plasma.

Necessary conditions for stability against ideal MHD current-driven modes have been derived by Robinson [11] on the basis of the energy principle [7]. The necessary condition is found to be $|P(r_w)| < 3P(0)$, where the pitch is $P(r) \equiv rB_\phi(r)/B_\theta(r) = Rq(r)$ and r_w is the location of the conducting wall. This can be written approximately as

$$\left(\frac{r_w}{a} \right)^2 < 3 \left| \frac{B_\phi(0)}{B_\phi(r_w)} \right|, \quad (4.2.-11a)$$

$$\Phi > 0, \quad (4.2.-11b)$$

where Φ is the total toroidal flux inside the conducting wall. These conditions require both the amplitude and the region of the field reversal not to be large: the conducting wall should be close to the plasma to stabilize current driven modes. Furthermore, stability against current-driven modes also requires that no pitch minimum occurs in the plasma.

The above conditions are usually well satisfied for experimental profiles and are also satisfied for the profiles calculated for TITAN design. The above necessary conditions (4.2.-11) are in practice close to being sufficient. Based on ideal MHD theory, therefore, RFP profiles are possible with plasma beta as high as 0.3. Note that Taylor's theory predicts that all states on the F-0 diagram including those with $F > 0$ and no field reversal are near-minimum-energy states and, therefore, are stable. However, profiles with no reversal exhibit a pitch minimum within the plasma and are unstable to both ideal MHD pressure-driven and current-driven modes. This conclusion is confirmed

Fig. 4.2.-3. Plasma resistance as a function of the pinch parameter, Θ (HBTX1A).

experimentally and is shown in Fig. 4.2.-3, which shows the plasma resistance as a measure of the confinement as a function of the pinch parameter, Θ . It is seen that as Θ increases, the resistance falls dramatically, particularly as Θ exceeds the value where the field reversal occurs [12].

In the ideal-MHD model, the plasma is assumed to have zero resistivity. This assumption constrains magnetic field lines to be "frozen" in the plasma and thus limits the class of potentially unstable modes. Resistive-MHD stability analysis has to be performed to provide a more realistic picture of the plasma behavior. In general, the criteria for resistive stability are more stringent, and a closer fitting conducting wall and a lower β are required. Detailed analysis of current-driven resistive tearing modes has been made [13] and stable RFP configurations have been found with $\beta \approx 0.2$. These configurations have been found, however, to be unstable to the so-called resistive g-modes. Moreover, analyses show that resistive g-modes can become unstable for pressure gradients substantially smaller than those that can drive ideal-MHD instabilities. These resistive g-modes are localized and may ultimately affect the confinement. In fact, some theoretical estimates of the energy confinement time have been proposed that are based on transport along stochastic field lines created by resistive g-mode turbulence [14] (see Sec. 4.2.4).

4.2.3. Relaxation and Sustainment

The theory of relaxed states has been extremely successful in predicting the behavior of RFPs and the characteristics of the P - Θ diagram. The details of the relaxation process to the near-minimum-energy states are neither invoked nor required in this theory. These details, however, are required to understand the relaxation, sustainment, stability, transport, and their relationship in RFPs.

Of particular interest is to account for RFP sustainment. Consider a cylindrically-symmetric, resistive plasma (even with anisotropic resistivity) with field-reversed profiles. According to the Ohm's law, there should be a poloidal electric field corresponding to the poloidal current at the reversal point. To sustain this resistive electric field, Faraday's law requires a resistive decay of the toroidal flux inside the reversal radius. Reversed-field pinch discharges, however, are maintained for times far longer than the resistive diffusion time. Some mechanism is necessary to drive the poloidal current at the plasma surface, canceling the resistive electric field there. This mechanism, the so-called "dynamo", generates the magnetic field that opposes the resistive field diffusion.

Several mechanisms for toroidal flux regeneration have been proposed, each offering a different explanation for the origin of the poloidal current at the reversal surface. For example, second-order effects of low-level MHD fluctuations resulting from nonlinear evolution of resistive MHD modes can drive a sufficient poloidal current at the reversal surface to sustain the field reversal [15]. Alternatively, a plasma model with stochastic field lines is proposed, wherein a global rather than a local Ohm's law applies and the poloidal current at the reversal surface can be driven by electromagnetic fields originating elsewhere in the plasma [16-18].

In summary, quasi-stationary RFP equilibria are sustained through continuous relaxation and field generation. These time-averaged equilibria are stable to ideal and resistive-tearing modes, because the relaxation process acts to maintain the stability. Relaxation and field-generation processes are driven continuously by a complex spectrum of resistive modes and their nonlinear interaction. These processes also involve field-line reconnection and profile modification which can impact the cross-field transport. Therefore, the plasma confinement and β are also affected by the relaxation process. The details of the mechanisms that drive the relaxation and dynamo activities in RFPs, however, remain unresolved.

4.2.4. Transport and Confinement

Some theoretical models for the transport in RFPs have been proposed. However, a detailed transport model is not yet available for RFPs, and the precise behavior of the energy confinement is unknown. One can use an empirical approach to evaluate present experimental results and form a basis for the extrapolation of these results to reactor regimes. Here, the details of the transport physics is not considered but rather, experimental observations are used to guide the theory. Starting with simple pressure balance, $p \propto \beta_\theta I_\phi^2 / r_p^2$, one has

$$T = \frac{\mu_0}{16\pi k_B} \left[\frac{I_\phi}{N} \right] \beta_\theta I_\phi, \quad (4.2.-12)$$

where $N = n r_p^2$ is the plasma line-averaged density, T is the average ion temperature, and k_B is the Boltzmann constant. First, Eq. (4.2.-12) predicts that $T/I_\phi \propto I_\phi/N$, a behavior which is observed experimentally. Second, the

plasma pressure in some devices is found to scale as I_ϕ^2 over a wide range of parameters, indicating a constant β_θ operation. In fact, there is some experimental evidence that RFPs operate near a beta limit; energy transport and loss mechanisms in RFPs are self-adjusted to lose just enough plasma energy to maintain β_θ constant (Sec. 4.3.6). Equation (4.2.-12) then suggests that the temperature varies linearly with the current for a fixed I_ϕ/N , as reported for a number of experiments. Note that the constant-beta assumption still remains an open question, since a strict linear relation between T and I_ϕ is not observed in all RFP experiments.

Next, using the definition of the energy confinement from the energy balance for a steady-state ohmically-heated discharge, and substituting from Eq. (4.2.-12) for T , one gets

$$\tau_E \approx \frac{3 n k T}{\eta j_\phi^2} = \frac{3}{16} \mu_0 \beta_\theta r_p^2 \sigma, \quad (4.2.-13)$$

where $j_\phi \equiv I_\phi/\pi r_p^2$ and $\sigma = 1/\eta$ is the plasma electrical conductivity. The plasma resistance is observed to have classical temperature dependence, $\sigma \propto T_e^{3/2}$, on a number of RFP experiments over a wide range of parameters. Then, using the constant-beta assumption and linear dependence of T and I_ϕ , the energy confinement time is found to scale with the plasma current, $\tau_E \propto I_\phi^{3/2} r_p^2$.

It is important to point out that in calculating the plasma resistance, geometrical effects (i.e., field-line pitch), impurities, and the anomalous resistance associated with the RFP dynamo must be considered. The dynamo represents an added dissipation of the currents driven by the dynamo electric field and, therefore, appears as an actual resistivity anomaly. In fact, the resistivity in RFP experiments in many conditions is observed to be close to the classical value, taking into account the geometrical and impurity effects. The dissipation associated with the dynamo effect is generally small and the ohmic power delivered to the plasma is not expected to exceed the classical predictions by a significant amount. However, at low densities (or high I_ϕ/N) the resistance anomaly factor is too high to be explained by geometrical or impurity effects.

Some theories have been proposed which estimate the energy confinement time associated with the electron parallel transport along stochastic magnetic field lines caused by resistive-fluid turbulence. In one theory [14], resistive

g-mode turbulence has been considered. The diffusion coefficient for this case is found to scale as:

$$D \propto \eta (M/m)^{1/2} \beta_{\theta}^2, \quad (4.2.-14)$$

where M and m are the ion and electron masses, respectively. Using this diffusion coefficient for ohmically heated discharges, the value of beta is found to be $\beta_{\theta} = (m/M)^{1/6}$ and is independent of machine parameters (the factor $1/6$ is a result of assuming a J_0 radial temperature profile). This, together with the pressure balance, results in a linear temperature-current scaling, as reported in a number of experiments.

In another theory [19], resistive tearing mode turbulence has been considered which results in another scaling, namely,

$$\beta_{\theta} \propto (I_{\phi}/N)^{-1/3} I_{\phi}^{-1/3} r_p^{-1/6}, \quad (4.2.-15a)$$

$$T \propto (I_{\phi}/N)^{2/3} I_{\phi}^{2/3} r_p^{-1/6}. \quad (4.2.-15b)$$

In still another theory based on current-driven drift-wave turbulence [20], the following scaling relationships are found:

$$\beta_{\theta} \propto (I_{\phi}/N)^{-1} I_{\phi}^{-1/7} r_p^{-2/7}, \quad (4.2.-16a)$$

$$T \propto I_{\phi}^{6/7} r_p^{-2/7}, \quad (4.2.-16b)$$

$$\tau_E \propto (I_{\phi}/N)^{-1} I_{\phi}^{8/7} r_p^{9/7}. \quad (4.2.-16c)$$

This last theory predicts a weak dependence of plasma beta on machine parameters and almost linear temperature-current dependence. A better resolution of these

various theoretical predictions must await experiments with a broader range of plasma and machine parameters (e.g., plasma current, dimensions).

4.3. RFP EXPERIMENTS

The earliest milestone (1965) for the RFPs was the discovery of a period of improved stability and reduced turbulence (called quiescence) on the ZETA device [21]. This quiescent period observed in ZETA was preceded by a turbulent phase with large energy losses and strong plasma-wall interaction. Furthermore, self-reversal of the external toroidal field relative to the on-axis field was observed, but the importance of these observations was not appreciated at the time.

To reduce RFP formation losses, experimental RFPs during 1970s used fast magnetic-field programming, with typical rise-times of a few microseconds, to force the reversal externally. These experiments required electrically insulating discharge tubes to accommodate the high voltages needed for fast programming and operated on time scales of up to tens of microseconds. Many important advances in RFP physics were made in these machines.

With experience from fast-programming machines and a general theory of relaxed states in hand, modern RFP experiments in late 1970s and 1980s have moved back toward a slow rising plasma current (0.1-1.0 ms) and the facility for slow B_ϕ control to assist and optimize the self-reversal process and to minimize RFP formation losses. These machines use a metallic liner, are equipped with better vacuum systems, and have more accurate magnetic-field geometry. The first of these modern machines to operate was ETA-BETA-II at Padova [22-24] at 1979. Today, high-temperature plasmas are routinely produced in many intermediate-size machines such as TPE-1R(M) at ETL, Sakura-Mura [25,26], ZT-40M at Los Alamos [27-29], HBTX1A at Culham [30,31], and OHTE/RFP at GA Technologies [32,33]. General parameters of these experiments are listed in Table 4.3.-I. The design parameters of the TITAN reactor are also listed in that table for comparison.

The plasma parameters obtained in these experiments have been improving steadily. Values of β_0 in the range 0.1 to 0.2 are routinely achieved, which are adequate for a reactor. Electron temperatures in the range 0.4-0.6 keV, densities up to about 10^{20} m^{-3} , and energy confinement times of a few tenths of millisecond are typical of these intermediate-size experiments. Data from a number of machines indicate a linear temperature-current scaling, which suggest

TABLE 4.3.-I
COMPARISON OF TITAN DESIGN POINT TO KEY RFP EXPERIMENTS IN PRESENT OPERATION^a

<u>Parameter</u>	<u>TPE-1R(M)</u>	<u>ETA-BETA-II</u>	<u>ZT-40M</u>	<u>ONTE-RFP</u>	<u>HBTX</u>	<u>TITAN</u>
Laboratory	ETL (Japan)	Padova (Italy)	Los Alamos (USA)	GA Tech. (USA)	Culham (UK)	UCLA et al. (USA)
Major Radius, R (m)	0.5	0.65	1.14	1.24	0.8	3.9
Minor Radius, r_p (m)	0.09	0.125	0.20	0.20	0.26	0.6
Maximum Current, I_ϕ (MA)	0.15	0.25	0.45	0.50	0.30	17.7 ($\lesssim 10$) ^c
Maximum Current Density, j_ϕ (MA/m ²)	5.9	5.1	3.6	4.0	1.4	15.7 ($\lesssim 5$) ^c
Pulse Risettime (ms)	0.45	0.1-0.5	0.25-2.0	0.2-0.5	0.3-4.0	10,000.
Pulse Length (ms)	0.8	1.0-2.0	5.0-37.	5.0-10.0	5.0-14.0	∞
Average Density, n ($10^{20}/m^3$)	0.1-1.0	0.2-4.0	0.1-1.0	0.1-5.0	0.1-1.0	4.3 ($< 4.$)
On-axis Electron Temperature, $T_e(0)$ (keV)	0.5-0.6	0.1-0.2	0.3-0.5	0.5	0.3-0.4	24.0 ($\lesssim 6.$) ^c
Maximum Ion Temperature, T_i (keV)	0.5-0.6	0.1-0.2 ^b	$> T_e(0)$	$> T_e(0)$	0.3-0.4 ^b	24.0 ($\lesssim 6.$) ^c
Poiodal Beta, β_θ	~ 0.1	~ 0.1	0.1-0.2	0.1-0.3	0.1-0.2	0.20 ($\lesssim 0.05$) ^c
Energy Confinement Time, τ_E (ms)	~ 0.1	~ 0.1	0.3-0.7	~ 0.4	0.1-0.3	252 (~ 400) ^c

^a Values given in this table do not form a self-consistent set measured for any one condition, but instead describe a range of typical conditions.

^b At maximum (peak) current.

^c Titan design parameters during burn (at ignition).

$\tau_E \propto T_e^{3/2}$. Furthermore, both experimental and theoretical evidence suggests a strong scaling of $n\tau_E$ with the plasma current ($\tau_E \propto I_\phi^{5/2}$).

4.3.1. Start-up and RFP Formation

The time history of a typical RFP experimental discharge can be divided into three phases: the formation phase; the sustainment phase; and the termination phase. A representative time history of a RFP discharge is shown in Fig. 4.3.-1 (upper trace). The formation phase denotes the time from the start to the peak of the toroidal plasma current. The sequence of events during the formation phase begins by establishing a toroidal magnetic field inside the discharge chamber using the toroidal field coils in the absence of the plasma. At the time of peak toroidal magnetic field, poloidal-field windings are activated to produce a toroidal voltage around the discharge chamber. This voltage typically ionizes the gas in a few microseconds, and the toroidal current is initiated. As time proceeds the toroidal current increases, and toroidal magnetic field on the axis increases, while the toroidal magnetic field at the wall decreases, keeping the average toroidal field in the chamber almost constant. Eventually the toroidal magnetic field at the wall passes through zero, becomes reversed, and is crowbarred.

The discharge is then extended by using a passive crowbar on the poloidal circuits, which gives a decaying waveform, or an active (power) crowbar, which produces a flat-top current waveform as seen in Fig. 4.3.-1 (upper trace). Reversed-field pinch discharges normally experience an abrupt end when the plasma current decreases rapidly to zero. Accompanying this fast current "termination" is a positive pulse in the toroidal voltage at the liner, in contrast to the negative toroidal voltage spike that accompanies the disruption of the current in a tokamak indicating a difference in the flow of magnetic energy to or from the plasma.

Three modes of operation are generally used for the RFP formation phase: "self reversal", where a conducting shell maintains and conserves the toroidal flux inside the chamber and is used on OHTE/RFP and often on HBTX1A; "matched mode", where external circuits are programmed to conserve the toroidal flux inside the chamber by maintaining $E_\theta = 0$ at the liner, simulating the action of a conducting shell, as usually used on ZT-40M; and "aided reversal", where the external circuits supplement the plasma self-reversal effect, as typically used on ETA-BETA-II. Field control during the formation phase provides flexibility in varying the pinch parameter, Θ , on which the configuration depends. The

Fig. 4.3.-1. Typical waveforms for the toroidal current, I_ϕ , average toroidal field $\langle B_\phi \rangle$, and toroidal field at the plasma edge, $B_\phi(r_p)$ ($Z \sim 40M$).

choice of the formation mode also affects the consumption of the poloidal flux. The final plasma parameters, however, are not particularly sensitive to the mode used for RFP formation.

Another mode of start-up of an RFP has been demonstrated on the ZT-40M experiment. This mode is called "ramped start-up" because the plasma current is slowly ramped to its final value after an initial low-current RFP is formed. The lower trace in Fig. 4.3.-1 shows such a ramped start-up sequence. In a conventional start-up sequence, the peak current is nearly reached at the time the field at the wall reverses (Fig. 4.3.-1, upper trace). This start-up mode is undesirable in a large experiment or a reactor because the RFP formation phase is a very lossy process until reversal is reached. In a ramped start-up, on the other hand, the RFP configuration is set up in a relatively short time at a low current and then the current is slowly raised to the desired value while maintaining the RFP profiles.

The ramped start-up scenario relies on the plasma relaxation process. During the current ramp, the toroidal flux must be increased proportionally to the current to maintain the RFP profiles (i.e., holding F and Θ constant). This requires generation of toroidal flux via the dynamo action since the toroidal field at the wall is negative while the average toroidal flux is positive. The plasma must generate an equal and opposite amount of negative and positive flux (to satisfy Faraday's law) and then expel the negative flux from the plasma to generate a net positive flux increase. Indeed, the ramped discharges show that toroidal flux continues to be generated on a multi-millisecond time scale, and negative flux is expelled from the plasma.

The TITAN design relies on ramped start-up scenario for RFP formation and plasma current ramp to its final value. Special attention is given to the RFP formation phase. Experimental data point to a RFP formation "window" in the parameter space constrained by many factors such as the volt-second requirement (i.e., poloidal-flux consumption), equilibrium and field error constraints, plasma density, and current density constraints. These experimental data are described in detail in Sec. 4.5.1 and then extrapolated to find a RFP formation window for the TITAN design.

4.3.2. Plasma Parameters

The time variation of the temperature from several machines is shown in Fig. 4.3.-2. For discharges with a flat-top current waveform (Fig. 4.3.-2.a and b) the electron temperature rises rapidly, reaching approximately 100-200 eV

Fig. 4.3.-2. Time dependence of electron temperature for HBTX1A (a), ZT-40M (b), and ETA-BETA-II (c), and ion temperature for TPE-1RM (d).

near the time of peak current, and then more slowly as the density drops, reaching ultimately 300-500 eV. At later times the temperature remains approximately constant or decreases slightly. This later behavior is attributed to wall effects, possibly caused by field errors or inadequate equilibrium. Similar behavior of the electron temperature is seen on smaller devices, as is illustrated in Fig. 4.3.-2.c for ETA-BETA-II, where the time scale is much shorter and T_e rises continuously during the pulse. It is shown in Fig. 4.3.-2.b that the temperature increases with plasma current. Similar behavior is generally observed in all RFP experiments.

The ion temperature is usually comparable to the electron temperature, with some examples showing $T_i > T_e$. Figure 4.3.-2.d shows the time variation of the ion temperature in TPE-1R(M). The electron-ion equilibration time, however, is so long that the ions cannot be heated by collisions with electrons and some anomalous ion heating mechanism is apparent.

Generally, RFP experiments operate without active refueling. The chamber is filled with gas prior to the discharge and density is maintained by recycling with the chamber wall. Modern RFP discharges are of sufficient duration where active refueling by pellet injection is being implemented [29]. The temporal variation of plasma density is shown in Fig. 4.3.-3. Typically, the electron density rises initially to a value corresponding to the filling density and then falls rapidly to 10-20% of the filling value during the formation phase (density "pump-out"). Thereafter, the density decays more slowly. In machines with long pulses, the density tends to reach a steady state value which shows little dependence on the initial filling density but depends on the plasma current and the wall conditioning.

The rate of initial pump-out depends strongly on the wall condition and is attributed to particles which leave the discharge and are not replaced fast enough by wall recycling. This process can be affected by pre-conditioning the wall, as seen in Figs. 4.3.-3.a and b for HBTX1B and ZT-40M, respectively. In these experiments, the walls were loaded with hydrogen or deuterium prior to the discharge initiation. The results show that the density decay can be much reduced and the density sustained approximately constant for up to 5 ms or longer without active refueling.

Gas-puffing through external fast-acting valves has been performed on both HBTX1A and ZT-40M. The valves inject a steady stream of gas for a pre-programmed portion of discharge. In these experiments, gas-puffing has increased the density by at least a factor of two while temperature measurements

Fig. 4.3.-3. Time dependence of plasma density for HBTX1B (a), ZT-40M (b), and ETA-BETA-II (c), and TPE-1R(M) (d).

indicate that the electron temperature decreased when the density was increased. Also, increasing the plasma density by gas-puffing has increased the duration of the current pulse in both HBTX1A and ZT-40M. Additional gas-puffing experiments await better equilibrium control, since gas-puffing often changes the equilibrium at such a rate that it cannot be controlled by the present feedback systems on these experiments.

4.3.3. Fluctuations

Fluctuations of MHD origin are important for field generation and sustainment. Fluctuations can also enhance cross-field transport because of the break-up of the magnetic surfaces and resulting ergodic field-line behavior. Fluctuations have been studied on various RFP experiments [17,34-37]. In HBTX1A, for example, dominant global modes with $m = 1$ and a broad n spectrum centered around $n = -8$ (corresponding to the aspect ratio of the device) were found with the minus sign indicating that these modes are resonant inside the reversal surface. Fine-scale activity with a short transverse correlation length, containing comparable power to the global modes, was also found in the core of the discharge. Modes with $m = 0$ and small toroidal mode numbers were also observed. These dominant modes were observed at all times during the discharge, including the formation phase where the amplitude was a factor 5 to 10 times higher. These oscillations, therefore, appear to play a fundamental role. In contrast to HBTX1A, coherent quasi-continuous $m = 1$ activity in the center of the plasma was observed to dominate in low- Θ ZT-40M discharges [38]. The $m = 1$ modes were consistent with the predictions of the resistive instability theory.

Estimates of the width and separation of magnetic islands resulting from the $m = 1$ modes indicate that the field lines are stochastic inside the reversal surface and probably throughout the plasma when the $m = 0$ modes are taken into account. Qualitative estimates of the energy confinement time based on stochastic field line diffusion from the $m = 1$ modes are similar in magnitude to those observed on HBTX1A.

The level of magnetic-field fluctuation is denoted by $\tilde{B}/|B|$ where $\tilde{B}$ is the rms value of the random fluctuation amplitude on the poloidal and toroidal fields and $|B|$ is the average value of $(B_\phi^2 + B_\theta^2)^{1/2}$. The fluctuation levels in RFPs are about 10^{-2} and are ten or more times greater than typically observed in tokamaks. The magnetic-field fluctuation levels are observed to decrease with increasing plasma current or magnetic Reynolds number, $S \equiv \tau_Q/\tau_A$ where τ_Q and τ_A

Fig. 4.3.-4. Magnetic fluctuation amplitude as a function of magnetic Reynolds number, S (OHTE).

are, respectively, the resistive and Alfvén times. This behavior is shown in Fig. 4.3.-4 for OHTE/RFP where fluctuation levels appear to decrease as $S^{-1/2}$. Similar behavior is also reported for ETA-BETA-II [39]. The cross-field transport caused by stochastic field-line diffusion, corresponding to such a correlation of fluctuations with S , can give a favorable temperature-current scaling which is consistent with the observed $T_e \propto I_\phi$ dependence.

4.3.4. Current termination

Operating RFP experiments usually end with a "current termination" phase where the plasma current is rapidly reduced to approximately zero. This termination occurs in a few tenths of microsecond in small devices and up to a few hundred microseconds in larger machines. Current termination is accompanied by a positive voltage spike and large density and magnetic-field fluctuations. A number of variables such as plasma radius and density, field reversal, magnetic field errors, and impurities are identified to affect the termination. However, a complete and satisfactory explanation of RFP current termination is not yet available.

Some evidence suggests that the onset of termination may be related to a loss of density and confinement, possibly leading to a streaming parameter, $\xi \propto (I_\phi/N)/T_e^{1/2}$, that exceeds a critical value for runaway electrons. A recent study [40] indicates that termination occurs when $I_\phi/N \sim 1-2 \times 10^{-13}$ A m, which is consistent with the occurrence of a critical drift threshold when ξ approaches unity.

Rapid current termination can have severe consequences in large, high-current experiments or reactors because of the large voltage spikes and the localized heating of the walls. A method of controlled "rundown" has been tested experimentally on HBTX1B [41] in which the toroidal-field circuit is controlled so that the pinch parameter is maintained at a given value as the current is decreased. The field reversal in this case is maintained until the current reaches a relatively low level. Maintaining reversal in this way is found to delay termination and the current can be reduced to between 10% to 20% of the maximum value before the termination occurs.

4.3.5. Scaling

Extensive measurements of the dependence of the temperature to the current for a range of RFPs have been reported. These measurements indicate that the on-axis electron temperature increase with plasma current as $T_e(0) \propto I_\phi^{V'}$, where

ν' is in the range of 0.5-1.0. For several experiments, $\nu' \approx 1$ up to plasma currents of 500 kA in agreement with the pressure balance Eq. (4.2.-12) for β_θ and I_ϕ/N constant. Temperature increases on the order of 1 eV/kA have been observed. Figure 4.3.-5 shows this behavior for OHTE/RFP. Data from ZT-40M for a range of conditions and short-pulse operation are given in Fig. 4.3.-6 which shows $\nu' \approx 1.2$ while $nT_e \propto I_\phi^2$ (constant β_θ). In other experiments on ZT-40M with flat-top operation and longer pulses, it was found that $T_e(0) \propto I_\phi^{0.7}$, but in these conditions, $n \propto I_\phi^{1.3}$, again resulting in $nT_e(0) \propto I_\phi^2$.

More recent results [42,43] suggest that the temperature-current scaling might be better described by postulating a constant beta, $nT_e(0) \propto I_\phi^2$, with a slope determined by I_ϕ/N . Evidence from a number of experiments indicates that β_θ varies relatively little over a range of conditions and from one machine to another, the latter suggesting little dependence on the dimensions of the apparatus. Some variation of β_θ with I_ϕ/N has been reported, with β_θ increasing somewhat as I_ϕ/N is reduced and as I_ϕ is increased. It should also be noted from present experimental results that the range over which favorable scaling is obtained appears to be extended by improved wall-conditioning methods and by reduction in field errors.

Estimates have been made of the energy confinement time, τ_E , on various experiments, but only a limited amount of scaling information is available. Specifically, quantitative data on the variation of τ_E with machine radius is not available. The experimental value of τ_E is generally obtained from the ratio of plasma energy to the heating power, which for all RFP experiments to date is the ohmic dissipation of the plasma current.

Under the assumptions of $T_e \propto I_\phi^{\nu'}$, $\sigma \propto T_e^{3/2}$, and $\tau_E \propto r_p^2$, similar to Eqs. (4.2.-12) and (4.2.-13), the following "ohmic" scaling law can be deduced:

$$\tau_E \propto I_\phi^{5/2-\nu'} r_p^2 f(\beta_\theta, I_\phi/N) / Z_{\text{eff}}, \quad (4.3.-1)$$

where the τ_E dependence on β_θ and I_ϕ/N have been incorporated into the function $f(\beta_\theta, I_\phi/N)$. In Fig. 4.3.-7, the inverse of plasma diffusivity, $1/\chi_E \propto \tau_E/r_p^2$, is plotted as a function of I_ϕ using the data from ZT-40M together with a few data points from ETA-BETA-II and TPE-1R(N). Two analytical curves that fit the data are also included. The design point for TITAN is also shown.

In the case where $\beta_\theta(I_\phi/N)$ is approximately constant, then $T_e \propto I_\phi$, and classical ohmic scaling Eq. (4.3.-1) yields $\tau_E \propto I_\phi^{3/2} r_p^2$, with the constant of

Fig. 4.3.-5. Variation of central electron temperature and central electron density with the plasma current (OHTE).

Fig. 4.3.-6. Variation of central electron temperature and the product of central electron temperature with the average electron density with the plasma current (2T-49M).

Fig. 4.3.-7. Variation of global energy confinement time, τ_E , with plasma current with data from several experiments.

proportionality depending on $\beta_0^{5/2} (I_\phi/N)^{3/2} / Z_{\text{eff}}$. The ZT-40M data of Fig. 4.3.-7 is plotted in Fig. 4.3.-8 in the form of $n\tau_E$ as a function of plasma current, which depends only on the parameter $(I_\phi/N)^{1/2} / Z_{\text{eff}}$, and is in agreement with the classical scaling $n\tau_E \propto I_\phi^{5/2}$, provided that Z_{eff} does not vary. A similar conclusion was also reached in OHTE/RFP, where a value of $n\tau_E \sim 10^{17} \text{ s/m}^3$ was recorded.

4.3.6. Evidence of a Beta-Limited Confinement

The observed scaling of plasma pressure with the toroidal current, $nT_e(0) \propto I_\phi^2$, is very suggestive that RFPs operate near a beta limit; the transport would adjust by MHD activity, radiation, or other mechanism to lose energy at a sufficient rate to maintain β_0 constant.

To test this hypothesis, a set of experiments was performed on ZT-40M by adding trace quantities of krypton as an impurity [44,45] to enhance the radiative losses of the plasma. The choice of krypton was made to maximize the ratio of radiated power to the ohmic heating input. It was found that as the impurity was injected, the radiation losses, P_{rad} , were increased, but at the same time, the input power, P_{in} , only slightly increased and most importantly the poloidal beta remained constant. It follows that as radiation losses increased, the non-radiative losses decreased to preserve the constant beta.

A simple zero-dimensional power balance equation for a plasma at steady state gives, $P_{\text{in}} = P_{\text{loss}} = P_{\text{rad}} + P_{\text{nr}}$, where P_{nr} and τ_{nr} are, respectively, the non-radiative power loss and non-radiative energy confinement time. Using the definition of the global energy confinement time, τ_E , one can write

$$\tau_{\text{nr}} = \tau_E \left(1 - \frac{P_{\text{rad}}}{P_{\text{loss}}} \right)^{-1}. \quad (4.3.-2)$$

For the assumed constant beta scaling and self-similar profiles of density and electron temperature (i.e., before and after krypton injection), the values of the total energy loss, P_{loss} , and τ_E remains unchanged. Equation (4.3.-2) then indicates that as the radiative losses were increased, the non-radiative losses were decreased (or τ_{nr} was increased) to maintain the energy content of the plasma and keep β_0 constant. The data from the krypton impurity experiments are plotted in Fig. 4.3.-9 which agree closely with predictions of Eq. (4.3.-2).

Fig. 4.3.-8. Variation of the Lawson parameter, $n\tau_E$, with the plasma current (ZT-40M).

Fig. 4.3.-9. Scaling of the non-radiative energy confinement time with the fractional radiative power loss (ZT-40M).

A second experimental check was made on the beta limit hypothesis. The energy confinement scaling Eq. (4.3.-1) also predicts that the total energy confinement of the plasma scales as Z_{eff}^{-1} . By injecting krypton impurities, the plasma resistance was varied and the total confinement time was measured. These experimental data is shown in Fig. 4.3.-10, which indicate that τ_E scales as Z_{eff}^{-1} , as predicted by Eq. (4.3.-1).

It is important to point out that while these results are very suggestive of the beta limit hypothesis they are not conclusive. Furthermore, it appears that far more power is being supplied to the discharge than is needed to maintain the plasma at its beta limit [44,45] and, therefore, these experiments are not expected to show an underlying transport which is not affected by the beta limit hypothesis.

4.3.7. Summary

In this section, the principles of the RFP confinement concept were discussed and the experimental data base was briefly reviewed. This data base is less extensive than that of tokamaks and, therefore, requires a larger extrapolation to reactor relevant regimes. However, modern RFP experiments such as those of Table 4.3.-I have all demonstrated the robustness of the RFP dynamo and an emerging commonality of the basic physical processes operative in RFPs.

The key physics requirements and uncertainties for a RFP reactor include heating, transport, plasma-wall interaction, current-drive (now under experimental investigation) and impurity control/particle exhaust with pumped limiters or magnetic divertors. The largest uncertainties in the existing RFP data base remain in the confinement physics and, in particular, in the mechanism and magnitude of cross-field transport in the near-minimum-energy state RFP configuration. Experiments with higher currents (and possibly higher current densities) and variable plasma size are needed to distinguish between different possible scaling laws. The modern RFP experiments are physically small, but operate with reactor-like power density; therefore, they can be strongly influenced by plasma-wall interaction. The increased particle and heat load on the first wall and limiter systems, and the need to control plasma-wall interactions also represent major challenges for the next-step multi-mega-ampere experiments.

Data from large multi-mega-ampere experiments are expected in the early 1990s. These data are of the utmost importance in resolving some of the key physics requirements and uncertainties for a RFP reactor. Furthermore, these

next-step experiments can provide valuable technological insight for devising a development path towards RFP fusion reactors.

4.4. MAGNET CONFIGURATION

The magnet configuration consists of a poloidal-field-coil (PFC) set, a toroidal-field-coil (TFC) set, a divertor-coil set, and an Oscillating-Field Current-Drive (OFCD) coil set. The divertor and the OFCD analyses have not progressed sufficiently to yield specific coil designs. Similarly, a detailed TFC design has not been performed, but two options are under consideration. One option uses a discrete set of normal-conducting, copper coils positioned outside the blanket/shield, similar to the CRFPR design [46], and is discussed in Sec. 4.4.2. The second TFC option is to use the integrated blanket/coil (IBC) concept [47], which is discussed in Sec. 8.2.5. An analysis of the PFC issues leading to the reference PFC design is presented in Sec. 4.4.1.

4.4.1. Poloidal-Field Coil (PFC) System

The PFC set performs both an equilibrium and an ohmic-heating (start-up) function. The equilibrium function requires that a vertical field of a certain magnitude and index, $n = \partial(\ln B_v)/\partial(\ln R)$, corresponding to the plasma current and beta [8,48,49], be imposed over the plasma cross section in order to maintain the plasma against the outward expansive forces arising from plasma and poloidal-field pressure. The ohmic-heating function provides the poloidal-flux swing required to establish the steady-state plasma current, which is then subsequently sustained by OFCD (Sec. 4.6). Since the ohmic-heating function is required only during start-up and the equilibrium function is required continuously, the PFC set is naturally, but not necessarily, split into two coil sets: an equilibrium-field coil (EFC) set and an ohmic-heating coil (OHC) set; both are discussed separately in the following subsections.

4.4.1.1. Equilibrium-Field Coils (EFCs)

Since the EFCs are continuously active, the recirculating power can be minimized by using superconducting EFCs. Superconducting EFCs, however, require ≥ 1.5 m of blanket and shielding between the coils and plasma compared to ≤ 0.8 m for normal-conducting EFCs; hence, more current is needed to produce the same field resulting in a more massive and expensive coil set. The trade-off between normal-conducting and superconducting EFCs was examined and found to

weigh somewhat in favor of superconducting EFCs (Sec. 5.3). Consequently, the use of superconducting EFCs is adopted for the scoping phase study. A more detailed analysis of the superconducting EFC performance during the plasma transients is underway. An additional constraint is imposed to use only a single pair of EFCs positioned not to interfere with vertical or horizontal movement of the first wall, blanket, shield, and TFC assembly during maintenance procedures.

The steady-state EFC currents are determined by equating on-axis EFC vacuum field to the vertical field required for toroidal equilibrium. The required vertical field, B_v , is given by [8,48]

$$B_v = \frac{\nu_0 I_\phi}{4\pi R_T} \left[\ln \left(\frac{8R_T}{r_p} \right) + \frac{l_i}{2} + \beta_\theta - 1.5 \right], \quad (4.4.-1)$$

where R_T and r_p are the plasma major and minor radii, respectively, I_ϕ is the steady-state plasma current, β_θ is the poloidal beta, and l_i is the internal inductance per unit length of plasma. Typically, $l_i \approx 1$ for RFP field and current profiles. The position of the EFCs is determined such that the value of the decay-index,

$$n \equiv - \frac{\partial(\ln B_v)}{\partial(\ln R)} \approx \frac{R_T}{r_p} \frac{B_v(R_T - r_p) - B_v(R_T + r_p)}{B_v(R_T - r_p) + B_v(R_T + r_p)}, \quad (4.4.-2)$$

remains in the range $0 < n < 1.5$ [49]. Having a circular plasma cross section further constrains the index [48] to $0 < n \leq 0.65$, which is the criterion used herein. The resulting EFC design is shown in Fig. 4.4.-1 and the associated parameters are given in Tables 4.4.-I to 4.4.-III.

4.4.1.2. Ohmic-Heating Coils (OHCs)

The most efficient coupling of OHCs to the plasma is obtained with the "close-fitting" OHC configuration shown in Fig. 4.4.-1. Such a configuration requires the removal of most of the OHCs in the upper-half plane to gain access to the reactor torus for (single-piece) maintenance purposes. In order to eliminate the need for coil movement for maintenance purposes, one can array the

Fig. 4.4.-1. A cross-sectional view of the "close-fitting" poloidal-field coil set for the 18-MW/m² Strawman design. The locations of the toroidal-field (TF) coils (if IBC is not used), the first wall, blanket, and shield assembly (FW/B/S), and the plasma are shown in addition to the equilibrium-field (EF) coils and the ohmic-heating (OH) coils.

Fig. 4.4.-2. A cross-sectional view of the "vertical-stack" OHC configuration considered for the 18-MW/m² Strawman design. The locations of the toroidal-field (TF) coils (if IBC is not used), the first wall, blanket, and shield assembly (FW/B/S), and the plasma are shown in addition to the equilibrium-field (EF) coils and the ohmic-heating (OH) coils.

TABLE 4.4.-1

Function	R (m)	±z (m)	ΔR (m)	Δz (m)	A (m ²)	I ^(a) (MA)	j ^(a) (MA/m ²)	Mass ^(b) (tonne)
BF	6.4959	2.4873	0.6973	0.6973	0.4862	8.882	18.27	146.1
OH1	5.8699	1.9473	0.4000	0.4000	0.1600	-2.057	12.86	43.4
2	3.9472	2.2299	0.4100	0.4100	0.1681	-2.057	12.24	30.7
3	3.1958	1.8533	0.3000	0.5000	0.1500	-2.057	13.71	22.2
4	2.7905	1.4625	0.2000	0.5000	0.1000	-2.057	20.57	12.9
5	2.5503	1.1031	0.2500	0.3300	0.0825	-2.057	24.94	9.7
6	2.4028	0.7705	0.3300	0.3000	0.0990	-2.057	20.78	11.0
7	2.3163	0.4557	0.3200	0.3000	0.0960	-2.057	21.43	10.3
8	2.2759	0.1508	0.3300	0.3000	0.0990	-2.057	20.78	10.4

(a) Values are at steady state for the EFCs and at the back bias for the OHCs.

(b) A density of 7.36 tonne/m³ is assumed.

OHCs into two vertical stacks with one stack positioned inboard of the torus and the other outboard (Fig. 4.4.-2). Both the "close-fitting" and the "vertical-stacks" configurations were analyzed in the scoping phase study using the code CCOIL [46,50].

The locations of the close-fitting OHCs are determined in CCOIL by first specifying an arc, which is defined as a segment of an ellipse that is symmetric about the equatorial plane, upon which the coils are to be arrayed. The Fourier coefficients for a series representation of the current distribution on the arc that excludes flux from the entire plasma cross section are then determined. Assuming equal-current coils to facilitate series electrical connection of the coils, the current distribution is integrated along the arc to yield the OHC current-center locations.

The locations of the vertical-stack OHCs are taken to be uniformly spaced within a stack and each stack is positioned adjacent to the TFCs in the equatorial plane to maximize vertical access to the torus (Fig. 4.4.-2). The coils within a stack are of equal current. The current distribution between stacks is determined by requiring the coil set exhibit an on-axis field null in order to facilitate RFP early breakdown/formation (Sec. 4.5.1).

The single-turn back-bias and forward-bias OHC currents, I_{OH}^- and I_{OH}^+ , are determined by imposing inductive flux conservation and ignoring the resistive losses, as given below:

TABLE 4.4.-II

PFC Parameters for the 18-MW/m² Strawman Design

Parameter	Value
EFC current (MA)(a)	17.8
EFC volume (m ³)	39.7
EFC mass (tonne)	292.1
EFC joule losses (MW)(a)	(378.4 NC)(0.0 SC)
EFC peak field (T)(a)	5.9
EFC current density (MA/m ²)(a)	18.3
Vertical field index	0.16
OHC current (MA)	
♦ back bias	-32.9
♦ forward bias	15.1
OHC volume	40.9
OHC mass (tonne)	301.2
OHC joule losses (MW)	(68.1 ^(c))/321.6 ^(b)
OHC von Mises stress (MPa)(b)	215.6
OHC peak field (T)(b)	8.3
OHC current density (MA/m ²)(b)	(12.2-24.9)
OHC stray vertical field (mT)(b)	1.25 (<2.45 ^(d))
PFC transparency (%)	67.2

(a) Steady-state values.

(b) Back-bias values.

(c) Forward-bias values.

(d) Stray vertical field constraint (see Sec. 4.5.1).

$$L_p I_\phi = M_{EF,p} I_{EF} + M_{OH,p} (I_{OH}^+ - I_{OH}^-), \quad (4.4.-3)$$

where I_ϕ and I_{EF} are the steady-state plasma and EFC currents, respectively, $M_{i,j}$ is the mutual inductance between the i^{th} and j^{th} circuit elements, and L_p is the plasma self-inductance. An additional constraint of a bipolar current swing, based on the startup scenario described in Sec. 4.5, is imposed to minimize the energy-storage and power-handling requirements:

TABLE 4.3.-III

PFC Circuit Parameters for the 18-MW/m² Strawman Design

<u>Parameter</u>	<u>Value</u>
Self-Inductances (μH)	
♦ L _p	13.26
♦ L _{EF}	14.80
♦ L _{OH}	3.39
Mutual Inductances (μH)	
♦ M _{OH,p}	3.47
♦ M _{OH,EF}	3.08
♦ M _{EF,p}	3.87
Current Levels (MA)	
♦ I _φ	17.75
♦ I _{EF}	17.76
♦ ΔI _{OH}	48.07
Magnetic Fluxes (Wb)	
♦ Plasma	235.
♦ EFC	68.
♦ OHC	167.

$$|I_{OH}^-| = I_{OH}^+ + I_{EF} \quad (4.4.-4)$$

The mutual inductances used in Eq. (4.4.-3) are estimated from the following formula for two coaxial hoops [51]:

$$M_{i,j} = \frac{2\mu_0(r_i r_j)^{1/2}}{k} [(1 - k^2/2)K(k) - E(k)] \quad (4.4.-5)$$

where

$$k = \frac{4r_i r_j}{(r_i + r_j)^2 + \Delta_z^2} \quad (4.4.-6)$$

The radii of the ith and jth hoops are r_i and r_j, Δ_z is the distance between the

two parallel coil planes, and $K(k)$ and $E(k)$ are the complete elliptic integrals of the first and second kinds, respectively. Each coil in a coil set is simulated by 100 hoops to ensure that a high degree of accuracy is obtained, especially for coil self-inductances. The plasma, however, is simulated with a single-hoop current, which is positioned in the equatorial plane at a major radius, R'_T , that includes a Shafranov shift [8]:

$$R'_T = R_T + \frac{r_w^2}{2R_T} \left[\left(\beta_\theta + \frac{1}{2} - \frac{1}{2} \right) (1 - x^2) - \ln x \right], \quad (4.4.-7)$$

where R_T is the torus major radius, r_w is the first-wall minor radius, and $x = r_p/r_w$ is the ratio of plasma and first-wall minor radii. When the calculation of the single-turn mutual inductance involves a coil set, a summation is performed over each hoop in each coil in the set; for example,

$$M_{OH,j} = \sum_{i=1}^{n_{OH}} M_{i,j}, \quad (4.4.-8)$$

where $n_{OH} = 100 N_{OH}$ is the number of hoops used to simulate the number of OHCs, N_{OH} . The single-turn self-inductances of the coil sets are determined by application of the formula for mutual inductance with both summations over the same coil set as follows:

$$L_k = \sum_i^{n_k} \sum_j^{n_k} M_{i,j}. \quad (4.4.-9)$$

where all of the filaments are equi-spaced and carry equal currents. The singular element $M_{i,i}$ is replaced with the self-inductance of a wire of finite minor radius given by [51]

$$M_{i,i} = \mu_0 R_i \left[\ln \left(\frac{8R_i}{\Delta} \right) - 1.75 \right], \quad (4.4.-10)$$

where R_i is the major radius of the hoop used to simulate a coil and Δ is the separation between the filaments and assumed to be 0.01 m. The plasma self-

inductance is expressed as a sum of an external inductance, $L_{p,ex}$, and an internal inductance, $L_{p,in}$, (i.e., $L_p = L_{p,in} + L_{p,ex}$). The external inductance is taken to be that for a wire with the same dimensions as the plasma [51]:

$$L_{p,ex} = \mu_0 R_T \left[\ln \left(\frac{8R_T}{r_p} \right) - 2 \right] . \quad (4.4.-11)$$

The internal inductance is derived from results of a one-dimensional equilibrium calculation [46] and is given by

$$L_{p,in} = \left[2\pi R_T (W_\Theta + W_\Phi) - \frac{\phi^2}{2L_C} \right] (I_\phi)^{-2} , \quad (4.4.-12)$$

where

$$W_i = \frac{\pi}{\mu_0} \int_0^{r_p} B_i^2(r) r dr , \quad (4.4.-13)$$

$$\Phi = 2\pi \int_0^{r_p} B_\phi(r) r dr , \quad (4.4.-14)$$

$$L_0 = \frac{\mu_0 r_p^2}{2R_T} . \quad (4.4.-15)$$

The above algorithms have been used to analyze the "close-fitting" and "vertical-stack" configurations. An additional constraint on the OHC design is the maximum level of the stray vertical field during breakdown, as described in Sec. 4.5.1. This constraint is in the form of a maximum value for the ratio of the stray vertical field to the initial toroidal field, $B_{\phi 0}$, given by Eq. (4.5.-1). For a given $B_{\phi 0}$, this constraint limits the stray vertical field produced by the OH coil set.

For the case of the vertical-stack configuration, the coil geometry is fixed by maintenance considerations. For the close-fitting configurations, however, the coil geometry is not placed under such a constraint and can be manipulated through the arc parameters to reduce the stray vertical field. The profiles of stray vertical field in the equatorial plane are shown in Figs. 4.4.-3 and 4.4.-4 in the back-bias condition for the close-fitting and vertical-stack configurations, respectively. Only the close-fitting configuration complies with the stray-vertical-field constraint by virtue of the freedom to move OHCs over the torus.

In principle, the initial toroidal field, $B_{\phi 0}$, can be increased to ensure that the "vertical-stack" configuration complies with the stray-vertical-field constraint (4.5.-1). However, any increase in $B_{\phi 0}$ would result in increases in the OHC volt-second consumption and in the formation energy and power. For example, with the vertical-stack configuration of Fig. 4.4.-2, $B_{\phi 0}$ must be increased by one to two orders of magnitude. A single order of magnitude increase in the value of $B_{\phi 0}$ would result in a volt-second consumption during formation ≥ 80 V-s, a formation energy ≥ 200 MJ, and a formation power ≥ 1 GW (Sec. 4.5.1). Consequently, a maximum value of 2.45 mT for the stray vertical field is adopted here.

A secondary constraint that the OHC set exhibit a field null within the plasma chamber, which is demonstrated in Fig. 4.4.-3 and Fig. 4.4.-4 for both configurations. Consequently, the close-fitting configuration of Fig. 4.4.-1 and Tables 4.4.-I to 4.4.-III has been adopted for the PFC design.

The circuit parameters of Table 4.4.-III, then, are used in the time-dependent simulation, which includes plasma-resistance effects and is discussed in Sec. 4.5.2, to yield more accurate determinations of the following back-bias OHC parameters: current, current density, joule losses, von Mises stress, peak field, and stray vertical field. Design iterations between the time-dependent simulation and CC0IL have not been performed.

4.4.2. Toroidal-Field Coil (TFC) System

Two options are being considered for the generation of the toroidal field. The first option is the use of normal-conducting, copper TFCs, positioned outside of the blanket and shield as is shown in Fig. 4.4.-1. In order to permit service access to the first wall, blanket, divertor, and plasma chamber, the TFCs must be discretized rather than forming a continuous toroidal shell. The discretization of the TFCs, however, introduces a toroidal-field ripple,

VERTICAL FIELD PROFILE AT BACK BIAS

Fig. 4.4.-3. The stray-vertical-field profile in the equatorial plane for the close-fitting OHC configuration shown in Fig. 4.4.-1. Also shown are the bands for the allowed vertical field when the field null is on axis (clear) and when the field null is off-axis (shaded) as is the case for the close-fitting configuration.

Fig. 4.4.-4. The stray-vertical-field profile in the equatorial plane for the vertical-stack configuration shown in Fig. 4.4.-2. Also shown is a 5 mT band to calibrate the extent of the violation of the stray vertical field constraint. Note the different scales than used in Fig. 4.4.-3.

which in turn causes magnetic islands within the edge-plasma region. Particles and energy flow freely within this island structure, density or temperature gradients cannot exist radially across the islands and, depending on island size, plasma confinement is degraded. To ensure that confinement is not adversely affected by the ripple, the radial extent of the islands is required to be small compared to the radial distance between the reversal surface and the plasma surface. This region is thought [1] to be the most responsible for confinement in an RFP. Consequently, the management of the ripple is a major factor in the design of the TFCs and is addressed in this section.

The design issues associated with the toroidal-field ripple has led, in part, to the consideration of a second TFC option, the integrated blanket/coil (IBC) concept [47] (Sec. 8.2.5). The IBC concept combines blanket and TFC functions by using a liquid metal which breeds tritium to fulfill the blanket function, flows so it can remove the energy deposited within it, and conducts electricity to fulfill the TFC function. The combination of functions eliminates the need for coolant penetrations through the conductor. With the major penetrations eliminated, the TFC current channel approaches a continuous toroidal shell which introduces no toroidal-field ripple. However, the IBC has a number of non-magnetic issues, as discussed in Sec. 8.2.5.

An estimate of the magnetic-island size produced by a discretized copper TFC set is given by the following formula for the radial thickness of an island [52]:

$$\Delta r = 4 \left[\frac{r \Delta B_R}{n B_\theta (dq/dr)} \right]^{1/2}, \quad (4.4.-16)$$

where r is the minor radius of the resonant surface, ΔB_R is the amplitude of the radial magnetic-field perturbation, n is the toroidal mode number of the resonant surface, B_θ is the poloidal field at the resonance, and the derivative of the safety factor, dq/dr , is evaluated at the resonant surface. The q -profile in the edge-plasma region can be taken to be linear:

$$\frac{dq}{dr} = \frac{q}{r_p - r_r}, \quad (4.4.-17)$$

where r_r and r_p are the minor radii of the reversal and plasma surfaces. Assuming that the toroidal mode number of the resonance is equal to the number of TFCs, N_{TF} , the island-width criterion becomes

$$\frac{\Delta r}{r_p} \approx 4 \left[\frac{\Delta B_R (1 - r_r/r_p)}{N_{TF} B_\theta q} \right]^{1/2} < (1 - r_r/r_p) . \quad (4.4.-18)$$

An estimate of the radial magnetic field arising from the ripple, ΔB_R , can be obtained from two-dimensional field-line tracings at the plasma surface with only the TFCs simulated. Such simulations [46] have yet to be performed for the TITAN design and are also of limited application because N_{TF} must exceed 100 before the primary resonance, N_{TF}^{-1} , appears within the plasma for the 18 MW/m² strawman design (Sec. 5.3.2) and Eq. (4.4.-16) becomes applicable. If the resonant surface is not in the plasma, there will be no islands in the plasma with a toroidal mode number of N_{TF} . However, islands resulting from higher order resonances may be present and are best uncovered by three-dimensional field-line tracings.

A more accurate assessment of island widths can be obtained from three-dimensional field-line tracings which simulate the toroidal, radial, and poloidal components of the magnetic field produced by the plasma, PFCs, and TFCs. Although such simulations remain to be done for this study, previous simulations [53] indicate that islands can be kept acceptably small if $\Delta B_R/B_\theta < 0.003$, which is the criterion used for the ZT-H design [54]. Scaling the number of TFCs from that design with aspect ratio, $A = R_T/r_p$, indicates that $N_{TF} \geq 28$ is required for the 18 MW/m² strawman design. Using the largest rectangular cross-sectional coils that will fit into the space allocated for the TFCs results in the preliminary TFC design shown in Fig. 4.4.-5 and described in Table 4.4.-IV. This design with $N_{TF} = 28$ will accommodate four or seven divertors that are equally spaced toroidally. If five or six divertors are needed to meet a divertor heat-load limit, then a minimum of $N_{TF} = 30$ is required to achieve equal spacing of the divertors. Note that the current density in the TFCs for this preliminary design is higher than predicted by the systems code in Sec. 5.3.2: 35.9 MA/m² compared to 17.4 MA/m². This increase in current density is the result of using coils with a rectangular cross section as opposed to specifying a radial build and assuming toroidal symmetry, as was done for the systems code (Sec. 5.2). The loss of conductor cross-sectional area

TABLE 4.4.-IV
PRELIMINARY TFC DESIGN PARAMETERS

<u>Parameter</u>	<u>Value</u>
Number of TFCs, N_{TF}	28
Major radius, R_{TF} (m)	3.892 (3.892)
Minor radius, r_{TF} (m)	1.431 (1.438)
Radial thickness (mm)	12.8 (26.0)
Toroidal thickness (mm)	548.0 (545.0)
Current per coil (kA)	251.6
Current density (MA/m ²)	35.9 (17.8)
Total ohmic power (MW)	65.0 (32.3)

Values in the parentheses are the results of increasing the radial space allocated for the TFCs from 0.028 m (Sec. 5.3) to 0.041 m.

results in a doubling of the ohmic power dissipated in the TFCs over that predicted by the systems code. The current density value of 17.4 MA/m², predicted by the systems code, can be recovered if the TFCs are made thicker in the radial direction by 0.013 m as indicated in Table 4.4.-IV. The effect of thicker the TFCs upon the PFC design should be negligible.

4.5. PLASMA/CIRCUIT SIMULATION

Early RFP reactor studies [46,53,55,56] have taken guidance from the results of (steady-state) parametric system models (Sec. 5) to provide initial conditions to a time-dependent, plasma/circuit simulation code that in turn models the start-up, approach to, achievement of, and maintenance of DT ignition. These early ignition/burn simulations generally assumed the existence of a low-current RFP "target" plasma ($I_\phi \approx 0.1$ MA, $T \approx 0.1$ keV, $n \leq 10^{20}$ m⁻³, $F \approx -0.1$, $\Theta \approx 1.5$) onto which a bipolar OHC swing was imposed to ramp the plasma to an ohmic ignition.

Figure 4.5.-1 illustrates the general start-up scenario assumed [46], with the bulk of the start-up energy being provided from the electrical grid; power and voltage requirements were appropriately constrained, as were OHC back-bias stresses needed to provide all poloidal flux (inductive and resistive) requirements. Hence, the TF and OH coil and power supply designs are coupled through the formation process by the breakdown constraint ($B_V/B_{\phi 0}$), reversal time, τ_R , poloidal-flux consumption during formation (τ_R , $B_{\phi 0}$, geometry), and

Equatorial-Plane View of TITAN RFP

Fig. 4.4.-5. An equatorial-plane view of a quadrant of the preliminary TFC design. Also shown are the first wall, blanket, and shield assembly (FW/B/S), the plasma, and the poloidal-field (PF) coil envelope.

Fig. 4.5.-1. Schematic diagram of a simplified RFP start-up scenario showing the following start-up stages: OHC charge, resistive flux transfer and current transfer to the plasma, grid drive to ignition, and burn, followed by OFCD to sustained power generation.

the degree of subsequent toroidal-flux generation possible by the RFP dynamo. Existing experimental evidence has been summarized, primarily from ZT-40M [57], and used to establish RFP breakdown/formation "windows" to estimate coil and circuit parameters and to provide initial conditions for the current ramp to ohmic ignition and burn. Fig. 4.5.-2 schematically depicts a more detailed view of the RFP start-up, with Sec. 4.5.1 dealing primarily with this formation phase.

It became evident towards the end of the early RFP reactor studies [46,53] and during the earliest phase of the TITAN study that both the TFC and PFC (OHC + EFC) design limits would be determined more by the plasma breakdown, formation, and ramp-up transients than by the steady-state operational phase. The desire to use the RFP dynamo to generate internal toroidal flux, rather than injecting all the toroidal flux by the TFCs, and the bias stress and power strongly influence the TFC and OHC designs. Furthermore, the PFC configuration determines the coupling of OHC with the plasma, the magnitude of the stray vertical field, and the degree of multipolarity of field nulls in the plasma chamber. These in turn influence the breakdown and RFP formation conditions through the amount of initial (vacuum) toroidal field, $B_{\phi 0}$, and ultimately affect the TFC design.

Section 4.5.1 describes and applies the existing RFP experimental data base for RFP breakdown and formation. Using the initial RFP conditions generated in Sec. 4.5.1, the plasma start-up (ramp-up $\rightarrow$ ignition $\rightarrow$ burn) is simulated by means of a zero-dimensional plasma/circuit model in Sec. 4.5.2. Recent developments to model eddy-current effects in the engineering structure that surrounds the RFP are given in Sec. 4.5.3.

4.5.1. Breakdown and RFP Formation

A body of experimental data is beginning to accumulate, which better defines the formation "window" and associated PFC/TFC circuit requirements for the TITAN reactor, as well as for other RFP devices [58,59]. Although, much of this information is not theoretically understood fully and extrapolation from ZT-40M-class experiments to a reactor is uncertain, this information and experience nevertheless is assimilated for the first time and used as part of the TITAN study. The formation phase is shown schematically on Fig. 4.5.-3, which gives more detail than shown in Fig. 4.5.-2 as well as introducing key notation. Generally, matched-mode RFP formation is assumed, wherein the RFP is externally driven to match exactly the reversed toroidal field generated by the

Fig. 4.5.-2. Schematic diagram of RFP start-up phases.

Fig. 4.5.-3. Matched-mode RFP formation leading to the initial conditions, θ_0 , F_0 , and $I_{\phi 0}$ used as initial conditions for start-up, ignition, and burn simulations.

plasma (no toroidal flux change across TFCs). The formation phase of the RFP is characterized by the following experimentally observed behaviors:

- * upper and lower density limits define a region outside of which poor or no RFP formation occurs.
- * minimum plasma current (or possibly current density, in that size variations are limited in present-day experiments) below which robust RFPs cannot be formed.
- * minimum limit on the toroidal electric field, E_ϕ , or ratio of E_ϕ to initial filling pressure, E_ϕ/P_0 , to ensure breakdown.
- * upper limit on the formation time, τ_R .
- * limits imposed on initial (vacuum) toroidal bias magnetic field, $B_{\phi 0}$.

In addition to setting windows for RFP formation, relationships between these variables and the poloidal flux and energy consumption during formation have been derived [57]. These constraints are briefly summarized in the following subsections and formulated into a simplified breakdown and formation model (Sec. 4.5.1.12) that in turn is evaluated to provide initial conditions for the simulation of plasma start-up, ignition and burn (Sec. 4.5.2).

4.5.1.1. Plasma Breakdown

Plasma discharge and subsequent RFP formation generally occurs for values of the ratio of toroidal electric field to initial filling pressure, E_ϕ/P_0 , that are similar to tokamak values and generally are close to electron runaway condition. For example, JET reports [60] $E_\phi/P_0 \geq 0.66 \times 10^4$ V/m torr compared to $1-2 \times 10^4$ V/m torr, for ZT-40M [57], which is very close to electron runaway condition. Fig. 4.5.-4 gives typical breakdown/formation characteristics for a range of tokamaks [61] and for ZT-40M. Generally, breakdown and discharge formation is not a problem for RFPs, but the degree of pre-ionization can greatly influence the discharge quality and poloidal-flux consumption [62]. Since stable RFP formation to date appears to require a high initial filling density for the levels of density pump-out experienced in present-day RFPs the generally common E_ϕ/P_0 values for both RFPs and tokamaks give significantly higher values of E_ϕ required to initiate a robust RFP.

4.5.1.2. Stray-Vertical-Field Constraint

As shown in Fig. 4.5.-5, a toroidal field line of strength $B_{\phi 0}$ in the presence of a vertical field B_V will intersect the first wall and prevent the

Fig. 4.5.-4. Typical breakdown curves for a) tokamak [61] and b) RFP [57] formation.

Fig. 4.5.-5. Schematic illustration of a field line in the presence of a vertical field.

formation of a continuous discharge if the ratio $B_v/B_{\phi 0}$ is too large. The condition for the confinement of a single toroidal trajectory with a field null at a minor radial position r_0 is given by

$$\frac{B_v}{B_{\phi 0}} \leq \frac{\epsilon}{2\pi} \left(1 - \left(\frac{r_0}{r_p} \right)^2 \right)^{1/2}, \quad (4.5.-1)$$

where $\epsilon = r_p/R_T$ is the inverse aspect ratio. In addition, a drift constraint has been suggested for JET [60]:

$$E_{\phi}/(B_v/B_{\phi 0}) \geq 10^3 \text{ V/m}. \quad (4.5.-2)$$

The value of B_v and the field-null(s) locations are determined from a vacuum-field calculation using the CCOIL model described in Sec. 4.4.2.

4.5.1.3. Initial Toroidal Flux Constraint

Once the $B_V/B_{\phi 0}$ constraint is established, the relationships between $B_{\phi 0}$ and the average toroidal flux within the initial RFP, $\langle B_{\phi} \rangle$, and hence, F_0 , must be determined (Fig. 4.5.-3). Generally, for matched-mode RFP formation $\langle B_{\phi} \rangle \approx B_{\phi 0}$. Figure 4.5.-6 shows the relationship between $B_{\phi 0}$ and $\langle B_{\phi} \rangle$ for a range of ZT-40 discharges [57,63] illustrating the experimental basis for this assumption. Given that $\langle B_{\phi} \rangle$ can be determined and the initial pinch parameter, Θ_0 , is specified, the initial (minimum) RFP current or current density (given r_p) is determined from the following:

$$I_{\phi 0} = 5r_p \Theta_0 \langle B_{\phi} \rangle . \quad (4.5.-3)$$

4.5.1.4. Initial Current-Density Constraint

Although Eq. (4.5.-3) gives a means to determine a current density that is consistent with the vertical-field constraint previously described, other, more dominant constraints may exist. For example, the ZT-40M experiment exhibits a minimum-current-density limit, which translates to $j_{\phi 0} \geq 0.4 \text{ MA/m}^2$, below which RFP formation is difficult. Although not well understood, the application of such a constraint to the TITAN represents a conservative connection to experiment. Secondly, a number of RFP experiments [1] have shown an impurity burn-through constraint, typical of which is shown in Fig. 4.5.-7 for ZT-40M. For these conditions, burn-through requires

$$\frac{j_{\phi}}{n} \geq 6 \times 10^{19} \text{ MA-m} . \quad (4.5.-4)$$

4.5.1.5. Minimum-Density Constraints

Generally if for a given initial filling pressure the pump-out of density is too great prior to toroidal field reversal, unreliable RFP formation occurs [57], as is shown in Fig. 4.5.-8a. Similarly, for a given initial filling pressure, P_0 , a maximum initial bias field $B_{\phi 0}$ is found above which RFP formation does not occur [57], as is shown in Fig. 4.5.-8b. Although RFPs form at lower values of $B_{\phi 0}$, these RFPs require excessive poloidal-flux consumption,

Fig. 4.5.-6. Relationship between B_{00} and $\langle B_0 \rangle$ for a range of ZT-40M discharges [57,63] where robust RFP formation occurred, as well as no RFP formation as very shallow, spheromak-like, RFP were formed.

Fig. 4.5.-7. Impurity-burn-through constraint for ZT-40M [57].

Fig. 4.5.-8a. Relationship between filling pressure and degree of pump-out, below which poor RFP formation is observed [57].

Fig. 4.5.-8b. Initial toroidal bias fields above which no RFP formation is found for a given filling pressure in ZT-40M [57].

Fig. 4.5.-8c. Cross-correlation plot of Figs. 4.5.-8a and 4.5.-8b.

with Fig. 4.5.-8b showing a "horn of plenty" for the ZT-40M conditions examined. It should be noted that a variable and poorly controlled wall condition creates hystereses and related unknown effects in many of these data correlations. Figures 4.5.-8a and 4.5.-8b have been combined in Fig. 4.5.-8c in an attempt to eliminate the filling-pressure variable and perhaps to reduce the impact of these unresolved wall effects on the data. The result is a relationship between average plasma density and initial bias field that is forced to assure robust RFP formation. The cross-correlation plot given in Fig. 4.5.-8c is fitted with the following functions to give the critical density, n_c versus $B_{\phi 0}$ relationship.

$$n_c (10^{20} m^{-3}) = 0.10$$

$$\text{for } B_{\phi 0} < 36 \text{ mT}$$

(4.5.-5)

$$n_c (10^{20} m^{-3}) = 2.78 \times 10^{-3} B_{\phi 0}$$

$$\text{for } B_{\phi 0} > 36 \text{ mT}$$

4.5.1.6. Reversal Parameter

Generally, Θ_0 and the initial poloidal beta, $\beta_{\Theta 0}$, are specified as independent goals or targets, and, along with the assumption of a μ profile (e.g., modified Bessel function model), the appropriate F- Θ or Taylor diagram is determined. This derived F- Θ diagram for the assumed initial $\beta_{\Theta 0}$ and $\mu(r)$ values, then determines F_0 .

4.5.1.7. Density Pump-out

The degree of density reduction between the initial filling pressure and the final RFP formation is poorly understood and strongly dependent on wall pre-conditioning. Hence, the pump-out is treated parametrically in terms of the ratio of initial filling density, n_0 , to the final RFP plasma density $n > n_c$. The assumption is made that pump-out and P_0 can be minimized, thereby minimizing E_ϕ at start-up.

4.5.1.8. Toroidal Electric Field

The toroidal electric-field constraint, E_ϕ/P_0 , was discussed earlier in Sec. 4.5.1.1 in terms of breakdown and discharge formation. In characterizing the TITAN initial conditions, the following four possible constraints were considered:

$$\begin{array}{ll}
 E_\phi/P_0 \text{ (V/m torr)} = 0.66 \times 10^3 & \text{(JET breakdown)} \\
 E_\phi/(B_V/B_{\phi 0}) \text{ (V/m)} = 10^3 & \text{(JET drift)} \\
 E_\phi/P_0 \text{ (V/m torr)} \leq 10^4 & \text{(electron runaway)} \\
 E_\phi/P_0 \text{ (V/m torr)} \approx 2 \times 10^4 & \text{(ZT-40M) \quad (4.5.-6)}
 \end{array}$$

Hence, four sets of results are presented according to the above four possibilities.

4.5.1.9. Initial Current Risetime

Neglecting compressional and resistive voltages during the initial phase ($I_{\phi 0} \approx 0$) of the RFP formation, the current risetime is approximated by the sinusoidal, inductive wave form to give the following expression for τ_R :

$$\frac{I_{\phi 0}}{\tau_R} \approx \frac{2}{\pi} \frac{V_{\phi 0}}{L_p}, \quad (4.5.-7)$$

where $V_{\phi 0} \approx E_{\phi}^2 R_T$,

$$L_p = \mu_0 R_T \left[\ln \left(\frac{6R_T}{r_p} \right) - 2 + l_i \right], \quad (4.5.-8)$$

and the plasma internal inductance per unit length, $l_i = 1 - (2F + 1)(1 - F)/2\theta^2$ for the Bessel-function model, or $l_i \approx 1$ for the modified Bessel-function model. For the ZT-40M experiment, $I_{\phi 0}/\tau_R \approx 30-40$ MA/s. The risetime τ_R should then be compared to an effective shell/liner time-constant, $\tau_L = \mu_0 r_w \delta_L / 2\eta_L$, where r_w is the first-wall radius of thickness δ_L and electrical resistivity η_L .

4.5.1.10. Poloidal-Flux and Energy Consumption During RFP Formation

Figures 4.5.-9a and 4.5.-9b give the dependence of poloidal-flux and energy consumption as a function of initial current risetime for ZT-40M [57]. Separating the resistive and inductive components of the flux consumption and correcting for geometry while using the following expression for plasma resistivity at formation [57]:

$$\eta_{AV} (\Omega m) = 1.4 \frac{r_p^2}{R_T I_{\phi 0}^{0.64}}, \quad (4.5.-9)$$

the flux consumption during formation becomes

$$\int_0^{\tau_R} V_{\phi} dt = L_p I_{\phi 0} + 9.04 \times 10^3 R_T I_{\phi 0}^{0.36} \frac{\tau_R}{r_p^{0.72}}, \quad (4.5.-10)$$

where L_p is again given by Eq. (4.5.-8) using the modified Bessel function model. The associated energy consumption is given by

Fig. 4.5.-9a. Flux consumption during formation as a function of risetime for ZT-40M.

Fig. 4.5.-9b. Energy consumption during formation as a function of risetime for ZT-40M.

$$W_{B\phi} = \frac{1}{2} I_{\phi 0} \int_0^{\tau_R} V_{\phi} dt . \quad (4.5.-11)$$

4.5.1.11. Toroidal-Field Coil During Formation

Given the current-radius of the TFC, r_{TF} , and the parameters self-consistently determined from the previous subsections, the toroidal flux, ϕ , and associated single-turn TFC voltage, $V_{\theta} = d\phi/dt$, can be estimated from the following expression

$$V_{\theta}(V/m) = \frac{\pi r_p}{5\Theta_0} \left\{ \frac{B_{\phi 0}}{\langle B_{\phi} \rangle} - 1 + \left[\left(\frac{r_{TF}}{r_p} \right)^2 - 1 \right] \left(\frac{B_{\phi 0}}{\langle B_{\phi} \rangle} - F_0 \right) \right\} \frac{I_{\phi 0}}{\tau_R} . \quad (4.5.-12)$$

From this expression, the stored energy and required TFC circuit power can be readily obtained.

4.5.1.12. Calculational Algorithm and Preliminary Result

The set of constraints and/or options suggested in the previous eleven subsections has been combined into a calculation format and algorithm shown on Fig. 4.5.-10. The overall procedure begins with cost-optimized estimates provided by the parametric systems analysis (PSA) model (Sec. 5). This first geometry estimate is fed to the CCOIL code (Sec. 4.4), which estimates all aspects of the PFC configuration based on poloidal-flux balance and bipolar start-up. In addition to back- and forward-biased currents, inductances, and coil positions, CCOIL provides the multipolarity and vertical-field characteristics of a given (optimized) OBC set that is then used in the aforementioned formation model. The time-dependent plasma/circuit simulation code, which uses CCOIL output, then is used to gain a more accurate estimate of PFC flux consumption and associated back-biased stress and power consumption. As shown at the top of Fig. 4.5.-10, a closely coupled interaction between the PSA, CCOIL and BURN codes provides a well converged estimate of the TITAN design prior to examination and optimization of the formation phase described in this section. The sequence shown in Fig. 4.5.-10 is then followed, leading to a

Fig. 4.5.-10. Calculational algorithm for the self-consistent determination of TITAN RFP formation conditions.

design for the TFC and OHC as dictated by RFP formation, that may or may not require further optimization through the PSA → CCOIL → BURN sequence.

Generally, the constraints imposed by the RFP formation physics, when combined, give the following global constraints on τ_R , n/n_0 , and v_θ/v_ϕ :

$$\tau_R \geq \frac{708\pi}{4} r_p^2 \frac{L_p}{R_T} \frac{(10^{20} j_{\phi/n_0})_c}{E_\phi/P_0}, \quad (4.5.-13a)$$

$$\frac{n}{n_0} \geq \frac{2.78}{5\Theta_0} r_p (10^{20} j_{\phi/n_0})_c, \quad (4.5.-13b)$$

$$\frac{v_\theta}{v_\phi} = \frac{2r_p}{L_p\Theta_0} \left[\frac{B_{\phi 0}}{\langle B_\phi \rangle} - 1 + \left\{ \left(\frac{r_{TF}}{r_p} \right)^2 - 1 \right\} \left[\frac{B_{\phi p}}{\langle B_\phi \rangle} - 1 \right] \right] \quad (4.5.-13c)$$

For the four possible means to determine E_ϕ/P_0 listed in Eq. 4.5.-6, Table 4.5.-I gives the physics and device parameters and the associated start-up conditions. The (JET) "drift" or the "runaway" conditions listed in Table 4.5.-I are considered most typical of present-day experiments and are used as initial conditions for the start-up, ignition, and burn simulation of the TITAN design objective given in the following section.

A main objective of the foregoing analysis is to estimate the impact of stray vertical field on the RFP formation requirements. Using the fourth column in Table 4.5.-I as a baseline, the computational algorithm summarized in Fig. 4.5.-10 was exercised while varying B_V . The impact of increasing B_V on the flux, energy, and power consumption is shown in Fig. 4.5.-11, with the design value being $B_V \approx 2.5$ mT. Since the resistive poloidal-flux consumption during the full ramp-up to ignition and burn is ~ 25 Wb ($\sim 10\%$ to total), an additional flux consumption during formation much above this value becomes a concern from the viewpoint of back-bias stress in the OHCs. Hence, B_V much above a few milli-tesla should be avoided.

4.5.2. Plasma Start-up to Ignition and Burn

4.5.2.1. Model and Initial Conditions

The preliminary results given in Table 4.5.-I for the RFP formation phase have been used to estimate initial conditions for the simulation of the current

TABLE 4.5.-I
RFP FORMATION PARAMETERS FOR TITAN

<u>BASIC PHYSICS DATA</u>	<u>Breakdown</u>	<u>Drift</u>	<u>Runaway</u>	<u>$j_{\phi}(\text{exp})$ (0.4 MA/m²)</u>
E_{ϕ}/P_o (kV/m/torr)	6.66	4.01	1.00	1.00
η_{Cu} ($\mu\Omega$ m)	0.0388	0.0388	0.0388	0.0388
η_{SS} ($\mu\Omega$ m)	0.358	0.358	0.358	0.358
$\langle B_{\phi} \rangle / B_{\phi 0}$	1.00	1.00	1.00	1.00
n_c (10^{20} m^{-3})	0.25	0.25	0.25	0.28
n/n_c	1.00	1.00	1.00	1.00
n (10^{20} m^{-3})	0.25	0.25	0.25	0.28
n_o/n	1.00	1.00	1.00	1.00
n_o (10^{20} m^{-3})	0.25	0.25	0.25	0.28
P_o (mtorr)	0.35	0.35	0.35	0.39
j_{ϕ}/n (10^{-20} MA/m)	1.00	1.00	1.00	1.43
<u>MACHINE PARAMETERS</u>				
R_T (m)	3.90	3.90	3.90	3.90
r_p (m)	0.60	0.60	0.60	0.60
r_o (m)	0.4892	0.4892	0.4892	0.4892
F_o	-0.10	-0.10	-0.10	-0.10
$\beta_{\theta o}$	0.10	0.10	0.10	0.10
θ_o	1.523	1.50	1.50	1.50
δ_{SLD} (m)	0.050	0.050	0.050	0.050
δ_{BLK} (m)	0.775	0.775	0.775	0.775
δ_{TFC} (m)	0.028	0.028	0.028	0.028
B_v (mT)	1.2484	1.2484	1.2484	1.2484
ΔI_{OH} (MA)	47.96	47.96	47.96	47.96
R_{OH} ($\mu\Omega$)	0.297	0.297	0.297	0.297

TABLE 4.5.-I (Continued)

START-UP CONDITIONS	Breakdown	Drift	Runaway	$j_{\phi}(\text{exp})$ (0.4 MA/m ²)
$B_V/B_{\phi 0}$ constraint	0.014	0.014	0.014	0.013
$B_V/B_{\phi 0}$ (update)	0.014	0.014	0.014	0.013
$B_{\phi 0}$ (mT)	88.1	88.1	88.1	100.0
B_{ϕ} (mT)	88.1	88.1	88.1	100.0
$B_{\theta 0}$ (mT)	132.1	132.1	132.1	150.0
$B_{\phi R}$ (mT)	8.8	8.8	8.8	-10.0
$I_{\phi 0}$ (MA)	0.40	0.40	0.40	0.45
E_{ϕ} (V/m)	2.30	14.18	3.46	3.93
V_{ϕ} (V)	56.4	347.4	84.7	96.2
L_p (μH)	13.9	13.9	13.9	13.9
dI_{ϕ}/dt (MA/s)	2.6	15.9	3.9	4.4
Max. circuit dI/dt (MA/s)	10.2	10.2	10.2	10.2
τ_R (ms)	153.4	24.9	102.2	1,454.40

RESULTS

Constant resistivity

* flux consumption (Wb)	13.95	6.88	11.13	12.14
* formation energy (MJ)	4.44	1.64	3.32	4.06
* formation power (MW)	28.93	65.62	32.49	39.70
* $j_{\phi 0}$ (MA/m ²)	0.35	0.35	0.35	0.40
* $j_{\phi c}$ (MA/m ²)	0.25	0.25	0.25	0.40

Constant resistivity and j_{ϕ}

* flux consumption (Wb)	39.94	11.11	28.44	30.26
* formation energy (MJ)	14.73	33.09	10.18	12.21
* formation power (MW)	96.09	13.28	99.66	11.95
δ_{L}^{Cu} (mm)	14.6	2.4	9.7	9.7
δ_{L}^{SS} (mm)	134.4	21.8	89.5	89.5
$E_{\phi B_{\phi 0}}/B_V > 1000$ (V/m)	162.49	1000.00	243.95	314.527
$j_{\phi 0}/n_0$ (10^{-20} MA/m)	1.43	1.43	1.43	1.43
$v_D/v_{TH} < 0.01$	0.0076	0.0076	0.0076	0.0071
V_{θ} (V/turn)	3.44	20.89	5.10	5.79
W_{θ} (kJ)	85.5	85.5	85.5	110.3
P_{θ} (MW)	0.56	3.43	0.83	1.1

Fig. 4.5.-11. Dependence of formation flux, energy and power consumption on stray vertical field.

ramp-up to ohmic ignition and burn. Table 4.5.-II summarizes these initial conditions. The time-dependent plasma/circuit simulation code models the post-formation fast current ramp-up (few seconds to $I_\phi \approx 10$ MA) followed by a slower ramp, driven from the grid, to plasma ignition and burn at $I_\phi = 18-20$ MA (Fig. 4.5.-2). The BURN code has been described in detail elsewhere [46,53]; a summary is given in Table 4.5.-III. The results of this zero-dimensional plasma simulation are compared with the one-dimensional MHD simulation in Sec. 4.7.1.

4.5.2.2. Results

The basic results from the zero-dimensional plasma/circuit simulation code are given in Figs. 4.5.-12. The start-up trajectory is shown on the F - Θ diagram in Fig. 4.5.-12a, which targets a design value of Θ and subsequent β_Θ variations cause F to decrease. More recent simulations target on the F value and that allows Θ to drift upward somewhat as beta increases in order to minimize and/or control the impact of formation and start-up on the TFC requirements.

The time-dependent plasma simulation requires that both electron and ion confinement times be specified. As discussed in Sec. 5.2.1, a scaling of the form,

$$\tau_{ce} = C_v I_\phi^v f(\beta_\Theta/\beta_{\Theta c}) \quad (4.5.-13a)$$

$$\tau_{pi} \approx 4\tau_{ce} , \quad (4.5.-13b)$$

is used, wherein, $f(\beta_\Theta/\beta_{\Theta c})$ simulates the experimentally observed RFP beta limited confinement. For the simulation, it is assumed that $f(\beta_\Theta/\beta_{\Theta c}) \approx (\beta_\Theta/\beta_{\Theta c})^m$ with $\beta_{\Theta c} = 0.19$ and $m = 8$. This assumption results in a strong pinning of the plasma beta upon ohmic ignition, but the electrons and ions decouple somewhat in temperature, as is shown in Fig. 4.5.-12b. The various confinement times and powers, as well as $n\tau_E$, are shown as a function of time in Fig. 4.5.-12c. The fueling rate is controlled throughout the start-up to assure that either a) the streaming parameter, $\xi = v_D/v_{TH}$, remains below a specific value safe from electron thermal runaway, or b) that the fueling rate never exceeds three times the loss rate. The crucial fields and stress levels in the OHC are also shown on Fig. 4.5.-12d. The results shown in Fig. 4.5.-12, while indicating no serious problem with ohmic ignition and burn for the scaling used ($v = 1.0$), are not unique and will be subject to modification and re-

TABLE 4.5.-II
PLASMA STARTING CONDITIONS FOR BURN SIMULATIONS

Ion, electron temperature, $T_{i,e}$ (keV)	0.05
Plasma/wall radius ratio, $x = r_p/r_w$	0.923
Plasma current, $I_{\phi 0}$ (MA)	0.45
Plasma current density, $j_{\phi 0}$ (MA/m ²)	0.40
Plasma density, n_{i0} (10^{20} m ⁻³)	0.71
Filling pressure, P_0 (mtorr)	1.0
Plasma kinetic energy, W_{p0} (kJ)	43.1

TABLE 4.5.-III
SUMMARY OF RFP PLASMA/CIRCUIT SIMULATION AND DESIGN CODE

- * Profile-averaged, zero-dimensional, time-dependent.
- * Follows concentration and energies of species from consistent reactions (H, ³He, ⁴He, T), using Fokker-Planck slowing-down model and including Bremsstrahlung, cyclotron, and line radiation.
- * Magnetic-field and current-density profiles computed consistent with assumed $p(r)$ [i.e., $n(r)$, $T(r)$] and $\mu(r)$ profiles.
- * Pellet-refueling model (constant $I_{\phi}/N = j_{\phi}/n$).
- * Complete analysis of PFC circuit, including plasma as a non-linear circuit element, with PFC circuit driving the simulation.
- * One-dimensional thermal and stress analysis performed as a function of time for a composite first wall.
- * Outputs:
 - Time-history of currents, voltages, powers, and energies in PFC and TFC circuits.
 - Self-consistent, 1-D equilibrium for given $p(r)$ [i.e., $n(r)$, $T(r)$], and $\mu(r)$, and associated profile factors for given $T(r)$.
 - Time-history of all plasma parameters, including energetic ion species.
 - Complete reactor energy balance.
 - Time-dependent first-wall response.
 - Reactor parameter file for use in standardized reactor costing.

Fig. 4.5.-12a. Time-dependent results from zero-dimensional plasma/circuit simulation code for TITAN $J \approx 18 \text{ MW/m}^2$ "Strawman" design using the initial conditions given in Table 4.5.-III and the coil geometry shown in Fig. 4.4.-1.

Fig. 4.5.-12b. Time-dependent results from zero-dimensional plasma/circuit simulation code for TITAN $I_w = 18 \text{ MW/m}^2$ "Strawman" design using the initial conditions given in Table 4.5.-III and the coil geometry shown in Fig. 4.4.-1.

Fig. 4.5.-12c. Time-dependent results from zero-dimensional plasma/circuit simulation code for TITAN I_v ≈ 18 MY/m² "Strawman" design using the initial conditions given in Table 4.5.-III and the coil geometry shown in Fig. 4.4.-1.

Fig. 4.5.-12d. Time-dependent results from zero-dimensional plasma/circuit simulation code for TITAN $I_w = 18 \text{ MW/m}^2$ "Strawman" design using the initial conditions given in Table 4.5.-III and the coil geometry shown in Fig. 4.4.-1.

optimization as new insight and understanding is developed in a range of crucial areas such as listed below:

- ♦ RFP formation
- ♦ current drive
- ♦ density (pellet) control
- ♦ impurity control (transients)

4.5.3. Eddy-Current Circuit Modeling

Analysis of plasma circuit interactions determines the plasma response to the externally applied fields. Such analyses are required so that appropriate switching sequences and voltages can be applied to the external circuitry (e.g., PF and TF coils) for various transient plasma operations, such as start-up and shut-down, fractional power operation, and OFCD.

The time-varying electromagnetic fields incurred during the plasma transients induce eddy currents in all conducting material surrounding the FPC, such as, the first wall, liner/conducting shell, vacuum vessel, blanket, shield, and structures. These eddy currents retard and modify the plasma response to externally applied magnetic fields. Furthermore, these eddy currents give rise to magnetic fields affecting the plasma equilibrium, to electromagnetic forces on all conducting materials which carry the eddy currents, and to an additional energy drain from the external circuits to compensate for Joule losses by eddy currents.

Eddy-current modeling is, therefore, a critical and usually the most difficult component of plasma-circuit interaction analyses. The approach to this problem, adopted for the TITAN study, is to divide the conducting material into small strips which simulate the actual eddy-current path and distribution. Each strip is modeled as an element of a complex circuit which also includes the external circuitry and the plasma as an equivalent circuit element. The interaction of these elements with each other is taken into account through the circuit-inductance matrix, $\vec{L}$, containing self and mutual inductances for all elements. The matrix circuit equation describing the evolution of currents in circuit elements, including the plasma itself, can then be written as follows:

$$\frac{d}{dt} \begin{pmatrix} \vec{L} & \vec{I} \end{pmatrix} + R \vec{I} = \vec{V}, \quad (4.5.-14)$$

where $\vec{I}$ and $\vec{V}$ are the column vectors representing the currents and voltages, respectively, and $\vec{R}$ is the diagonal matrix of resistances. For a given time history of voltages and switching sequences, the above matrix equation can be solved to obtain the currents, and then, magnetic fields and power flow through the circuits. In principle, the accuracy of such a procedure should be improved by increasing the number of the equivalent eddy current circuit elements, which, in turn, increases the complexity of the overall circuit analysis as well as the computation time.

The starting point of this procedure is the division of the conducting material into equivalent eddy-current circuit elements. Such a division, however, requires a priori knowledge of eddy-current paths and distributions. As an example, consider a conducting material that contains resistive breaks. These resistive breaks are usually introduced to suppress the magnitude of the eddy currents and associated effects. One can model these resistive breaks as high resistance conductors, as insulators, or a combination thereof. In the "conductor" model, the eddy currents flow through the breaks. The only effect of the resistive break is to increase the resistance of the eddy-current circuit element substantially. In the other extreme of the "insulator" model, the eddy currents do not flow through the breaks but instead turn around and form a "saddle" loop. The most reasonable model of course, is to combine both current paths: a part of the eddy-current flow through and the rest turns around the resistive break. These three models are schematically shown in Fig. 4.5.-13.

A comparison study of these models has been made by Brown, et al. [64], in which the effects of the eddy currents induced in the vacuum vessel of TFTR on the poloidal-field penetration were studied. Four different models for eddy-current circuit elements were used. In each case, the external PF circuits were oscillated in the absence of the plasma, and the resultant vertical field inside the vacuum vessel was calculated and compared to experimental measurements. As expected, the best simulation result was found for the most sophisticated model that properly included both the inductive and resistive contributions of the saddle current and break-paths. Also, satisfactory agreement was obtained with the simplest model, namely, the "conductor" model for the resistive breaks, without taking into account saddle currents. However, the other two simulations with simplified saddle current models did not produce satisfactory results. This work underscores the importance of the insight and a priori knowledge of eddy-current paths and distribution for the analysis.

(a) TOROIDALLY CONTINUOUS PATH

(b) SADDLE CURRENT

(c) COMBINED

Fig. 4.5.-13. Three models of a resistive break, conductor model (a), the resistive model (b), and combination (c). The resistive breaks are the shaded regions and arrows indicate the eddy-current paths.

To study the impact of eddy currents on the plasma response of the TITAN reactor, a circuit-analysis code has been developed. Given a specified FPC geometry and external coil sets, this code divides the FPC into small sectors and models each as an eddy-current circuit element. The self and mutual inductances of the various circuit elements are then calculated, and the overall inductance matrix is constructed. For a given voltage time history or start-up sequence (e.g., given in Fig. 4.5.-1), the circuit solver computes the time-varying currents in each circuit element. Special attention is given to the equilibrium-field-coil (EFC) circuits. At all times, the current in the EF coils should provide the necessary vertical field to maintain the plasma equilibrium. The required vertical field at the magnetic axis is given by the Shafranov formula [8],

$$B_V = \frac{\mu_0 I_\phi}{4\pi R_T} \left[\ln \left(\frac{8R_T}{r_p} \right) + \beta_\theta + \frac{l_i}{2} - 1.5 \right]. \quad (4.5.-15)$$

It has been shown [65] that the Shafranov formula is an accurate measure of the required vertical field for an RFP over a wide range of plasma conditions. In the presence of conducting material, the vertical field produced by the EFC set together with contributions from other external coils and eddy-currents should be equal to the vertical field given by Shafranov formula (4.5.-15). The code computes these contributions to find the current in the EF coil set self-consistently, and then calculates the necessary voltage, power, and switching sequence that has to be applied to the EFC set.

At present, the circuit analysis code uses the "conductor" model for resistive breaks. An upgrade that takes the saddle currents into account is under development. Furthermore, the plasma relaxation via the RFP dynamo couples the poloidal and toroidal-field circuits. Inclusion of this coupling and simulation of OFCD circuitry are also under study. Application of this code to the TITAN reactor start-up and transients is guiding the coil design, FPC engineering analyses, and FPC design integration effort.

4.6. CURRENT DRIVE

At full plasma current the nominal 18 MW/m^2 baseline TITAN design supports $W_M \approx 2 \text{ GJ}$ of stored magnetic energy and requires $L_p I_\phi \approx 250 \text{ Wb}$. Given that

these burn conditions can be achieved by the start-up procedures described in Sec. 4.5, an inductively pulsed burn would be sustained for only $L_p/R_p \approx 200\text{--}400$ s. Considerations of total power balance, thermal cyclic fatigue in a high-power-density environment, as well as the costs of on-site energy storage (frequent grid start-up seems unlikely) and thermal storage force serious consideration of steady-state current drive. An inductively driven RFP reactor, although a possibility [55], would be re-optimized to minimize the plasma resistance, thereby resulting in larger plasmas, lower power density and possibly the use of superconducting coils.

Section 4.6.1 reviews a number of current-drive options for the RFP. The TITAN study has focused on the Oscillating-Field Current Drive (OFCD) system which is described from an MHD viewpoint in Sec. 4.6.1.1. An analogue circuit model for OFCD is used in Sec. 4.6.2 to investigate parametrically the characteristics of the OFCD system for the TITAN reactor, and a design point is suggested for further engineering analyses.

4.6.1. Options

4.6.1.1. Oscillating-Field Current Drive (OFCD)

Unlike the tokamak, the toroidal and poloidal currents in the RFP are closely coupled since the RFP plasma is in a minimum-energy state. This near-minimum-energy state is defined [2,3,67] primarily by holding the toroidal flux, Φ , and the magnetic helicity, $K = \int \vec{A} \cdot \vec{B} \, dV$, invariant within a conducting wall surrounding the plasma, where $\vec{A}$ is the magnetic vector potential ($\vec{B} = \nabla \times \vec{A}$) and the integration is performed over the volume enclosed by the conducting wall. The locus of these near-minimum-energy states form a curve in $F\text{--}\theta$ space, shown in Fig. 4.6.-1. If an external circuit parameter (e.g., voltage applied to TFC, V_θ) is varied to change the toroidal flux external to the plasma, intrinsic plasma processes related to turbulence and/or resistive instabilities generate voltages and currents within the plasma and increase or reduce poloidal flux in order to maintain the helicity constant and the plasma in a near-minimum-energy state. This nonlinear coupling between plasma and magnetic fields through the $F\text{--}\theta$ diagram, like that shown on Fig. 4.6.-1, can be used to "rectify" current oscillations created at external coils into a net steady-state current within the plasma [5,46,66]. This " $F\text{--}\theta$ pumping" is envisaged to transform toroidal magnetic flux (poloidal currents) into toroidal currents (poloidal magnetic flux) through the plasma relaxation which maintains the near-minimum-energy

Fig. 4.6.-1. Taylor diagram showing locus of near-minimum-energy RFP states for a range of μ profiles and poloidal beta values.

configuration. The result is an efficient inductive but oscillatory (i.e., with no loss of electromagnetic flux) mean of steady-state current drive for RFPs [5,53,66].

Most of the analysis and design for the OFCD system is based on circuit-analogue models [5,46,66]. Such a model is described in Sec. 4.6.2 and is used to analyze the TITAN reactor current-drive system. One can also explain OFCD in RFPs on the basis of MHD theory. Consider the evolution of the q -profile for one OFCD cycle, as is shown in Fig. 4.6.-2. Initially the near-minimum-energy RFP state is characterized by a q -value that is less than unity on axis, with q falling to zero near the plasma edge and reversing sign at the plasma surface. In order to implement OFCD in the RFP, the external toroidal and poloidal field circuits are oscillated about steady-state values, with the most efficient pumping (i.e., maximum current for minimum reactive power) occurring when the toroidal and poloidal voltages are ninety degrees out of phase (Sec. 4.6.-2). A decrease in the external poloidal flux, Ψ , and an increase in the amplitude of

Fig. 4.6.-2. Schematic representation of OFCD for RFPs and tokamaks in terms of a q -profile evaluation.

the external toroidal flux, Φ , will result in more negative F values (i.e., deeper reversal); this deeper reversal appears as a compression phase in Fig. 4.6.-2. The plasma under these conditions is unstable to a series of resistive MHD modes with a poloidal mode number $m = 1$ and high toroidal mode numbers, n . These instabilities drive a magnetic reconnection process, sometimes referred to as Kadomtsev reconnection, in a flattening of the central portion of the q profile and increasing the poloidal flux [69,70]. The magnetic reconnection process may also occur during the compression phase, and need not be a distinct phase, as is shown in Fig. 4.6.-2. The plasma is then decompressed as the external circuits approach the end of a period; the external poloidal flux is increasing, and the toroidal flux is decreasing at this point in the OFCD cycle. With the value of q at the plasma edge reset to the initial value, the plasma then relaxes by means of a double reconnection [70] (i.e., dynamo effect) into the initial near-minimum-energy state.

An analogous series of q profiles is shown for comparative purposes in Fig. 4.6.-2 for a tokamak. The major difference between the RFP and the tokamak is the process whereby poloidal flux is created on axis; Kadomtsev magnetic reconnection creates poloidal flux in an RFP, whereas it destroys poloidal flux in a tokamak. An as yet unspecified process, therefore, is required to create poloidal flux on axis in a tokamak. The application of OFCD to tokamaks, therefore, requires the identification of a flux-generating process. A scheme for increasing poloidal flux on axis has been proposed [71]. This scheme requires tailored ECRH heating to create an off-axis current channel; a non-monotonic q profile results. The Kadomtsev reconnection process ($m = 1$, $n = 1$) will return a monotonic q profile.

4.6.1.2. Bootstrap Current (BC)

Neoclassical theory predicts the existence of a "bootstrap current" caused by radial diffusion. An expression for this bootstrap current density is given by [72]

$$j_{BC} = - \frac{\epsilon^{1/2}}{B_\theta} \left(\frac{\partial p}{\partial r} \right), \quad (4.6.-1)$$

where $\epsilon = r_p/R_T$ is the inverse aspect ratio. The high β that characterizes the RFP suggests a large bootstrap current, but the high aspect ratio will tend to

reduce the effect. As an example, the following approximation to the RFP design-point equilibrium is assumed: $p(r)/p_0 = 1 - (r/r_p)^2$ with $p_0 \approx 6$ MPa, $B_\theta = \mu_0 I_\phi / 2\pi r = \mu_0 j_\phi r / 2 \approx 5.9$ T, and $\epsilon^{1/2} \approx 0.4$. For these conditions, Eq. 4.6.-1 results in a bootstrap current density of $j_{BC} \approx 1.4$ MA/m² or a total of $\pi r_p^2 j_{BC} = 1.6$ MA of toroidal current ($\pi r_p^2 = 1.13$ m²) as compared to the required $I_\phi \approx 18$ MA for the design point. Significantly steeper pressure gradients and lower aspect ratios would be required to make the bootstrap current a significant contributor, if it exists at all. Bootstrap current is not treated as a serious possibility for TITAN.

4.6.1.3. Alfvén-Wave (AW) Current Drive

Of all the proposed forms of RF current drive, the compressional Alfvén wave is by far the most efficient, out-performing the LH wave by about a factor of two on an ampere per watt basis. For Alfvén-wave current drive, Ehst [73] gives

$$P_{RF}^{AW} = 8.9 \times 10^{-20} \frac{I_\phi R_T n}{T}, \quad (4.6.-2)$$

whereas Fisch and Karney [74] give

$$P_{RF}^{AW} = 1.5 \times 10^{-20} \frac{I_\phi R_T n}{T \beta^{1/2}}, \quad (4.6.-3)$$

where β is the local beta value and T is in units of keV. Both Eqs. (4.6.-2) and (4.6.-3) are based on consideration of waves with $k_\parallel > k_\perp$. For a special Alfvén wave in which $k_\perp \gg k_\parallel$ (a condition which can easily be accomplished in tokamaks) a detailed calculation by Li [75] gives

$$P_{RF}^{AW} = 7.5 \times 10^{-20} \frac{I_\phi R_T n}{T}. \quad (4.6.-4)$$

For the $I_\phi \approx 18$ MA TITAN reactor design, these three relations respectively

result in 137, 73, and 115 MW of RF power required for the steady-state current drive system.

The main problems with the Alfvén-wave current drive revolve around trapped-particle effects in toroidal geometry and possible resonance with and damping of the wave by fast alpha particles. In principle, particles with speeds comparable to the Alfvén velocity, v_A , resonate with the wave and drain energy from it. The desirable condition is for the fast electrons to resonate with the wave and absorb all of the wave momentum since these electrons are not very collisional. However, in a typical RFP reactor $v_A \approx 2v_\alpha(300 \text{ keV})$ and $v_A = v_\alpha(1.2 \text{ MeV})$, where $v_\alpha(E_\alpha)$ is the velocity of an alpha-particle with energy E_α ; a part of the alpha-particle distribution, therefore, will resonate with the wave. Most of the wave energy and momentum absorbed by the alpha particles will, in turn, be passed onto the bulk electron population which is highly collisional and produces less current as compared to the case where all of the wave momentum is absorbed by the fast electrons in the first place. The net impact of the alpha particles is, therefore, to decrease the Alfvén-Wave current-drive efficiency.

Trapped-particle effects in toroidal geometry present a much more serious problem for low-aspect-ratio systems. Generally, the Alfvén waves have low phase velocity and, therefore, interact most strongly with electrons precessing slowly around the torus (these electrons have $v_\perp \gg v_\parallel$). Such particles are precisely those which are most susceptible to trapping in banana orbits. Fisch and Karney advance [74] arguments that the trapped-particle effects would not impair the Alfvén wave drive. Ultimately, however, this question would have to be resolved by experiment. The trapped-particle effects are expected to be especially strong in systems like the ATR/ST [76], but are ignorable in high aspect ratio stellarators (aspect ratio, $A = 14$) and should also be ignorable in a typical RFP reactor ($A \approx 6-8$).

4.6.1.4. Lower-Hybrid (LH) Current Drive

The LH wave has been proven experimentally to be an effective means of current drive in tokamaks, and, in fact, has been used in PLT [70] to start the tokamak without the use of OHCs. Lower-hybrid drive experiences no problem with resonating alpha particles or trapped-particle effects, but it does have a density limit set by $\omega_{ce} > \omega_{pe}$. This condition can be expressed as $\beta < 0.008 \text{ T(keV)}$, where β is the local value, and is marginally satisfied by the TITAN baseline design. Lower-hybrid current drive, therefore, should be possible,

provided care is taken in the detailed design of the system. Lower-hybrid waves can be easily launched from waveguides and have been launched with intensities up to 150 MW/m^2 . The main problem with LH drive is that of efficiency. Peng [78] gives the following expression for the drive power:

$$P_{\text{RF}}^{\text{LH}} = 13.5 \times 10^{-20} \frac{I_{\phi} R_T n}{T}, \quad (4.6.-5)$$

where T is again in units of keV, while Fisch and Karney give [74]

$$P_{\text{RF}}^{\text{LH}} = 0.93 \times 10^{-20} I_{\phi} R_T n. \quad (4.6.-6)$$

The results of applying these relationships to the $I_{\phi} = 18 \text{ MA}$ baseline design give $P_{\text{RF}}^{\text{LH}} = 207$ and 286 MW , respectively. It can be seen that the LH wave is less efficient than the Alfvén wave as applied to the RFP.

4.6.1.5. Relativistic Electron Beams (REB)

Reference 79 presents detailed analysis and design for a REB current-drive system for the FED-A fusion device. Those calculations indicate that the current-drive power for such a system can be taken approximately as

$$P^{\text{REB}} = 2 I_{\phi}^2 R_p, \quad (4.6.-7)$$

where R_p is the plasma resistance. Applying Eq. (4.6.-7) to the TITAN reactor with $I_{\phi} = 18 \text{ MA}$ results in a current drive system with 16 MW of REB power, consistent with a 1.6-MJ pulse energy and an interpulse period of 0.1 s . The REB current drive has been demonstrated experimentally on several tokamaks and has been used successfully for start-up without the use of OH coils [80].

Although the low-power requirement makes REBs attractive, a number of obstacles to implementation can be identified. Firstly, the REB current-drive system involves the repeated firing of megavolt capacitor banks, giving rise to questions of reliability when applied to a commercial reactor. Secondly, theory indicates that the REB current will only penetrate a few centimeters into the plasma, leading to a hollow current profile. Deeper penetrations can be

achieved if very high voltages (~ 50 MV) can be used. A combination of REB to provide current in the outer plasma with Alfvén-Wave drive to provide current at the plasma core may offer some power savings, albeit at the cost of additional complexity. One hope in dealing with the hollow current profile is that the REB-created currents will diffuse rapidly into the plasma core through anomalous processes or be transported into the central plasma through MHD activities.

4.6.2. OFCD Plasma/Circuit Modeling

Of the current drive options summarized in Sec. 4.6.1, the OFCD system was chosen for further evaluation. This choice was based on the projected efficiency of the drive, its relative simplicity, and the uniqueness of this scheme to the RFP. A plasma/circuit model for OFCD was then used to identify and assess the potential design, power engineering, and magnetic problems. This section summarizes this effort.

4.6.2.1. Model

Little analysis and design beyond that described in Ref. 53 are available to describe OFCD systems for either RFP experiments or reactors. This section develops and applies a methodology by which future engineering evaluations of OFCD can proceed. In developing and evaluating a global picture of OFCD, the circuit parameters, definitions, and approach summarized in Table 4.6.-I are used to arrive at an OFCD equation. Note that the brackets ($\langle \rangle$) throughout this section denote time-averaged quantities with the exception of the magnetic fields, with $\langle B_\phi \rangle$ being a spatial average and $\bar{B}_\phi$ being a time average.

In order to implement OFCD in the RFP, the external toroidal and poloidal-field circuits are oscillated about steady state values. The plasma is described in terms of K , Φ , and W_M , and the relationship between these parameters and the circuit variables (i.e., resistances, inductances, currents, voltages) constitutes the overall current-drive model. The time dependence of K , Φ , and W_M results directly from the Maxwell equations:

$$\frac{dK}{dt} = 2\Phi V_\phi - 2 \int \eta \vec{j} \cdot \vec{B} \, dV, \quad (4.6.-8)$$

$$\frac{d\Phi}{dt} = + V_\theta, \quad (4.6.-9)$$

TABLE 4.6.-I

SUMMARY OF DEFINITIONS AND NOTATION USED TO MODEL
OSCILLATING-FIELD CURRENT DRIVE (OFCD)

Definition	Parameter
Toroidal voltage on plasma	V_ϕ
Toroidal current in plasma	I_ϕ
Poloidal voltage applied to toroidal-field coil	$V_\theta = \partial\Phi/\partial t \approx \dot{\Phi}$
Poloidal current flowing in external conductor	I_θ
Plasma resistance	R_p
Magnetic helicity	$K = \frac{1}{\mu_0} \int \vec{A} \cdot \vec{B} \, dV$
Toroidal flux	$\Phi = 2\pi \int_0^R B_\phi(r) \, r \, dr$
Total field energy within conducting shell	$W_H = \int (B^2/2\mu_0) \, dV$
Vacuum toroidal inductance	$L_0 = \mu_0 R_p^2/2R_T$
Inverse aspect ratio	$\varepsilon = r_w/R_T$
Average toroidal field within shell	$\langle B_\phi \rangle = \Phi/\pi r_w^2$
Reversal parameter	$F = \bar{B}_\phi(r_w)/\langle B_\phi \rangle = L_0 I_\theta/\dot{\Phi}$
Pinch parameter	$\Theta = \bar{B}_\theta(r_w)/\langle B_\phi \rangle = L_0 I_\phi/\varepsilon \dot{\Phi}$

$$\frac{dW_H}{dt} = I_\phi V_\phi - I_\theta V_\theta - \int \eta \vec{j} \cdot \vec{j} \, dV, \quad (4.6.-10)$$

$$\vec{E} = \eta \vec{j} - \vec{v} \times \vec{B} \quad (E_\phi = \eta j_\phi - \varepsilon B_\phi), \quad (4.6.-11)$$

where a positive Faraday's Law convention for the toroidal circuit is used in Eq. (4.6.-9); this convention orients $\dot{\Phi}$ in the same direction as I_ϕ . The last expression (4.6.-11) gives an Ohm's law corrected for the plasma dynamo effect. Defining the plasma energy and "helicity" resistances, R_p and R'_p , respectively, by

$$R_p \equiv \frac{1}{I_\phi^2} \int \eta \vec{j} \cdot \vec{j} \, dV, \quad (4.6.-12a)$$

$$R'_p \equiv f_A R_p \equiv \frac{1}{\Phi I_\phi} \int \eta \vec{j} \cdot \vec{B} \, dV, \quad (4.6.-12b)$$

the plasma helicity and energy equations become

$$\frac{dK}{dt} = 2\Phi(V_\phi - I_\phi f_A R_p), \quad (4.6.-13)$$

$$\frac{dW_H}{dt} = I_\phi V_\phi - I_\Theta V_\Theta - I_\phi^2 R. \quad (4.6.-14)$$

The factor $f_A \equiv R'_p/R_p \equiv (2\Theta/r_p R_p) \int (\eta/\mu) \vec{j} \cdot \vec{j} \, dV$ is the ratio of energy resistivity to "helicity" resistivity and $f_A = 1$ for the Bessel-function model (i.e., for the special case of $\beta = 0$ and $\mu(r) \equiv \mu_0 \vec{j} \cdot \vec{B}/B^2$ equal to a constant). It is readily seen from either Eq. (4.6.-8) or (4.6.-13) that if the time-averaged $\langle dK/dt \rangle$ is to equal zero during an OFCD oscillation period, the dissipation term (the power to be supplied to the plasma to maintain the plasma current) must be equal to the time-averaged $2\langle \Phi V_\phi \rangle$. Thus, Φ and V_ϕ should be nominally in phase; or, V_ϕ and $V_\Theta \approx \dot{\Phi}$ should be out of phase by 90° .

The plasma magnetic energy, W_H , can be written in terms of plasma and circuit parameters as

$$W_H = \frac{1}{2} L_p I_\phi^2 + \frac{\Phi^2}{2L_0}. \quad (4.6.-15)$$

The plasma inductance, L_p , is a function of the field profiles and, for example, can be derived using the BFM (i.e., $\mu(r) = \text{constant}$). Combining Eqs. (4.6.-9), (4.6.-14), and (4.6.-15) leads to the following expression for the toroidal voltage around the plasma, V_ϕ :

$$V_{\phi} = I_{\phi} R_p + \left(L_p + \frac{\Theta}{2} \frac{dL_p}{d\Theta} \right) \dot{I}_{\phi} + \left(\frac{1-F}{\epsilon\Theta} - \frac{\epsilon\Theta^2}{2L_0} \frac{dL_p}{d\Theta} \right) V_{\Theta} . \quad (4.6.-16)$$

Equation (4.6.-16) shows that if a) the coupling of fields is sufficiently strong to make L_p a function of Θ , and b) if a mechanism exists to allow the plasma near-minimum-energy state to relax to some point in F - Θ space (Fig. 4.6.-1) on a time scale, τ_R , then oscillations of V_{ϕ} and V_{Θ} in proper phase at a frequency less than $\sim 2\pi/\tau_R$ can give a net time-averaged current, $\langle I_{\phi} \rangle$, with $\langle V_{\phi} \rangle = 0$ (i.e., no net flux change).

The OFCD equation (4.6.-16) can be used to determine the flux changes, field oscillations, and power flows associated with OFCD as applied to the RFP. In order to solve Eq. (4.6.-16), the plasma inductance, L_p , its derivative with respect to the pinch parameter, $dL_p/d\Theta$, and the plasma resistance, R_p , must be known. These quantities depend on the field profiles and the relationship between F and Θ . The plasma resistance, R_p , used in Eq. (4.6.-16) is a function of the field profiles through the factor g_{OHM} as:

$$R_p = 2 g_{OHM} \frac{\eta}{\epsilon^2 R_T} , \quad (4.6.-17)$$

where η is the classical resistivity evaluated at the average plasma temperature. The profile factor, g_{OHM} , is often referred to as the resistance "anomaly" factor, a name that can be misleading in that as given above g_{OHM} for $Z_{eff} = 1$ is a measure of the extended current path resulting from the "screwing-up" of the high-pitch field lines.

The special case of the BFM is amenable to analytic solution and is reproduced here. Using the BFM field profiles [i.e., $B_{\theta}(r) \propto J_1(\mu r)$ and $B_{\phi}(r) \propto J_0(\mu r)$] and equating the total magnetic energy stored within the plasma to $L_p I_{\phi}^2/2$ gives the following expression for L_p :

$$L_p = \frac{2L_0}{\epsilon^2} \left[1 + \frac{(2F+1)(F-1)}{2\Theta^2} \right] , \quad (4.6.-18)$$

where $F = \Theta J_0(2\Theta)/J_1(2\Theta)$ for the BFM; included in Eq. (4.6.-15), and hence, in Eq. (4.6.-18), is the energy needed to compress an initially uniform toroidal flux into the $B_\phi(r) \propto J_0(\mu r)$ distribution. The purely poloidal inductance for the BFM is

$$L_{p\Theta} = \frac{2W_{B\Theta}}{I_\phi^2} = \frac{L_0}{\epsilon^2} \left[1 + \frac{F}{\Theta^2} (F - 1) \right]. \quad (4.6.-19)$$

For the BFM and a flat temperature distribution, g_{OHM} is given by

$$g_{OHM} = 2(\Theta^2 + F^2) - F. \quad (4.6.-20)$$

For the BFM model without deep reversal ($\Theta < 1.6$), g_{OHM} typically is ~ 3 .

For more realistic models of the μ profile (e.g., modified-Bessel-function model), the field profiles should generally be calculated numerically. An example is a preliminary model described in Appendix A of Ref. 53. The MHD pressure balance, $\vec{j} \times \vec{B} = \nabla p$, can be used to specify the current density parallel and perpendicular to the magnetic field:

$$\vec{j}_{\parallel} = \mu \vec{B}. \quad (4.6.-21a)$$

$$\vec{j}_{\perp} = \frac{\nabla p \times \vec{B}}{B^2}, \quad (4.6.-21b)$$

$$\nabla \times \vec{B} = \mu_0 \vec{j} = \mu_0 (\vec{j}_{\parallel} + \vec{j}_{\perp}), \quad (4.6.-21c)$$

where $\mu(r) = \mu_0 \vec{j} \cdot \vec{B} / B^2$ and the last equation is the Ampere's Law. For given μ and pressure profiles, Eqs. (4.6.-21) can be solved to find the magnetic field and current density profiles. For one-dimensional axisymmetric system, these equations can be expressed in cylindrical co-ordinates as:

$$-\frac{\partial B_\phi}{\partial r} = \mu_0 j_\theta = \mu B_\theta + \mu_0 \frac{\partial p}{\partial r} \frac{B_\phi}{B^2} \quad (4.6.-22a)$$

$$\frac{1}{r} \frac{\partial}{\partial r} (r B_{\theta}) = \mu_0 j_{\phi} = \mu B_{\phi} - \mu_0 \frac{\partial p}{\partial r} \frac{B_{\theta}}{B^2} . \quad (4.6.-22b)$$

These equations are solved for field and current-density profiles by specifying $T(r)$, $n(r)$, $\mu(r)$ and the following constraints:

$$\beta_{\theta} = \frac{\int_0^{r_p} p \, 2\pi r \, dr}{\pi r_p^2 B_{\theta}^2(r_p)/2\mu_0} , \quad (4.6.-23a)$$

$$I_{\phi} = \int_0^{r_p} j_{\phi} \, 2\pi r \, dr . \quad (4.6.-23b)$$

The resulting profiles then are used to determine g_{OHM} , F , L_p , and $dL_p/d\theta$ for use in the OFCD equation (4.6.-16).

The poloidal and toroidal circuit equations coupled through Eq. (4.6.-16) can be solved numerically for I_{ϕ} once driver functions, such as those given below, are selected for ϕ and V_{ϕ} .

$$\phi = \phi_0 + \delta\phi \cos \omega t , \quad (4.6.-24a)$$

$$V_{\theta} = \dot{\phi} = \delta\phi \, \omega \sin \omega t , \quad (4.6.-24b)$$

$$V_{\phi} = \delta V_{\phi} \cos \omega t . \quad (4.6.-24c)$$

The constraint that the time-averaged helicity is constant ($\langle dK/dt \rangle = 0$) can be used to establish limits on the magnitude of the field oscillations required to sustain a given toroidal plasma current. If the ohmic dissipations for both the induced and driven cases are similar, and if the induced case is characterized by ϕ_0 and $V_{\phi 0}$, then dK/dt for the driven case is given by Eq. (4.6.-13) with $V_{\phi 0} = I_{\phi} R_p$. Hence,

$$\frac{dK}{dt} \approx 2\Phi(V_\phi - V_{\phi 0}) \quad (4.6.-25)$$

Using the driver functions given by Eqs. (4.6.-24), the time average of dK/dt for the driven case is

$$- \langle dK/dt \rangle = \delta\Phi\delta V_\phi + 2\Phi_0 V_{\phi 0} \quad (4.6.-26)$$

For $\langle dK/dt \rangle = 0$, the following condition on the amplitudes of the toroidal flux and voltage oscillation results:

$$\left(\frac{\delta\Phi}{\Phi_0}\right) \left(\frac{\delta V_\phi}{V_{\phi 0}}\right) \approx -2 \quad (4.6.-27)$$

Given that toroidal flux oscillations much above $\delta\Phi/\Phi_0 \approx 0.02-0.03$ are expected to impact seriously the RFP configuration (i.e., loss of toroidal-field reversal), the AC toroidal voltage needed to drive a DC toroidal current with $\langle dK/dt \rangle \approx 0$ can be 6-10 times greater than the voltage needed to sustain an inductively driven RFP.

To assess the tradeoff between $\delta\Phi/\Phi_0$, $\delta V_\phi / \langle I_\phi F_{ARp} \rangle$, ω , $\delta I_\phi / \langle I_\phi \rangle$, and reactive power, the plasma OFCD equations must be solved in conjunction with a realistic circuit model. In a simplified form, Fig. 4.6.-3 illustrates two independent circuits coupled through the RFP plasma. A circuit representation of the poloidal-field (PF) and toroidal-field (TF) circuits coupled through the OFCD equation (4.6.-16), and the F- Θ diagram is shown in Fig. 4.6.-4, where the subscript "H" refers to "helicity"-drive, and current elements designated with "L" refer to liner or blanket elements. Figure 4.6.-5 schematically illustrates the oscillating or reactive power flows in terms of a simplified model, with the power P_Ω being dissipated therein. Of the reactive power or "Poynting" vectors flowing across the plasma surface, P_p^* , the quantity P_L is dissipated in the liner surrounding the plasma and the reactive power $P_H^* \ll P_p^*$ actually must be managed by the helicity drive circuits. The remaining reactive power, $\approx P_p^* - P_H^* - P_L$, is stored in the external plasma inductance, and may be subject to further dissipative loss, depending on a yet-to-be-resolved arrangement of

Fig. 4.6.-3. Schematic diagram of OFCD circuits coupled through the RFP plasma.

Fig. 4 6.-4. OFCD circuit diagram. The subscript "H" refers to "helicity"-drive, and current elements designated with "L" refer to liner or blanket elements.

conducting paths in the immediate vicinity of the reactor torus. Of the P_H^* reactive power handled by the OFCD circuit, the amount P_H is dissipated therein. Thus, the total real power that must be supplied to sustain the RFP current, I_ϕ , is $P_H + P_L + P_Q$. Figure 4.6.-5 defines a number of plasma and circuit Q-values and current-drive efficiencies.

The calculational algorithm developed to solve simultaneously and parametrically the plasma and circuit OFCD equations is shown schematically in Fig. 4.6.-6. Input parameters include all plasma profiles determined from the solution of the MHD Eqs. (4.6.-21), geometry, F- θ oscillation point, key plasma parameters, and all helicity-circuit parameters. The highly non-linear set of partial differential equations is solved for $\langle dI_\phi/dt \rangle = 0$ under the following constraints

- ♦ $\langle dI_{H\phi}/dt \rangle = 0$ (no net flux in the circuit),
- ♦ $\langle dK/dt \rangle = 0$ (constant average helicity),
- ♦ Total energy balance,
- ♦ $E_\phi \approx E_\theta$ around the plasma.

These four conditions give a unique OFCD design point for a given set of input variables about which sensitivity studies have been made, with the constraint that $E_\phi/E_\theta \approx 1$ being relaxed somewhat to allow variations in $\delta\phi/\phi_0$.

4.6.2.2. Parametric Results

The model shown in Fig. 4.6.-6 has been evaluated through single-point variations of poloidal beta, β_θ , plasma temperature, T , OFCD frequency, ω , toroidal flux swing, $\delta\phi/\phi_0$ (i.e., E_θ/E_ϕ), and liner resistance, $R_L = \eta_L(R_T/\delta_T r_w)$. Table 4.6.-II gives the basecase TITAN parameters about which sensitivity studies were made.

A key parameter of interest is the reactive power required to sustain in steady state the parameters listed on Table 4.6.-II. Figure 4.6.-7 shows the dependence of reactive power in both poloidal and toroidal circuits, $P_{H\phi}^*$ and $P_{H\theta}^*$, respectively, as well as the associated Poynting-vector power flows past the plasma surface, $P_{p\phi}^*$ and $P_{p\theta}^*$, respectively. As β_θ is increased, the required plasma current is decreased for a given fusion power and plasma dimension. Typically, $\delta\phi/\phi_0 \approx 0.016$, $\delta V_\phi/I_\phi R_p \approx 460$, and $\delta I_\phi/I_\phi \approx 0.006$ for this case of a highly resistive liner ($\eta_L \approx 100 \mu\Omega/\text{m}$ and $\delta = 1 \text{ mm}$).

Fig. 4.6.-5. OFCD circuit/plasma global power flows.

Fig. 4.6.-6. The algorithm used to solve OFCD plasma/circuit equations.

TABLE 4.6.-II
BASECASE TITAN OFCD PARAMETERS

Minor radius, r_p (m)	0.61
Major radius, R_T (m)	3.96
Current, I_ϕ (MA)	<u>18-22</u> (a)
Poloidal beta, β_θ	<u>0.1-0.2</u>
Density, n (10^{20} m^{-3})	<u>4.-8.</u>
Temperature, T (keV)	<u>10.-20.</u>
Profile exponents, v , $f = 1 - (r/r_p)^v$	
* $\mu(r)/\mu(0)$	8.
* $T(r)/T(0)$	4.
* $n(r)/n(0)$	2.5
Shell/liner parameters	
* Minor radius, r_w (m)	0.66
* Thickness, δ_L (m)	0.001
* Resistivity, η_L ($\Omega \text{ m}$)	10^{-4}
OFCD Parameters	
* Frequency (Hz)	60
* Flux-swing, $\delta\Phi/\Phi_0$	0.016
* $E_\theta/E_\phi = (V_\phi/2 R_T)/(V_\theta/2\pi r_p)$	1.0

(a) Underline value gives basecase.

As the plasma resistance is decreased for higher operating temperature, the required OFCD reactive power decreases, as is shown on Fig. 4.6.-8. The toroidal-flux and plasma current swing are also shown. Figure 4.6.-9 gives the dependence of reactive power, $\delta\Phi/\Phi_0$, and $\delta I_\phi/I_{\phi 0}$ on drive frequency, with the plasma energy change per cycle decreasing with increased frequency (less dissipation over a shorter period), giving the relative insensitivity of reactive power to frequency change. The variation of reactive powers and current swing on the toroidal flux swing is shown in Fig. 4.6.-10, where the $\delta\Phi/\Phi_0$ variation was caused by varying E_θ/E_ϕ away from the basecase value of unity. It is seen from the viewpoint of minimizing plasma current variations, plasma reactive power, and (most importantly) the helicity-circuit reactive power ($P_{H\phi}^* + P_{H\theta}^*$), that an optimal value of $\delta\Phi/\Phi_0$ in the range 0.02 - 0.03 is indicated. Also shown on Fig. 4.6.-10 is the region where toroidal-field

Fig. 4.6.-7. Dependence of GPCD reactive powers on poloidal beta.

Fig. 4.6.-8. Dependence of OFCD reactive power, toroidal flux change, and plasma current variation on average plasma temperature.

Fig. 4.6.-9. Dependence of OFCD reactive power, toroidal flux change, and plasma current swing on drive frequency.

Fig. 4.6.-10. Reactive powers, relative electric fields, E_θ/E_ϕ , and plasma current swing as a function of the toroidal flux swing.

reversal is lost ($F > 0$). Lastly, Fig. 4.6.-11 gives the dependence of key OFCD parameters on the liner resistivity, with the point where the liner dissipation equals the plasma dissipation ($P_L = P_Q = 8$ MW) being at $\eta_L = 0.2 \mu\Omega \cdot m$ ($\eta_{Cu} = 0.018 \mu\Omega \cdot m$). For the shell dimensions assumed (Table 4.5.-II), the time constant amounts to $\tau_L = 7$ ms and the ohmic power density is ~ 100 MW/m³ or $\sim 50\%$ of the local nuclear-heating power density for the $I_\phi \approx 18$ MW/m² Strawman design.

4.6.2.3. Interim Conclusions

Although the foregoing models and analysis represent the first attempt to integrate circuit effects into the OFCD plasma modeling, the TITAN layout must be evolved further in other respects before a clear-cut assessment of design, power engineering, and magnetics problems can be made. This work remains for the next phase of the TITAN study. Nevertheless, the following interim observations and conclusions can be made.

- Inclusion of OFCD distorts wave forms and generates phase shifts, giving rise to complications, in that three iterative steps are required.
- no net circuit volt-seconds, which requires an iteration on $V_{H\phi}$ (bias).
- if $\frac{\delta\phi}{\phi_0} \cdot \frac{\delta V_\phi}{f_A I_\phi R_p} > 2$ an iteration for helicity balance is required.
- circuit/plasma energy balance requires iteration on $f_A = R_p/R_p'$.
- Small plasma current swings ($\delta I_\phi/I_\phi = 0.02$) are sufficient to drive steady-state currents in the 18-20 MA range with resistive powers in the plasma of ~ 8 -10 MW.
- Large power flow past plasma surface (20-30 MJ at a frequency of 60 Hz), but the real power consumption is 8-10 MW.
- Total/circuit reactive power ratio is ~ 25 , with $P_{p\phi}^* \approx 4$ GW and $P_{H\phi}^* \sim 170$ MW; shielding of OHC/EFC may be required.

EFFECTIVE LINER RESISTIVITY, $\eta_L (\mu\Omega \cdot m)$

Fig. 4.6.-11. Dependence of OFCD reactive powers, liner dissipative powers, and flux and current swings on the liner resistivity ($\eta_L = 100 \mu\Omega \cdot m$ base case).

- ♦ Liner resistance, $R_L \geq 2 \text{ m}\Omega$ ($\tau_L < 1.3 \text{ ms}$) is required to hold $P_L \leq P_Q \sim 8 \text{ MW}$ ($\sim 80 \text{ MW/m}^3$ of liner). A larger τ_L leads to ohmic power density that exceeds nuclear heating ($\geq 200\text{--}300 \text{ MW/m}^3$). The design of an actual "shell" (i.e., liquid-metal cooled blanket, first-wall, shields, supports, gaps, penetrations) that gives required R_L or η_L may be a crucial issue.
- ♦ Energy flow across plasma surface (20-30 MJ) is small, but not negligible, compared with plasma kinetic energy ($\sim 10\%$). For example, the basecase conditions ($n = 4.35 \times 10^{20} \text{ m}^{-3}$, $T = 20 \text{ keV}$, $r_p = 0.60 \text{ m}$, $R_T = 3.9 \text{ m}$, $p = 4.2 \text{ MJ/m}^3$, $V_p = 27.7 \text{ m}^3$), gives a total stored kinetic energy of $W_p = 230 \text{ MJ}$. However, the energy flow associated with OFCD is negligible (1-2%) compared with plasma magnetic energy in that $\beta \approx \beta_0/2 \approx 0.1$ and $W_B = (2/3)W_p/\beta \approx 1,530 \text{ MJ}$. Finally, transfer occurs on a time scale ($\sim 60 \text{ Hz}$) that is 5-10% of τ_E .

4.7. ONE-DIMENSIONAL CORE PLASMA SIMULATIONS

4.7.1. Model

A one-dimensional RFP plasma burn code, RFPBURN [81], has been used for

TABLE 4.7.-I
SUMMARY OF KEY FEATURES OF RFPBURN

- ♦ *The code follows the time-dependent, cylindrical plasma evolution of ion and alpha-particle densities, ion and electron temperatures, and poloidal and axial (toroidal) magnetic fields.*
- ♦ Physics constraints include Ohm's law, radial pressure balance and local quasi-neutrality.
- ♦ Physical terms include conduction, diffusion, convection, resistive dissipation, fusion reactions, impurity radiation based on a coronal equilibrium model, a volumetric ion source and/or a pellet injection model, and finally, a simple dynamo model which conserves helicity and allows the RFP configuration to be maintained in steady state.
- ♦ Boundary conditions consist of six regularity conditions at the origin ($r = 0$) and six wall conditions which include the toroidal magnetic field, the toroidal voltage, and either extrapolation endpoint or pedestal conditions on the densities and temperatures.
- ♦ Global plasma particle and energy balance is followed and used to check particle and energy conservation.

studying transport and other 1-D effects in the bulk plasma of the TITAN RFP reactor. Table 4.7.-I summarizes the key features of the RFPBURN code.

While the zero-dimensional plasma/circuits code BURN described in Sec. 4.5. is the main tool used to examine RFP reactor transients, RFPBURN supplements the 0-D model by examining 1-D aspects of local transport assumptions, impurity radiation with beta limits, pellet refueling, dynamo, and current drive. Additionally, RFPBURN can be coupled to the edge-plasma models to provide self-consistent core/edge-plasma boundary conditions. The 1-D results reported herein include: a) simulation of the parametric systems analysis design points and b) examination of a means to enhance radiative over transport losses in order to reduce erosion of the plasma-interactive components.

4.7.2. Results

For TITAN applications, RFPBURN has been used to study steady state burn conditions. The results are compared in Table 4.7.-II with the 0-D steady-state code results given in Sec. 4.5., and good agreement is found when in the 0-D model, flatter plasma density and temperature profiles are used relative to the Bessel-function profiles (Figs. 4.7.-1 and 2). The assumed transport coefficients are given by Eqs. (4.7.-1) to (4.7.-4) with $g(r) = 1$ and $\beta_{0c} = 0.19$.

Experimental evidence on ZT-40M suggests that RFPs can operate at a soft beta limit [44,45], as discussed in Sec. 4.3. This characteristic of the RFP is in marked contrast with other confinement schemes such as the tokamak, where increasing the impurity content would increase the total energy loss rate and degrade the plasma pressure. Enhanced radiation from a (high) beta-limited plasma is important because it permits first-wall designs to receive a higher average (but more uniform) heat flux and to minimize the divertor (or limiter) power loads, thereby optimizing the overall design for the maximum power density while maintaining realistic engineering constraints on all systems.

The RFPBURN [81] one-dimensional transport model has been used to examine some of the properties of a beta-limited and radiation-dominated reactor-grade plasma. The radiation model assumes coronal equilibrium with the impurity density of the j^{th} species, n_j , being specified by means of an impurity fraction, $f_j \equiv n_j/n_i$, where n_i is the DT ion density. In order to incorporate a

TABLE 4.7.-II
COMPARISON OF 0-D AND 1-D PLASMA SIMULATION DESIGN POINT

Parameter	0-D	1-D
Plasma minor radius, r_p (m)	0.6	0.6
Plasma major radius, R (m)	3.9	3.9
Plasma average ion density, n_i (10^{20} m^{-3})	4.35	4.66
Density peaking, n_{i0}/n_i	1.80	1.73
Alpha-particle fraction, n_α/n_i	0.053	0.042
Ion temperature, T_i (keV)	20.0	20.7
Ion-temperature peaking, T_{i0}/T_i	1.22	1.37
Electron temperature, T_e (keV)	20.0	19.5
Electron-temperature peaking, T_{e0}/T_e	1.22	1.19
Global particle confinement time, τ_p (s)	0.96 ^(b)	0.88
Global energy confinement time, τ_E (s)	0.24 ^(b)	0.29
Lawson parameter, $n_i \tau_E$ (10^{20} s/m^3)	1.04	1.35
Poloidal beta ^(a) , β_θ	0.221	0.218
Plasma toroidal current, I_ϕ (MA)	17.8	17.9
Ohmic power in plasma, P_Q (MW)	8.0	8.1
Fusion power, P_F (GW)	2.26	2.37
Profile factors:		
• DT reactivity, g_{DT}	1.43	1.28
• Ohmic dissipation, g_Q	3.45	2.99
• Bremsstrahlung, g_{BR}	1.33	1.24

(a) Includes thermalized alpha particles.

(b) Based on the empirical scaling (Sec. 5.2.1.) $\tau_{EE} = C_v I_\phi^v r_p^2$
($v = 1$, $C_v = 0.05$) and $\tau_p = 4\tau_{ce}$.

soft beta limit in a one-dimensional formulation, the transport coefficients were assumed with the following form:

$$K_e = K_{\perp e}^{cl} f(\beta_\theta) \quad (4.7.-1)$$

$$K_i = K_{\perp i}^{cl} + K_e/4 \quad (4.7.-2)$$

Fig. 4.7.-1. Comparison of 0-D and 1-D radial density profiles.

Fig. 4.7.-2. Comparison of 0-D and 1-D radial temperature profiles.

$$D = D^{cl} [1 + 0.1f(\beta_\theta)] \quad (4.7.-3)$$

where $K_\perp^{cl}$ and D^{cl} are classical values of the cross-field thermal conductivity and particle diffusivity, and the following function $f(\beta_\theta)$ is used to model the observed soft beta limit:

$$f(\beta_\theta) = \begin{cases} 1, & \beta_\theta \leq \beta_{\theta c} \\ \exp \left[\left(\frac{\beta_\theta}{\beta_{\theta c}} \right)^{15} - 1 \right] \times g(r), & \beta_\theta > \beta_{\theta c} \end{cases} \quad (4.7.-4)$$

where,

$$g(r) = \begin{cases} 1, & r > 0.84r_p \text{ or } \beta(0) \leq 0.11 \\ 1 + 10 \left[1 - \left(\frac{r}{0.84r_p} \right)^2 \right], & \beta(0) > 0.11 \end{cases} \quad (4.7.-5)$$

Here, the exponential factor represents the poloidal beta limit. The parabolic factor represents an enhanced internal transport and excludes highly peaked temperature profiles caused by highly-localized radiation losses. Such a factor would have a physical basis either if rapid transport occurs in the plasma core, possibly resulting from large scale $m = 1$ current-driven tearing modes, or if the global beta limit results from local beta limits. The model utilizes boundary conditions at r_p which include the toroidal magnetic field and the toroidal loop voltage as well as extrapolation endpoint conditions for the density and temperatures.

The following computational results pertain to a typical RFP reactor plasma (listed in Table 4.7.-III), but are not to be taken as the most recent TITAN design parameters. The required impurity fraction for a given radiation

TABLE 4.7.-III
REACTOR CONDITIONS OF THE BETA-LIMITED 1-D TRANSPORT STUDY

Plasma minor radius, r_p (m)	0.715
Plasma major radius, R_T (m)	3.93
Plasma toroidal current, I_ϕ (MA)	18.0
Plasma average ion density, n_i (10^{20} m^{-3})	8.67
Critical poloidal beta, $\beta_{\theta c}$	0.19

TABLE 4.7.-IV
STEADY-STATE PLASMA PARAMETERS COMPUTED FROM THE 1-D MODEL

Impurity	--	C	Xe	U
Z_j	--	12	54	92
f_j	0.0	4×10^{-2}	1×10^{-4}	3×10^{-5}
$Z_{eff}(0)$	1.04	2.0	1.3	1.15
$\tau_E^{NR}(s)$	0.64	7.9	1.2	1.8
$\tau_E(s)$	0.25	0.53	0.35	0.35
$P_\alpha(\text{MW})$	336	233	306	321
f_{RAD}	0.12	0.76	0.71	0.81

fraction is illustrated in Fig. 4.7.-3. Table 4.7.-IV summarizes key results for different plasma impurities.

For no impurities other than a ~4% alpha particle ash, a minimum $f_{RAD} = 0.12$ is obtained. While low-Z impurities such as carbon can radiate the necessary power, a high impurity level ($f_j = 0.04$) is required. For a given plasma beta and plasma current, such a large impurity level changes the shape of the plasma profiles and thus the fusion (alpha-particle) power, P_α , as shown in Table 4.7.-IV. However, slight increases in I_ϕ result in large increases in P_α , and therefore, design powers can be easily recovered with moderate (5%) increases in I_ϕ . The fusion power can also be increased by trading off density with temperature. Such optimizations can best be done at the 0-D level of analysis. Also, the rather high value of $Z_{eff} = 2$ for the carbon impurity would double the current-drive power requirements.

Fig. 4.7.-3. Impurity fraction versus radiation fraction for different impurities.

High-Z impurities such as xenon require a much smaller impurity concentration and produce lower values of Z_{eff} (e.g., $Z_{\text{eff}}(0) = 1.3$ for Xe and the Z_{eff} decreases with radius as T_e decreases). Therefore, the high Z impurities are favored for enhancing the plasma radiation fraction.

The profiles of plasma parameters found by these 1-D simulations are uncertain since local transport is not known. But the highly localized radiative losses in the outer regions of the plasma are not expected to drastically change the plasma profiles if a local beta limit or some other source of rapid transport in the plasma core exists. It is noted that if the transport in the plasma core is too low, then high radiation losses at the plasma edge could result in a temperature collapse of the entire plasma column as the cold, radiating edge propagates inward.

Listed below are the major unknowns associated with assuming beta limits for RFP reactors. Even though a few of these issues may be partially addressed by 1-D analysis, the majority of the unknowns will require experimental demonstration at reactor conditions and, therefore will remain unknown for some time.

- * β -limited confinement scaling in the reactor plasma regime.
- * Limit on the local beta.
- * The level and mechanism of the intrinsic transport.
- * The collapse of the temperature profile due to impurity injection.
- * The interaction of competing profile affects (e.g., refueling, alpha-particle transport, $T_i:T_e$ split).
- * The impact of the impurities on the edge-plasma and the wall (e.g., sputtering, wall buildup, vacuum and tritium systems).
- * The effects of the impurities on the start-up transient.

Based on both experiment and theory, it appears feasible that RFP reactors can exhibit a soft β limit. Such a β limit was assumed in choosing the ZT-H experimental parameters [44]. If such β limits exist, it may be possible to

adjust f_{RAD} to any level between 0.12 and 0.95 with only a minor increase (10-30%) in plasma resistance by injecting high-Z impurities into the plasma core. Only small variations in the impurity fraction are required to significantly vary f_{RAD} . In practice, the maximum operating f_{RAD} will be determined by the level of intrinsic transport. If the intrinsic transport mechanisms are classical, then the f_{RAD} upper limit could be higher than 0.99. Finally, it is noted that the impurity fraction of Xe required for $f_{\text{RAD}} = 1$ in the plasma core is two orders of magnitude smaller than that required for $f_{\text{RAD}} = 1$ in the divertor chamber.

4.8. SUMMARY OF PHYSICS ISSUES AND FUTURE DIRECTIONS

The main physics areas that remain to be resolved before a viable commercial reactor can be based on the RFP are listed in Table 4.8.-I and summarized in the following subsections. Figure 4.8.-1 compares these issues and options with other confinement schemes, with the areas of transport, start-up and equilibrium/stability being combined under the general heading of plasma control.

4.8.1. Transport

The microscopic energy and particle confinement in present-day RFPs remain largely unknown, with global energy confinement for ohmically-heated discharges showing a dependence of the form $\tau_E \propto I_\phi^\nu$, where ν is in the range 0.8-1.5 and I_ϕ is the toroidal plasma current.

Simple energy balance on an ohmically heated plasma and the assumption of constant I_ϕ/N lead to the following expression:

$$\tau_{ce} = C_\nu I_\phi^\nu r_p^2 f(\beta_\theta), \quad (4.8.-1)$$

where C_ν is a fitting constant that is dependent on ν , a soft beta limit has been assumed (prescribed by $f(\beta_\theta)$), and the global energy loss is generally ascribed to anomalous electron losses. In performing TITAN fusion core simulations, it is generally assumed that $\nu = 1$, the ion particle confinement time, τ_{pi} , equals four times τ_{ce} , and the RFP operates at a beta limit with $f(\beta_\theta) = (\beta_{\theta c}/\beta_\theta)^m$ ($m = 6-8$ and $\beta_{\theta c} \approx 0.20$).

TABLE 4.8.-I

SUMMARY OF KEY PHYSICS ISSUES FOR THE RFP REACTOR

- ♦ Transport
 - Current, size, and beta scaling
 - Field errors, ripple, non-axisymmetry, magnetic islands
 - Radiative, beta-limiting, and intrinsic transport
- ♦ Heating
 - Efficient start-up and maximum use of dynamo
 - Ohmic to alpha-particle heating transition
 - Anomalous ion heating (start-up)
- ♦ Equilibrium and Stability
 - Control of terminations
 - Role of conducting shell (gaps)
 - Active (long-term) feedback
- ♦ Start-up
 - Efficient breakdown and RFP formation (E_ϕ , B_v , $B_{\phi 0}$, τ_R , P_0 , ...)
 - Slow ramp-up to ohmic ignition
- ♦ Current drive (OFCD)
 - Management of reactive power (plasma, circuit)
 - Impact on dynamo
 - Impact on impurity control
 - Drive coils (location and impact on equilibrium control)
 - Alternatives
- ♦ Impurity control (divertor or limiter)
 - Value of the radiation fraction
 - Impact of non-axisymmetry
 - Particle loads (energy, power)
 - Pumping efficiency
 - Lifetime (erosion)
 - Heat load (peaking, asymmetries)
 - Magnetics (divertor)
 - connection length
 - flux compression/expansion
- ♦ Fueling
 - Edge-plasma refueling
 - Pellet penetration

Together, these assumptions and the sensitivity of the reactor design to these assumptions form the essential issues for plasma transport and confinement. Specifically, the size range of present-day RFPs is insufficient to establish a firm r_p^2 dependence, although a theoretical basis for such a dependence exists. Next, if the I_ϕ dependence of τ_{ce} is weaker than $v = 0.8$, the impact during start-up on the OHC system becomes a concern. Finally, the RFP is assumed to operate at a beta limit. The transition of this beta-limited plasma scaling from ohmic to alpha-particle heating represents another unknown. As shown in Sec. 4.7.2., the RFP soft-beta limit permits operation with high radiation fractions. However, the level of intrinsic transport in the core plasma establishes an upper limit on f_{RAD} and becomes an important issue.

Fig. 4.8.-1. Summary of main physics issues and options for the RFP reactor and comparison with other confinement systems. Transport, start-up and equilibrium/stability are combined under plasma control.

Transport and confinement associated with non-intrinsic mechanisms related to magnetic islands, created by large-amplitude, (but high toroidal mode number) radial components of the magnetic field resulting from divertor coils or inadequately designed TFC sets, are also a concern that can generally be alleviated by careful design procedures. Furthermore, the nonuniformity of first-wall thermal heat load becomes a consideration in the context of maximum allowable field errors or ripples. This peaking of the first-wall heat load must be offset against the effect that island size has on the peak heat fluxes in the divertor, in that larger islands reduce peak heat fluxes in this region. From the viewpoint of electron runaway, the streaming parameter, $v_{TH}/v_D \propto (I_\phi/N) T_e^{1/2}$, must be monitored during early RFP formation/start-up, but becomes a minor consideration during the subsequent heating, ignition, and burn phases.

In summary, the developing RFP experimental database on transport and the observed strong current (or current-density) dependence is encouraging for an ohmically-heated and alpha-particle-sustained plasma. Furthermore, high- f_{RAD} plasmas have been achieved in the laboratory, albeit the level of intrinsic transport and its scaling in these relatively low-temperature (300-500 eV) experiments are not known. The dimensional and beta dependence of confinement remains to be resolved, but most causes for degraded confinement in present experiments seem, in principle, to be under design control (e.g., field errors, wall interactions).

4.8.2. Heating

The plasma current density and resistivity envisaged for the RFP reactor, as for the experiment, generate a local ohmic power density that in magnitude cannot be matched by any form of auxiliary heating. The adoption of ohmic heating as the primary heating method, therefore, does not represent an intrinsic issue, but instead is related more to the previously described issue of transport and confinement. The currents that provide the intense ohmic heating in the RFP also provide the primary poloidal confining field at the plasma edge. The transition from the start-up ohmic heating in the outer regions of the core plasma to the more central alpha-particle heating expected during ignition and OFCD-sustained burn represents an uncertainty, particularly with respect to the relaxation time scale and overall efficacy of the RFP dynamo and its response to the OFCD.

In connection with start-up, it is becoming evident that the use of the dynamo to build-up the internal toroidal field is desirable to minimize the TFC power, energy, and stress during formation and the early start-up. Application of ohmic heating on a subsequent slow ramp that minimizes poloidal-flux consumption also represents an area where further optimization of the heating cycle is needed.

The degree of anomalous ion heating, as observed in some present-day RFP experiments, can also aid greatly in the early formation phases. As indicated earlier, the changing heating and μ profiles as the plasma moves from the ohmic-heating domain (8 MW and 0.3 MW/m^3 average heating at steady state, 56 MW and 2 MW/m^3 peak) to the alpha-particle heating phase (453 MW and 16.4 MW/m^3 average) with OFCD represents uncharted territory.

In summary, strong ohmic heating represents the hallmark of poloidal-field-dominated systems in general and the RFP in particular. While the combination of heating and confinement has distinct advantages in reducing system cost and complexity for both the commercial reactor and the experiment, the inability to separate heating and transport does reduce the information available from a given experiment. Nevertheless, it seems unlikely that auxiliary heating would ever compete or be used in the reactor, although some advantages of local heating and profile control by non-ohmic means may exist.

4.8.3. Equilibrium and Stability

Global stability has been expressed in the reactor study in terms of a beta limit, as is observed in many RFP discharges, and equilibrium is accurately described by the usual Shafranov theory. Probably the issues having the largest uncertainty and impact on the FFC engineering design are the need and constraints of a conducting shell that fits closely to the plasma.

The thickness (time constant) of this shell may impact the blanket tritium-breeding ratio and peak heat flux allowed and its geometry and uniformity dictate the degree and shape of gaps and penetrations. Also, the eddy currents induced in the shell affect the start-up transients and OFCD performance (field errors, power losses, phase shifts). In addition, the standoff distance between the plasma-core edge and the conducting first-wall shell establishes the maximum thickness of the scrape-off layer, and therefore, strongly impacts the characteristics of the plasma in the scrape-off layer and the efficacy of the divertor (open or closed) impurity-control scheme.

Another aspect of equilibrium and stability is the abrupt termination of the plasma current, which represents an area where better understanding and control is required, although great progress has been made. Given that the reactor plasma kinetic energy under steady-state burn conditions is ~ 150 MJ (1.5 MJ/m^2) and the magnetic-field energy amounts to 1.6 GJ, the means of current-termination control that have worked so well in present-day experiment (density control, toroidal-field ramp-down at constant reversal parameter, F) must be extended to the reactor regime. The impact of alpha-particle-driven plasma oscillations on transport (both fuel and ash), stability, beta limits, and the overall RFP dynamo is largely unknown.

In summary, the near-minimum-energy RFP has proven grossly stable and easily held in equilibrium, with local instabilities generally leading to turbulence, reduced plasma quiescence, and a general reduction in the confinement properties, rather than gross disruptions. Hence, like heating, equilibrium and stability are more closely associated with transport and confinement, rather than presenting a serious threat to the global plasma integrity (i.e., gross plasma disruption in present tokamak experiments).

4.8.4. Start-up

The TITAN study has examined the formation and start-up constraints for the reactor and their impact on the steady-state subsystem designs. For the first time, a wide range of experimental data and observed (but not necessarily fully understood) formation/start-up constraints have been applied to the reactor design.

There are similar limits on E_ϕ/P_0 ($\geq 10^4$ V/m Torr) for both RFPs and tokamaks. Since filling-density limits for good RFP formation (i.e., adequate reversal, minimized poloidal-flux consumption) are higher than for tokamaks, higher values of E_ϕ are required to form the RFP. An initial current risetime in the range $\tau_{R0} = 10\text{--}100$ ms to achieve an initial (target) RFP of $I_\phi \approx 0.2\text{--}0.4$ MA with an expenditure of 3-30 Wb of poloidal-flux is suggested for an efficient matched-mode operation (i.e., constant toroidal flux). These parameters would be achieved using flux and energy expenditures determined by extrapolating the minimum RFP conditions derived from ZT-40 to a reactor which involves some level of uncertainty. This target-RFP plasma would then be subjected to a relatively fast current ramp (8-10 MA in a few seconds) to achieve 5-6 keV plasma and to minimize resistive poloidal-flux consumption. This fast ramp would be the result of a bipolar swing of the back-biased OHC. A

slower current ramp (≥ 10 s) to ignition and burn ($I_\phi = 18\text{--}20$ MA) would then be driven directly from the power grid.

The main issues remaining to be verified by subsequent experiment (RFX, ZT-H) include: a) scaling of minimum RFP conditions with j_ϕ , I_ϕ , r_p , etc.; b) the degree of electron runaway allowed upon formation; c) density control during RFP formation and during both fast (~ 1 s or ~ 10 MA/s) and slow (≥ 10 s or 1 MA/s) current ramp-ups; d) optimal conditions for both burn-through upon RFP formation and subsequent mounting the nt barrier just prior to ohmic ignition; e) the phasing of OFCD and impurity control systems during start-up, with the possibility of an OFCD assist to ignite and to reduce OHC design constraints (e.g., stresses); and f) the efficacy and power required of the EFC system, particularly during the early start-up phases. This last concern may require separate equilibrium control coils (resistive), located closer to the plasma, which could also provide EFC trim during OFCD.

In summary, the start-up procedure can have a strong impact in designing the steady-state RFP reactor, particularly the constraints imposed by the RFP formation and fast ramp-up (~ 1 s or ~ 10 MA/s) phases. Fortunately, a broad base of experimental evidence is available to contribute to the quantitative design of this aspect of the TITAN RFP, and most indications are favorable for achieving the ohmically ignited reactor burn at this time. During the formation and early start-up phase, power requirements, eddy-current generation, and equilibrium control may present some concerns.

4.8.5. Current Drive

Because of the higher resistance plasma and smaller physical size, an inductively pulsed compact RFP reactor would be sustained for a shorter period than a pulsed tokamak (100s versus 1,000s of seconds). Although not examined in detail, an inductively pulsed RFP must strive for reduced plasma resistance by increased minor radius and decreased major radius to an extent where superconducting TFCs and PFCs must be considered. A compact, high-power-density RFP reactor requires an efficient current-drive system.

Steady-state current drive by the phased oscillation of poloidal and toroidal fields (OFCD) has been adopted for the TITAN reactor design. Although some experimental and theoretical basis exist, substantial current driven by OFCD has not yet been demonstrated in the laboratory and, therefore, represents a main issue for the TITAN design. Given that the OFCD principle can be fully

demonstrated experimentally, the design of OFCD coils (e.g., location, sizes) and associated circuitry remains to be completed.

The drive coils can be located outside the FPC and can probably be incorporated as a subset of the main coil windings. Large reactive power flows across the plasma surface; however, the perturbation to the plasma (as measured in terms of field energy, magnetic field and current fluctuations, and fusion-power oscillations) is small. The impact of the driving field oscillations on the RFP dynamo, MHD behavior, and beta remain as unresolved issues. In addition, the maintenance of plasma equilibrium during the OFCD cycle, the impact of reactive power flows on the EFC, and overall energy balance remain to be resolved.

In summary, OFCD represents a large uncertainty for the compact RFP reactor, although preliminary experimental results from ZT-40 are encouraging [5,6]. Given that the OFCD principle of helicity injection is eventually proven in hotter RFP plasmas, several technological issues regarding the efficient application of OFCD to the reactor have to be examined. While this low-frequency (50-60 Hz) but high-reactive-power system appears to be achievable at a reasonable cost, the greatest concern at present is the losses related to eddy currents produced by the interactions of the drive-field with surrounding coils and structures. Furthermore, the requirements of the EFC system in maintaining gross equilibrium in the presence of OFCD have yet to be determined. An assessment of these uncertainties, however, must await a better resolved FPC design.

4.8.6. Fueling and Impurity Control

4.8.6.1. Fueling

The high densities required for a compact RFP reactor make the fueling more difficult. Gas-puffing will probably not penetrate beyond the separatrix and pellet injection, therefore, is required. Deep pellet penetration into the center of the plasma, as for tokamaks, requires very high injection velocities ($> 50\text{-}100\text{ km/s}$). Pellet ablation by energetic alpha particles may further increase the injection velocity.

The strong poloidal currents flowing in the outer regions of the RFP also create the complication of curved pellet trajectories and a difficulty for these pellets to reach the plasma center. The generally high level of inner-plasma turbulence in RFPs, however, may allow low-penetrating pellets and near-edge

refueling to supply sufficient fuel to the plasma core. Thus, for an RFP, deep pellet penetration is not a likely requirement.

4.8.6.2. Impurity control

Given that an inductive batch-burn mode of operation is not possible for the economic, compact reactor concepts being examined by the TITAN study, an active means of impurity control is a major requirement for the design. Arrays of poloidal pump limiters or poloidally "symmetric" toroidal-field magnetic divertors are being considered, and the concerns of limiter erosion and possible plasma contamination have resulted in a focus on the magnetic divertor. As for OFCD, the viability of the magnetic divertor for the RFP must be based primarily on computational models, although a stronger experimental database exists from tokamak experiments that can better guide the edge-plasma modeling effort.

The main issues in this area with respect to the magnetics are the impact of introducing magnetic field pitch minima near the divertor coils and the creation of magnetic islands. With respect to the former, since a magnetic island (i.e., the separatrix) cannot reconnect through a coil, the potential stability problem related to pitch minima should not be a concern near the divertor. The impact of magnetic islands on transport, on the other hand, can be minimized through careful coil design.

The plasma models of the scrape-off layer indicate that high peak heat loads are to be expected on the divertor target and the injection of high-Z impurities into the divertor chamber has been investigated in order to cool radiatively the divertor plasma and reduce the peak heat load. A high impurity fraction ($\geq 1\%$ of the electron density for xenon) is required for this purpose, and it is essential that the impurity remains confined in the divertor with no contamination of the core plasma. Entrainment of the impurities in the background plasma flow may not be effective if the strong recycling at the divertor target near the separatrix creates a local reversal of the flow (as observed in fully two-dimensional edge-plasma models), tending to drive the impurities out of the divertor. In this case the feasibility of impurity injection for enhanced radiation in the divertor chamber would be seriously in doubt. In addition to this concern, a large uncertainty exists in the modeling of impurities in the scrape-off layer in terms of the lack of accurate data for the radiative cooling rate of high-Z impurities at low plasma temperatures.

A problem with the closed divertor chamber configuration is that compression of magnetic flux and poloidal asymmetries in the field-line density

lead to an additional peaking factor in the divertor heat load. A more open divertor configuration alleviates this effect considerably, although the proximity of the divertor plate to the core plasma in this case raises the possibility of interaction between the divertor and core plasma.

A quasi two-dimensional edge-plasma model, incorporating an analytic neutral particle model at the divertor target is being used to investigate the issues raised above. Radiation from high Z impurities in the scrape-off layer (arising from the injection of impurities into the core plasma to increase the radiation fraction in that region as well as from possible injection into the divertor) will also be included more fully in estimating the divertor heat loads. The open divertor configuration will be examined in detail, with particular emphasis placed on the transport of neutral particles in this geometry.

In summary, a wide range of active impurity-control schemes is available for the RFP, including high wall coverage ($\sim 50\%$) poloidal pumped limits, and a range of toroidal magnetic divertor configurations. Focus has been placed on magnetic divertors to control the edge-plasma characteristics and protect the first wall from erosion. A range of options exist to operate with a highly radiating core plasma and edge-plasma conditions that reduce heat flux peaking and wall erosion. The models needed to describe these processes are necessarily two-dimensional and, therefore, represent a development item. Preliminary indications are that a number of self-consistent solutions to the toroidal divertor impurity control scheme exist. These options primarily in the context of the open divertor configuration will be pursued during the design phase. In all cases, the compact RFP reactor approach requires a large fraction ($f_{\text{RAD}} > 0.8$) of the alpha-particle power to be radiated uniformly to the first wall and impurity control surfaces facing the plasma.

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5. PARAMETRIC SYSTEMS STUDIES

R. L. Miller, and R. A. Krakowski

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5. PARAMETRIC SYSTEMS STUDIES

5.1. INTRODUCTION

A parametric systems analysis (PSA) computer code is used to identify "Strawman" TITAN design points and establish the context of the design by means of sensitivity and trade-off studies. The code was originally developed for use in the Los Alamos Compact Reversed Field Pinch Reactor (CRFPR) studies [1-3] and the cost database was updated in the course of the Los Alamos ATR/ST study [4]. Further possible updates of the cost database are discussed in Sec. 5.2.3. The PSA code is used as a centerpiece of a constellation of activities, characterized in Fig. 5.1.-1, that comprises the TITAN design activity.

The objective of the Systems Analysis Activity is the systematic study and determination of plant operating parameters through economic analysis and optimization of the power station. The Strawman designs are chosen to meet program design goals (e.g., minimal cost of electricity, COE, and high mass power density). Code models include steady-state surveys designed to assess sensitivities and trade-offs related to various TITAN operating configurations and assumptions, as well as time-dependent start-up/burn simulations (Sec. 4.5.2). These models are bench-marked and calibrated against more detailed plasma physics and magnetic models to provide a framework for the overall design process. At least as important as the Strawman designs themselves is the parametric context established by the trade studies.

As indicated on Fig. 5.1.-2, the PSA code identifies optimal parameters in a set of nested search loops centered on a convergence operation for the engineering Q-value, $Q_E = 1/\epsilon$, and the specified net-electric power, P_E , where ϵ is the recirculating power fraction. For a given total coil thickness, δ_c , this inner iteration searches for the value of Q_E that yields the specified P_E as the split between the toroidal-field coil (TFC) and poloidal-field coil (PFC) geometry varies, subject to the constraints of equal (but unspecified) coil current densities and the matching of fixed engineering and physics parameters. The PSA code algorithm used in the CRFPR study (Fig. 6.-1 of Ref. 3) has been modified to treat the equilibrium-field (EF) and ohmic-heating (OH) coil sets separately, allowing the consideration of superconducting EFCs. The total TF + OH coil thickness δ_c that produces a minimum-COE design for an otherwise fixed geometry, including plasma minor radius, is first determined after convergence of the set (Q_E, P_{TH}) for a given P_E . The outer-loop optimum is then

Fig. 5.1.-1. TITAN study approach organized by key plasma and nuclear engineering activities and interfaces. NEQ [5,6], NEWLIT [7], ONEDANT [8], TRIDENT-CTR [9], MCNP [10], CCOIL [2], TORSIDO [11], and ZEPHYR [12] are computer codes of potential use in the TITAN study.

Fig. 5.1.-2. Parametric Systems Analysis (PSA) procedure used for the TITAN study, as adapted from CRFPR methods [1,2].

determined as a function of plasma radius, r_p , which shows a higher-order (lower) COE minimum. Generally, r_p is used as a display variable, with the respective minimum-COE design corresponding to a particular value of P_E and plasma aspect ratio, $A = R_T/r_p$. The outermost loop then varies the aspect ratio in search of an even lower minimum-COE system, although within realistic bounds, the optimum in A is relatively flat for the RFP. These fully-cost-optimized design points are then examined as a function of P_E and various physics, engineering, and economic parameters and options. The results of this analysis identify Strawman design points which serve as the starting points for conceptual engineering analysis and elaboration.

Section 5.2 summarizes the physics, engineering, and costing models used in the PSA code. Sec. 5.3 presents results of the PSA studies performed to date and summarizes the parameters of the present set of Strawman design points.

5.2. MODELS

5.2.1. Plasma Physics Models

The TITAN systems model begins with a steady-state, point-plasma model, corrected for profile effects. It is convenient to define a filling fraction $x \equiv r_p/r_w$ for the circularized plasma in a toroidal chamber where r_w is the minor radius of the first wall. The parameter x is chosen to anticipate the first-wall geometry and scrape-off layer thickness in relating the volumetric fusion power to the average 14.1 MeV neutron first-wall loading.

The average plasma DT fusion (17.58 MeV/fusion) power density, P_F/V_p , is given by

$$P_F/V_p \text{ (W/m}^3\text{)} = 7.04 \times 10^{13} n_i^2 \langle \sigma v \rangle_{DT} g_{DT} . \quad (5.2.-1)$$

In the above equation, n_i is the average DT ion density, $\langle \sigma v \rangle_{DT}$ is the DT fusion reactivity, g_{DT} is the fusion-power profile correction factor. The $\langle \sigma v \rangle_{DT}(T)$ values used in this study are based on the recent Los Alamos experimental measurement and temperature-dependent fitting function [13] in the range $0 < T < 20$ keV, with typical results summarized in Table 5.2.-I. Differences in physics assumption between the TITAN study and earlier CRFPR studies are summarized in Table 5.2.-II. For purposes of the TITAN study, a lower value of poloidal beta, β_θ , and flatter radial profiles of plasma temperature and density

TABLE 5.2.-I
MAXWELLIAN-AVERAGED DT FUSION CROSS SECTIONS [13]

T(keV)	$\langle\sigma v\rangle_{DT}(m^3/s)$	$\langle\sigma v\rangle_{DT}/T^2(m^3/s-kev^2)$
0.5	5.58×10^{29}	2.23×10^{28}
1.0	6.71×10^{27}	6.71×10^{27}
5.0	1.33×10^{23}	5.30×10^{25}
10.0	1.12×10^{22}	1.12×10^{24}
13.5	2.21×10^{22}	$1.215 \times 10^{24}(\text{max})$
15.0	2.71×10^{22}	1.20×10^{24}
20.0	4.29×10^{22}	1.07×10^{24}
25.0(a)	5.95×10^{22}	9.52×10^{25}

(a) Out of nominal range of fitting function.

density have been assumed; the flatter profiles are a result of 1-D plasma simulations (Sec. 4.7.2.1). The "g" values reported in Table 5.2.-II measure the peaked-profile enhancement of fusion power (DT), ohmic heating (OHM), and Bremsstrahlung (BR) relative to the values obtained from flat profiles. Generally, the volume-averaged power density is given by

$$P_i \equiv \langle P_i(r) \rangle = \langle f_i [B(r), j(r), n(r), T(r)] \rangle, \quad (5.2.-2)$$

where the subscript "i" denotes fusion, radiation, or ohmic-heating power densities. The systems model calculates volume-averaged power densities, P_i , using average parameters; all profile information is contained in the profile enhancement factors g_i , where

$$P_i = g_i f_i(\langle B \rangle, \langle j \rangle, \langle n \rangle, \langle T \rangle). \quad (5.2.-3)$$

Henceforth, all quantities are volume-averaged except otherwise specified. The average current density, plasma density, and plasma temperature used in Eq. (5.2.-1) are defined as follows:

TABLE 5.2.-II
COMPARISON OF BASELINE PHYSICS PARAMETERS

<u>Parameter</u>	<u>CRFPR [2-4]</u>	<u>TITAN(1-D)</u>
$T(r)/T(0)$	$J_0(ur)$	$1 - (r/r_p)^4$
$n(r)/n(0)$	$J_0(ur)$	$1 - (r/r_p)^{2.5}$
$\mu(r)/\mu(0)$	$\begin{cases} 1 & (r < r_r) \\ \frac{r_p - r}{r_p - r_r} & (r_r < r < r_p) \end{cases}$	$1 - (r/r_p)^8$
$T(\text{keV})$	10.	10.
Poloidal beta, β_θ	0.20	0.13
Pinch parameter, Θ	1.55	1.47
Reversal parameter, F	-0.12	-0.11
Reactivity enhancement, g_{DT}	2.23	1.59
Ohmic heating, g_{OHM}	5.08	3.62
Bremsstrahlung, g_{BR}	1.52	1.33
$n\tau_E$ ($m^3 s$) @ $T = 20 \text{ keV}$	--	1.1×10^{20}

$$j_\phi \equiv I_\phi / A_p, \quad (5.2.-4a)$$

$$n \equiv \frac{2\pi}{A_p} \int_0^{r_p} n(r) r dr, \quad (5.2.-4b)$$

$$T \equiv \frac{2\pi}{n A_p} \int_0^{r_p} T(r) n(r) r dr, \quad (5.2.-4c)$$

where I_ϕ is the toroidal plasma current and $A_p = \pi r_p^2$ is the plasma minor cross-sectional area. The profile factors are then defined as

$$g_i = \frac{2\pi}{P_i A_p} \int_0^{r_p} P_i(r) r dr . \quad (5.2.-5)$$

The ohmic-heating profile correction factor, for example, becomes

$$g_{OHM} = \frac{2\pi \int_0^{r_p} \eta_{||} [n(r), T(r)] [j_\phi^2(r) + j_\theta^2(r)] r dr}{\eta_{||}(n, T) \frac{2}{I_\phi^2 A_p}} , \quad (5.2.-6)$$

where $\eta_{||}$ is the classical Spitzer resistivity. It is the natural tendency for the RFP configuration to relax to a $\beta = 0$, $\mu = \mu_0 \vec{j} \cdot \vec{B} / B^2 = \text{constant}$ (force-free) Taylor minimum-energy state [14], characterized by F (the reversal parameter), Θ (the pinch parameter), and $\mu(r)$, where

$$F \equiv \bar{B}_\phi(r_p) / \langle B_\phi \rangle \quad (5.2.-7)$$

$$\Theta \equiv \bar{B}_\theta(r_p) / \langle B_\phi \rangle \quad (5.2.-8)$$

$$\mu(r) / \mu(0) \equiv \bar{j}(r) \cdot \bar{B}(r) / \bar{j}(0) \cdot \bar{B}(0) , \quad (5.2.-9)$$

again, with $\bar{B}_\phi$, $\bar{F}$, $\bar{j}(r)$ being flux-surface-averaged quantities. Typically, $\mu(r)$ is constant within the central plasma and decreases more-or-less linearly to zero near the cold plasma edge, where the highly resistive edge-plasma cannot support large current densities.

The plasma-surface-averaged poloidal magnetic field, $\bar{B}_\theta(r_p)$, is approximated by

$$\bar{B}_\theta(r_p) = \frac{\mu_0 r_\phi}{2\pi r_p} . \quad (5.2.-10)$$

The plasma self-inductance, L_p , is approximated by

$$L_p(H) = \mu_o R_T \left[\ln \left(\frac{8R_T}{r_p} \right) + \frac{l_i}{2} - 2.0 \right], \quad (5.2.-11)$$

where $l_i \equiv \langle B_\theta^2 \rangle / \bar{B}_\theta^2(a)$ is the plasma internal inductance per unit length normalized to $\mu_o = 4\pi \times 10^{-7}$ H/m. The vertical field, B_V , required to maintain the plasma toroidal equilibrium becomes

$$B_V(T) = - \frac{\mu_o I_\phi}{4\pi R_T} \left[\ln \left(\frac{8R_T}{r_p} \right) + \beta_\theta + \frac{l_i}{2} - \frac{3}{2} \right], \quad (5.2.-12)$$

Neglecting corrections for $Z_{eff} > 1$, the plasma pressure balance can be written in the form

$$I_\phi = \left[\frac{2 p}{\beta_\theta \mu_o} \right]^{1/2} 2\pi r_p, \quad (5.2.-13)$$

where the plasma pressure is $p = 2nk_B T$ and k_B is the Boltzmann constant.

The PSA code searches for minimum-COE design points satisfying the ignition condition while balancing the plasma ohmic and fusion-product power inputs against radiation and transport losses, such that

$$P_\Omega + P_\alpha = P_{RAD} + P_{TR} \quad (5.2.-14)$$

at a profile-corrected Lawson parameter, $n\tau_E$, value consistent with the stipulated average plasma operating temperature, T [15]. The plasma ohmic power, P_Ω , is given by

$$P_\Omega = g_{OHM} I_\phi^2 R_p = g_{OHM} \eta_\parallel j_\phi^2 V_p, \quad (5.2.-15)$$

where $\eta_\parallel$ is the classical plasma resistivity. The plasma current is maintained at steady state by Oscillating Field Current Drive system (OFCD). The OFCD system power, P_{CD} , is (see Sec. 4.6)

$$P_{CD} = P_Q \left(1 + \frac{Q_p}{Q_c} \right), \quad (5.2.-16)$$

where Q_p and Q_c characterize the plasma coupling and circuit performance, respectively.

The RFP energy confinement time, τ_E , is assumed to scale from values obtained in present-day experiments according to

$$\tau_E(\text{PHYS}) = C_v [I_\phi(\text{MA})]^v r_p^2 f(\beta_\theta) \quad (5.2.-17)$$

with typical values of the scaling exponent, v , and numerical coefficient summarized in Table 5.2.-III. The function $f(\beta_\theta)$ models the soft-beta limit and assumed to have the form, $f(\beta_\theta) = 1$ for $\beta_\theta < \beta_{\theta c}$ (≈ 0.19) and $f(\beta_\theta) = (\beta_\theta/\beta_{\theta c})^8$ for $\beta_\theta > \beta_{\theta c}$. The energy confinement time of a PSA-code-determined minimum-COE design point, $\tau_E(\text{OPT})$, at $P_E = 1,000$ MWe is typically consistent with $v \approx 1.0$. Smaller output reactors with smaller values of r_p typically require $v > 1.0$ (i.e., better intrinsic energy confinement).

5.2.2. Reactor Engineering Models

Given a stipulated target net-electric power output, P_E , the thermal power output, P_{TH} , and recirculating power fraction, $\epsilon \equiv 1/Q_E$, are determined for a nominal value of the thermal conversion efficiency, η_{TH} , such that $P_E = \eta_{TH}(1 - \epsilon)P_{TH}$. The gross electrical power output is $P_{ET} = \eta_{TH}P_{TH}$. A

TABLE 5.2.-III

RFP ENERGY CONFINEMENT SCALING ^(a) PARAMETERS	
v	C_v
1.5	0.140
1.25	0.085
1.1	0.062
1.0	0.050
0.9	0.040

$$^{(a)} \tau_E(\text{s}) = C_v [I_\phi(\text{MA})]^v r_p^2$$

fraction $f_{AUX} = 0.07$ of P_{ET} is allocated for primary-loop pumping power and other auxiliary functions, such that $P_{AUX} = f_{AUX} P_{ET}$. The plasma ohmic-heating power, P_Q , resistive-coil Joule dissipation in the respective coil sets, P_{TFC}^Q and P_{DFC}^Q , and current-drive power, P_{CD} , complete the components of recirculating power for the TITAN. If the EFC is taken to be superconducting, $P_{EFC}^Q = 0$. Additionally, following the start-up transient, the OHC current can be slowly ramped down to zero, such that for purposes of the average steady-state power balance, $P_{OHC}^Q = 0$. The engineering Q-value figure of merit, Q_E , can be written as

$$Q_E = \frac{1}{\epsilon} = \frac{\eta_{TH}(M_N P_N + P_{RAD} + P_{TR} + P_Q)}{P_Q + P_{AUX} + P_{TFC}^Q + P_{DFC}^Q + P_{CD}}, \quad (5.2.-18)$$

where M_N [assumed to be 1.33 pending calibration by neutronics calculations, (Sec. 8)] is the blanket fusion-neutron energy multiplication. The average neutron wall loading, I_w , is given by

$$I_w (\text{MW/m}^2) = \frac{P_{TH} x}{(2\pi)^2 A r_p^2 (M_N + 0.25)}, \quad (5.2.-19)$$

and $x \equiv r_p/r_w$. A scrape-off layer thickness of 0.05 m is presently assumed, such that $r_w = r_p + 0.05$.

The normal-conducting copper-coil resistivity is taken to be $\rho_{Cu} = 2.0 \times 10^{-8} \Omega \cdot \text{m}$. Typically, the effective resistivity is increased by $1/\lambda_c$, where $\lambda_c \approx 0.7$ is the assumed conductor filling fraction, pending a more-detailed design of the coil internals. The resistive coil density is consistent with a composition of 70 v/o copper conductor, 10 v/o stainless-steel structure, 10 v/o MgO insulation, and 10 v/o (drained) coolant. For superconducting EFCs, the maximum coil current density is

$$j_m (\text{MA/m}^2) = \frac{(96 - 6B_{\theta c})}{\{1 + (B_{\theta c}/12)^{1.5}\}}, \quad (5.2.-20)$$

where $B_{\theta c}$ is the magnetic-field strength in Tesla, calculated at the EFC surface.

Drained blanket, shield, and coil masses are calculated using homogenized densities $\rho = 7.75, 7.0, 7.3$ tonnes/m³, respectively. A PbLi eutectic breeder/coolant material density is 9.4 tonnes/m³, as used in the CRFPR [2,3] compared with 0.5 tonnes/m³ for Li, one of the present TITAN options. The FPC mass, M_{FPC} , is used to compute mass utilization M_{FPC}/P_{TH} (tonne/MWt) and mass power density, MPD (kWe/tonne), figures of merit (Ref. 16 and Appendix C of Ref. 4).

Steady-state operation of the TITAN RFP reactor relieves thermal-fatigue problems and increases the system reliability. Commercial operation also requires adequate maintenance access. The goal of incorporating fully remote, single-piece maintenance in the reactor building and hot cell exerts another strong influence on system economics, particularly from the viewpoint of plant availability. Remote handling is presently undergoing rapid development, and it is assumed that the necessary equipment will have been developed.

System redundancy, steady-state operation, ease of reactor torus replacement, and development of reliable components should permit the nominal overall plant availability of 76% for the TITAN designs. Steady-state operation should considerably improve reliability for the application of economically optimum engineering safety factors. The plant availability is reduced from 100% because of outage time for scheduled, t_s , and unscheduled, t_u , maintenance periods. The plant availability, $p_f = (365 - t_u - t_s)/365$, where t_u and t_s are expressed in days. The scheduled outage time has been estimated as 28 days per reactor-torus replacement. To achieve the target availability of 76%, the unscheduled outage is set at 60 days per year. The periodic first-wall and blanket replacement becomes an important operational feature. A first-wall lifetime of $I_w \tau = 15$ MWyr/m² is assumed, which, together with a plant availability of 76% results in annual FW/B/S replacement at $I_w \approx 20$ MW/m². Higher wall loadings require additional increments of 28 days of replacement, resulting in higher values of the COE which in turn determines the minimum of the COE as a function of the wall loading.

An alternative availability algorithm has been proposed by the FEDC group, based on empirical coal and fission experience as it relates to plant size and penalizing for higher wall-loading according to

$$p_f^* = 0.827 - 0.060 \left(\frac{P_E(\text{MWe})}{1000} \right) - 0.017 \left(\frac{I_w(\text{MW/m}^2)}{20} \right) . \quad (5.2.-21)$$

For the nominal min-COE Strawman design point, $p_f \approx p_f^* = 0.75$, but the cost penalty of lower-wall-loading operation is reduced using this algorithm.

5.2.3. Costing

The Cost of Electricity (COE) is the most important evaluation tool to optimize and to compare with alternative energy sources. Both constant-1986 and then-current-1991 dollar analyses are used to evaluate the TITAN economic parameters for an assumed 5-yr construction time. The general equation for bus-bar energy cost is given by

$$\text{COE} = \frac{C_{AC} + (C_{O\&M} + C_{SCR} + C_F)(1 + E)^P}{8760 P_E p_f} , \quad (5.2.-22)$$

where

COE = Cost of electricity in constant or then-current dollars (mills/kWeh),

C_{AC} = Annual capital cost charge, equals total capital cost multiplied by fixed charge rate (0.10 for constant-dollar analysis or 0.15 for then-current-dollar analysis),

$C_{O\&M}$ = Annual operations and maintenance cost, $C_{40} + C_{41} + \dots + C_{47}$,

C_{SCR} = Annual scheduled component replacement cost, $C_{50} + C_{51}$,

C_F = Annual fuel costs, C_{02} and C_{03} ,

E = Escalation rate equals 0.0 for constant-dollar analysis and 0.05 for then-current-dollar analysis,

P = Construction period (yr),

P_E = Net plant capacity (MWe), and

p_f = Plant availability factor.

The essential elements of the TITAN cost database [2,4] are summarized in Table 5.2.-IV. Costs as scaled from 1980 to 1986 using the factor 1.348 [4]. For purposes of costing in the parametric systems model, the reactor building is divided into a variable-volume reactor cell, housing the FPC and vacuum tank, and a fixed-volume region, housing the primary heat-transfer/transport loops. The volume of the latter portion is estimated to be $1.55 \times 10^5 \text{ m}^3$ and is similar to that of the STARFIRE design [17]. The reactor room is modeled by a rectilinear enclosure extending horizontally 9 m beyond the FPC with a height approximately six times that of the FPC, such that $V_{RB} = -\{2(R_T + r_s + 9)\}^2(12r_s) \text{ m}^3$. The basic building structure (Account 21.2.1) is priced at $300 \text{ \$/m}^3$, a value intermediate between that of STARFIRE [17] and MARS [18], to which is added 2 M\$ for building services (Account 21.2.2), 30 M\$ for containment structures (Account 21.2.3), and 7.5 M\$ for architectural costs (Account 21.2.4).

The Main Heat Transfer System includes a liquid-metal (LM) loop serving the blanket, divertor, and shield. Allowances are made for a fraction, f_w , of thermal power to be delivered to a pressurized-water loop. The cost of the LM loop (Account 22.2.1) is estimated to be $3.40 \times 10^4 P_{TH}(1 - f_w) \text{ M\$}$ and that of a pressurized-water loop (Account 22.2.3) is estimated to be $3.50 \times 10^4 P_{TH}f_w \text{ M\$}$, these estimates being calibrated by the dual-media (PbLi + H₂O) MARS design [18] with a reduction of 80% of the dominant piping costs of that design to reflect the shorter pipe runs in the TITAN case. This model results in a ~ 50 M\$ increase in cost over the pressurized-water Main Heat Transfer System in STARFIRE [17]. In the TITAN case, where Li is the sole primary coolant, $f_w = 0$, unlike the dual-media system of the CRFPR. The LM inventory in the system consists of 95% of the blanket volume, corrected by a factor of 1.09 to account for the FPC ducts connecting the blanket through the TFC/PFC sets to the main manifolds. To this variable volume is added a fixed increment (~ 500 m³) for the primary-loop inventory, a value assumed to be relatively constant over the parameter range of interest. The cost of the primary-loop LM is reported under Special Materials (Account 26), insofar as it is salvageable and reuseable. The unit cost of the LM (PbLi or Li) is an increasing function of the ⁶Li enrichment, f_{6Li} .

The first-wall/blanket/divertor (or limiter) replacement cost estimate applies a factor of two to the direct cost of these components to allow for the handling/replacement of the spent reactor torus. For an assumed first wall life of $I_w \tau = 15 \text{ MWyr/m}^2$ at a cost-optimized neutron wall loading, $I_w = 20 \text{ MW/m}^2$ and

TABLE 5.2.-IV
SUMMARY FUSION REACTOR COST DATABASE^(a)

<u>ACC. NO</u>	<u>ACCOUNT TITLE</u>	<u>(M\$, 1980)</u>
20.	Land and land rights	3.3
21.	Structures and site facilities	
21.1	Site improvements and facilities	11.15
21.2	Reactor building	$3. \times 10^{-4} V_{RB} + 39.5$
21.3	Turbine building	33.5
21.4	Cooling structures	$7.135(P_{ET}/1000)^{0.3}$
21.5	Power supply and energy storage	9.16
21.6	Miscellaneous buildings	76.5
21.7	Ventilation stack	1.81
21.98	Spare parts (2%)	
21.99	Contingency (15%)	
22.	Reactor Plant Equipment	
22.1	Reactor Equipment	
22.1.1	Blanket and first-wall structure	$0.0533 M_{BL}$
22.1.2	Shield	$0.015 M_{SHD}$
22.1.3	Magnet coils	$0.040 M_C(NC) \text{ or } 0.080 M_C(SC)$
22.1.4	Supplemental heating systems	$1.65 P_{rf}$
22.1.5	Primary structure and support	$0.1125 V_{STR}$
22.1.6	Reactor vacuum system	$0.015 M_{VAC} + 0.83(\dot{r}_F/250)$
22.1.7	Power supply (switching & energy storage)	$1.0 + 0.0148(P_C^2 + P_{CD})$
22.1.8	Impurity control system	$0.0026 V_{VAC}$
22.1.9	Direct energy conversion	0.0
22.1.10	ECRH Breakdown system	2.82
22.2	Main heat transfer system	
22.2.1	Primary coolant (LM)	$0.034 P_{TH} (1 - f_w)$
22.2.2	Intermediate coolant system	0.0
22.2.3	FW/Limiter/Shield coolant system (H ₂ O)	$0.035 P_{TH} f_w$

TABLE 5.2.-IV (Cont.)
SUMMARY FUSION REACTOR COST DATABASE^(a)

<u>ACC. NO</u>	<u>ACCOUNT TITLE</u>	<u>(M\$, 1980)</u>
22.3	Auxiliary cooling systems	$6.7 \times 10^4 P_{TH}$
22.4	Radioactive waste treatment	$1.2 \times 10^3 P_{TH}$
22.5	Fuel handling and storage	$9.65 \times 10^3 P_{TH}$
22.6	Other reactor plant equipment	$1.09 \times 10^2 P_{TH}$
22.7	Instrumentation and control	23.41
22.98	Spare parts allowance (2%)	
22.99	Contingency allowance (15%)	
23.	Turbine plant equipment	
23.1	Turbine-generators	$59.9 (P_{ET}/1000)^{0.7}$
23.2	Main steam system	$4.80 (P_{TH}/2860)$
23.3	Heat rejection systems	$33.0 (P_{TH}/2860)^{0.8}$
23.4	Condensing system	$13.8 (P_{ET}/1000)^{0.9}$
23.5	Feed heating system	$7.55 (P_{TH}/2860)$
23.6	Other turbine plant equipment	$40.9 (P_{ET}/1000)^{0.6}$
23.7	Instrumentation and control	$7.80 (P_{ET}/1000)^{0.3}$
23.98	Spare parts allowance (2%)	
23.99	Contingency allowance (15%)	
24.	Electric plant equipment	
24.1	Switchgear	$8.6 (P_{ET}/1000)$
24.2	Station service equipment	$14.2 (P_{ET}/1000)$
24.3	Switchboards	$5.4 (P_{ET}/1000)$
24.4	Protective equipment	2.11
24.5	Electrical structures and wiring containers	$11.12 + 6.28 (P_{ET}/1440)$
24.6	Power and control wiring	$23.0 + 13.0 (P_{ET}/1440)$
24.7	Electrical lighting	8.2
24.98	Spare parts allowance (4%)	
24.99	Contingency allowance (15%)	

TABLE 5.2.-IV (Cont.)
SUMMARY FUSION REACTOR COST DATABASE^(a)

ACC. NO	ACCOUNT TITLE	(M\$, 1980)
25.	Miscellaneous plant equipment	
25.1	Transportation and lifting equipment	15.68
25.2	Air and water service systems	12.35
25.3	Communications equipment	6.22
25.4	Furnishings and fixtures	1.20
25.98	Spare parts allowance (3%)	
25.99	Contingency allowance (15%)	
26.	Special Materials	
26.1	Reactor LM coolant/breeder ^(b)	PbLi: $M_{LM}(7.83f_{6Li} + 2.46)10^{-3}$ Li: $M_{LM}(1169f_{6Li} - 58.0)10^{-3}$
26.4	Other	0.25
90.	Total direct cost (TDC)	
91.	Construction facilities, equipment, and services (10%)	
92.	Engineering and construction management services (8%)	
93.	Other costs (5%)	
94.	Interest during construction, (IDC, 10%/yr)	
95.	Escalation during construction, (EDC, 5%/yr)	
99.	Total cost	

(a) Gross electric, P_{ET} , net electric, P_E , and total thermal, P_{TH} , powers given in MW. Volumetric $V(m^3)$ abbreviations or corresponding mass $M(\text{tonne})$ costs for the fusion power core (FPC) and related items are given as follows:

Reactor building, $V_{RB} = 4(R_T + r_S)^2(12r_S) + 1.55 \times 10^5 (m^3)$

Blanket structure (5%), $M_{BL}(\text{tonne})$, Shield, $M_{SHD}(\text{tonne})$

Magnet coils, $M_C(\text{tonne})$, Structure, $V_{STR}(m^3)$

Vacuum tank, $M_{VAC} = (0.07)(7.8)2\pi[(R_T + r_S + 3)^2 + 4r_S(R + r_S + 3)] (\text{tonne})$

(b) Liquid-metal, $M_{LM} (\text{tonne})$: ${}^6\text{Li}$ enriched, $0.075 < f_{6Li} < 0.90$.

a plant factor ≈ 0.76 , routine replacement occurs annually. Account 50 represents $\sim 3\%$ of the base-case COE for the TITAN and is distinct from the nominal annual O&M charge (Accounts 40-47, 51), conservatively estimated [19-20] to be 2% of the direct cost. This scheme costs the first reactor, first wall, and blanket twice, and credit for any reactor-torus component reuse (i.e., TFCs or shield) is not taken.

An updating of the prevailing cost accounting scheme [19-20] and unit-cost database is presently underway [21-22], subject to community review and consensus. Cost values obtained under the revised scheme are not expected to differ dramatically from the results reported herein. A preliminary comparison showed overall agreement of the direct-cost models to within 5%.

5.3. PARAMETRIC RESULTS

5.3.1. Sensitivity and Trade-Offs

The CRFPR framework studies [2,3,23,24] focused on a design with a neutron wall loading of $I_w \approx 20 \text{ MW/m}^2$ [CRFPR(20)], high-coverage (poloidal) pump limiters, a self-cooled $\text{Pb}_{83}\text{Li}_{17}$ /ferritic-steel (HT-9) blanket, thin (0.10-m) steel shielding, closely coupled copper-alloy toroidal-field (TFCs) and poloidal-field coils (PFCs, includes both ohmic-heating, OHCs, and equilibrium-field coils EFCs), oscillating-field current drive (OFCD) for steady-state operation [25-26], and single-piece PFC (800-tonne PFC, 300-tonne first-wall/blanket/shield/TFC) maintenance.

The present focus of the TITAN study is a divertor-based [27], high-neutron-wall-loading ($10\text{-}20 \text{ MW/m}^2$) design that also invokes OFCD for steady-state operation, retaining the motivation of high power density, compact fusion [28-30]. A range of pool- and loop-type blanket concepts is being considered. The desire to eliminate steady-state power consumption in the resistive EFC (53.5 MW for the CRFPR(20) framework design [22-23]) combined with a desire for a more open PFC geometry, which in turn is penalized by poorer coupling of PFCs to the plasma, points towards cost and operational incentives for otherwise more expensive superconducting EFCs, particularly if pool-type blankets, resistive-coil divertors, and more conservative physics assumptions (lower beta, flatter plasma profiles) are used. The OHCs and TFCs in TITAN, however, have remained as resistive-coil systems in order to retain a compact reactor torus, with the OHC being used and sized for start-up conditions only. Both OHCs and TFCs ideally would also serve current-drive functions, depending on the electrical

design of the intervening first wall, blanket, and shield. The OHC is also sized for full grid power applied in the back-biased condition and 20% of grid power available for resistive OHC losses in the forward-bias condition just prior to application of OFCD and steady-state operation. Somewhat higher plasma currents are required compared to the CRFPR(20) design because, for purposes of this section, the maximum poloidal beta has been decreased from $\beta_\theta = 0.2$ to a more conservative value of 0.13, and flatter density and temperature profiles have been assumed, as suggested by one-dimensional plasma simulations performed as part of the TITAN study (see Sec. 4.7.2.1).

In order to examine at the earliest stages of the TITAN study the impact and sensitivities of the above-described changes, the systems model described in Ref. 3 was expanded to include a self-consistent treatment of separate OHCs and EFCs, as depicted in Fig. 5.3.-1. The scaling of poloidally symmetric toroidal-field divertors [27] and OFCD [2,25,26] was also included in the optimization algorithm. The computational algorithm used remains essentially as described in Ref. 3. As for any model of this nature, best choices for input are made on the basis of separate and detailed neutronics, plasma equilibrium, OFCD, divertor, and thermal-hydraulic calculations; important tradeoffs like construction time versus size and complexity, mean-time-to-repair versus mean-time-to-fail as a function of power density and size, and elasticity of nuclear and size economies of scale for key components remain inadequately resolved, however, and in need of future work.

Table 5.3.-1 lists key design variables that were either fixed or varied in the re-optimization of the RFP reactor for the TITAN study. The blanket/shield standoff distance between the first wall and TFC/OHC sets is typical of a self-cooled, ^6Li -enriched liquid-metal blanket, although a range of pool and loop concepts is being examined. The range of thermal-conversion efficiencies considered reflects primarily the blanket choice and whether or not the first wall and/or divertor chamber walls require a separate and possibly lower-temperature coolant.

The variation of cost with plasma aspect ratio, $A = R_T/r_p$, is weak in the range examined ($A = 5-9$). Establishing a maximum grid power of $P_{\text{GRID}} \approx 300$ MWe delivered to the OHC in the back-bias mode during start-up, and maintaining the peak von Mises stresses in the OHC below ~ 200 MPa sets a limit of $A \geq 5.5-6$; a baseline value of $A = 6.5$ was selected to allow for added start-up flux. Figure 5.3.-2 gives the last level of constant-1986 COE minimization (i.e., r_p variation) for the baseline ($A = 6.5$); the effects of normal-conducting (NC)

Fig. 5.3.-1. Fusion-power-core (FPC) model used to optimize and perform sensitivity studies of resistive-coil RFP reactors. The computational model loops through all FPC and plasma dimensions in search of minimum-COE designs.

TABLE 5.3.-I
FIXED AND VARIED PARAMETERS FOR TITAN RFP REACTOR
OPTIMIZATION AND SENSITIVITY STUDIES^(a)

Minor plasma radius, $r_p(m)$	[0.60]
Plasma aspect ratio, $A = R_T/r_p$	[6.5]
Plasma average temperature, $T(keV)$	10.
Poloidal β_θ	[0.13]
Temperature/density profiles, $T(r)/T(0)$, $n(r)/n(0) = 1 - (r/r_p)^v$ $v = 4, 2.5$	
Lawson parameter, $n\tau_E(10^{20} \text{ s/m}^3)$	1.60
Pinch parameter, $\Theta = B_\theta(r_p)/\langle B_\phi \rangle$	1.47
Reversal parameter, $F = B_\phi(r_p)/\langle B_\phi \rangle$	-0.11
Thermal-conversion efficiency, η_{TH}	[0.40]
EFC option	SC or NC
OHC, TFC, or DFC options	NC
Blanket/gap/shield standoff, $\Delta(m) = \Delta b + \Delta g + \Delta s$	[0.78]
EFC shield standoff	{0.0(NC), 1.5(SC)}
Blanket multiplication, M_N	1.33
SC coil current density, $j_c(MA/m^2)^{(b)}$	$(96.-6B_{\theta c})/[1 + (B_{\theta c}/12)^{1.5}]$
NC current density, $j_c(MA/m^2)^{(c)}$	$\leq 50.$
Plant factor, p_f	(28 day/FPC scheduled maintenance, 60 day/year unscheduled maintenance)
FPC radiation lifetime, $I_w\tau(MWyr/m^2)$	[15.]
Typical FPC unit costs (\$/kg, 1986)	
♦ First-wall/blanket	
- $Pb_{83}Li_{17}(90\% \text{ } ^6Li)$	12.8
- HT-9	53.9
♦ Shield	20.2
♦ NC coil	53.9
♦ SC coil	107.8
♦ Structure	20.2
♦ OFCD power costs (\$/kVA)	[20.0]
OFCD plasma/circuit Q-values	[100]/[100.]

(a) Values in brackets [] were varied, with nominal design value being shown.

(b) Ref. 16.

(c) Cost optimization usually set j_c for resistive coils far below this limit, with $j_c = 5-10 \text{ MA/m}^2$ being typical.

versus superconducting (SC) EFCs and the net electric power variations are shown. The secondary results of I_w (MW/m²) and FPC mass power density, MPD (kWe/tonne), are also shown. Comparison of generally common-basis costs are made with the STARFIRE [17], MARS [18], and the Spherical Torus Advanced Tokamak Reactor [4] (ATR/ST, a low-aspect-ratio extrapolation of the present tokamak database, and also a system with SC/EFC, NC/TFC, and OFCD, but using RF start-up and no OHC).

The most prominent feature of Fig. 5.3.-2 is the shallowness of the COE versus r_p (and hence, I_w) minimum, although the compressed COE scale should be noted. Nevertheless, increasing I_w from 5 to 10 and then to the COE-minimum of 20 MW/m² results only in a 3 and 11% reduction, respectively, in COE. Other developmental and operational (i.e., single-piece maintenance) incentives not included in the present costing model can justify the higher- I_w , high-MPD design points that reside closer to the COE minimum. The dependence of COE on net plant capacity shown on Fig. 5.3.-2 is typical of the nuclear economy of scale and is shown explicitly on Fig. 5.3.-3; a comparison with fission and fossil (coal) energy costs [34,35] is also given.

An interim TITAN baseline reactor has been chosen to explore further technology requirements and cost sensitivities. Typical physics, engineering, and costing parameters are listed on Table 5.3.-II. This system is based on superconducting EFCs and generates a net electric power of $P_E = 1,000$ MWe(net).

The sensitivity of COE for the $I_w \approx 19$ MW/m² baseline design to changes in P_E , EFC choice, I_w , MPD, and other key design parameters is shown in Fig. 5.3.-4. Both $I_w \approx 19$ MW/m² and MPD = 544 kWe/tonne appear to be optimum values. Changes in the baseline values for beta ($\beta_0 = 0.13$) and FPC lifetime ($I_w\tau = 15$ MW/m²) by more than ~ 25% and ~ 50%, respectively, are required before more than a 10% respectively, effect is observed on COE. This relative insensitivity is a result of the small percentage contributed by the FPC to the overall plant direct cost (~ 7% compared to 25-30% for STARFIRE [17] and MARS [18]), despite higher unit (\$/kg) costs assumed for the TITAN reactor. It should be noted that these COE sensitivities represent single-point variations, and the resulting minimum-cost designs reflect a number of simultaneously changing features; for instance, the decrease in FPC radiation life increases cost both because of increased operating (blanket/shield replacement) cost as well as decreased I_w and MPD, with the latter decreased power density and increased direct costs resulting from the system re-optimization to maintain an

Fig. 5.3.-2. Dependence of near-minimum-COE designs on plasma minor radius, r_p , net electric power, P_E (MW_e), and superconducting versus normal-conducting EFC options. Shown also is the secondary dependence of neutron wall loading, I_w (MW/m²), and mass power density, MPD (kW_e/tonne); comparison with CRFPR(20) [3], STARFIRE [17], MARS [18], the Advanced Tokamak Reactor based on the Spherical Torus, ATR/ST [4,32], and a typical PWR [33], are also shown.

Fig. 5.3.-3. Projected cost of electricity, COE, as a function of net plant capacity and a comparison with fossil and fission costs for a range of coal costs (oil at 10 \$/bbl equals ~ 2.0 \$/MBTU), medium-experience and best-experience PWR fission reactor costs (respectively, PWR/ME and PWR/BE). The GENEROMAK [16] and "new-age" inherently-safe fission reactor cost projections [34] are also shown (LSPB: Large Scale Prototype Breeder, SAFR: Sodium Advanced Fast Reactor, PRISM: Power Reactor Inherently Safe, and HTR: High Temperature gas-cooled Reactor).

TABLE 5.3.-II
SUMMARY OF 1000-MWe(net) BASELINE RFP DESIGN

Plasma Parameters

Minor plasma radius, $r_p(m)$	0.61
Major plasma radius, $R_T(m)$	3.96
Plasma current, $I_\phi(MA)$	21.7
Plasma density, $n(10^{20}/m^3)$	8.17
Poloidal field at plasma surface, $B_\theta(T)$	7.11
Fusion power density, $P_F/V_p(MW/m^3)$	83.2
Plasma ohmic dissipation, $P_Q(MW)$	32.8

Poloidal-Field Quantities

Coil thickness, $\delta_{c\theta}(m)$	0.30
Average minor radius, $r_{c\theta}(m)$	1.62
Coil field, $B_{c\theta}(T)$	1.69
OHC current density, $j_{c\theta}(MA/m^2)(b)$	18.0
Mass of OHC set, $M_{OHC}(\text{tonne})$	396.9
EFC current density, $j_{c\theta}(MA/m^2)(c)$	18.0
Mass of EFC set (tonne)	376.8
OHC dissipation during back-bias (MW)	370.6

Toroidal-Field Quantities

Coil thickness, $\delta_{c\phi}(m)$	0.04
Average minor radius of coil, $r_{c\phi}(m)$	1.45
Mass of coil, $M_{TFC}(\text{tonne})$	39.1
TFC current density, $j_{c\phi}(MA/m^2)$	18.0
Ohmic dissipation during burn, $P_Q^{TFC}(MW)$	49.4
Mass of divertor coil, $M_{DFC}(\text{tonne})$	5.3
Ohmic dissipation in divertor, $P_Q^{DFC}(MW)$	33.0

TABLE 5.3.-II (Cont.)
SUMMARY OF 1000-MWe(net) BASELINE RFP DESIGN

<u>Engineering Summary</u>	
Engineering Q-value, $Q_E = 1/\epsilon$	5.27
Fusion power, P_F (MW)	2,416
Total thermal power, P_{TH} (MW)	3,086
Neutron Wall Loading, I_w (MW/m ²)	19.0
First-wall major radius, r_w (m)	0.66
Masses (tonne)	
♦ first wall/blanket	40.6
♦ shield	972.8
♦ total coil set	818.1
♦ FPC mass	1,836.8
♦ FPC structure	1,052.7
System power density, P_{TH}/V_{FPC} (MWt/m ³)	12.6
Mass power density, $1,000P_E/M_{FPC} = MPD$ (kWe/tonne) ^(d)	545.
<u>Cost Summary</u>	
Cost of electricity, COE(mills/kWeh)	37.2
Unit direct cost, UDC(\$/kWe)	1,488.
Total cost, TC(M\$)	2,027.
FPC unit cost (\$/kg) ^(e)	53.0
Fractions of total directed TDC	
♦ reactor plant equipment, RPE/TDC	0.37
♦ fusion power core cost, FPC/TDC ^(d)	0.0655

-
- (a) All designs are for baseline parameters given in Table 5.3.-I,
 $A = R_T/r_p = 6.5$, $\beta_\theta = 0.13$.
- (b) Peak current in back-biased state, decreases by factor of ~ 2 in forward-biased state, subsequently decays to zero upon initiation of OFCD.
- (c) Superconducting magnet.
- (d) Does not include structure.

Fig. 5.3.-4. Sensitivity of COE to perturbations in key physics and engineering characteristics of the $I_w = 19 \text{ MW/m}^2$ design of Table 5.3.-II.

acceptable plant factor because of the constant time assumed for each FPC replacement.

The RFP reactor has been examined over a range of neutron wall loadings and varying utilization of resistive versus superconducting magnets. Recent emphasis has been placed on compact, resistive-coil approaches because of the promise of substantial economic, operational, and development advantages for these physically smaller systems. These improved fusion reactors have an FPC power density in the range 5-15 MWt/m³ and a mass power density in the range 500-1000 kWt/tonne, which represent improvements by factors of 10-30 compared with earlier fusion reactor designs. Because the cost of FPC is a smaller portion of the total plant cost (typically 7% compared with 25-30% for earlier designs), the unit direct cost, UDC(\$/kWe), is less sensitive to related physics and technology uncertainties; installation and maintenance requirements are also eased. A faster, less costly development path also becomes a possibility. Both physics and technological problems remain to be solved for these higher power-density systems, however. The Strawman designs and the relative sensitivities presented in the following subsection serve as a basis for quantitative assessment of the above-described issues.

5.3.2. "Strawman" Design-Point Selection

Although the sensitivity/trade-off studies reported in the previous subsection were performed at a conservative baseline of $\beta_\theta \approx 0.13$, the TITAN Physics Advisory Committee has since recommended a value closer to 0.20, consistent with the best presently-available experimental values. Figure 5.3.-5 presents the COE as a function of r_p for $\beta_h = 0.20$, $A \equiv R_T/r_p = 6.5$, and for various values of power output P_g in the range 400-1,200 MWe(net). Contours of constant 14.1-MeV-neutron first-wall loading are also shown, together with contours of constant ν , the τ_E (PHYS) scaling exponent, corresponding to $\tau_E(\text{OPT}) = \tau_E(\text{PHYS})$.

Consistent with Fig. 5.3.-5, new Strawman design points have been generated, incorporating the following features:

1. $\beta_\theta = 0.20$ at $\theta = 1.53$ and $F = -0.10$.
2. OFCD Q-values, $Q_p = 535$ and $Q_c = 9969$ (see Sec. 4.6).
3. Operation at a higher average plasma temperature $T = 20$ keV to reduce reactive-power dissipation in the shell and other structures during OFCD. Costs (≈ 35 mills/kWeh at $I_w \approx 18$ MW/m²) are insensitive to T in the range 10-20 keV.

Fig. 5.3.-5. The Cost of electricity, COE, for a compact RFP reactor with PbLi/HT-9 blanket as a function of plasma radius, r_p , for a range of P_E values. The COE of a 1000 MWe RFP reactor with Li/V blanket is also shown. Contours of constant I_w as well as the condition where $\tau_E(\text{OPT}) = \tau_E(\text{PHYS})$ are shown assuming $\tau_E(\text{PHYS}) \propto I_w r_p^2 f(\beta_\theta)$ scaling and a range of ν values.

4. Superconducting EFCs (SC/EFC). The COE for a normal-conducting-EFC (NC/EFC) case at a neutron wall loading $I_w \approx 18 \text{ MW/m}^2$ is ~ 37 mills/kWeh, equal to about that for the $\beta_0 = 0.13$ SC/EFC min-COE Strawman.
5. Stray vertical magnetic fields are below target levels (2.5 mT).

The minimum-COE design point at 1,000 MWe(net) has a plasma radius $r_p = 0.605 \text{ m}$ and $I_w \approx 18 \text{ MW/m}^2$. For the convenience of the mechanical engineers, plasma and wall radii $r_p = 0.60 \text{ m}$ and $r_w = 0.65 \text{ m}$, respectively, are specified, at which $I_w = 18.1 \text{ MW/m}^2$. Previously, the plasma radii for the $I_w = 10$ and 5 MW/m^2 Strawman were $r_p = 0.85$ and 1.20 m , respectively. These Strawman design points are illustrated in Fig. 5.3.-6. Retaining these dimensions for convenience, the corresponding neutron wall loadings now become 9.3 and 4.8 MW/m^2 . The dimensions of these Strawman designs are summarized in Table 5.3.-III, including parameters for a new $I_w = 15.6 \text{ MW/m}^2$ case. The schematic geometry was illustrated in Fig. 5.3.-1, which defines the geometric parameters. A more detailed parameter summary of the Strawman, updating Table 5.3.-IV of Ref. 31, is presented in Table 5.3.-IV.

The $I_w = 19 \text{ MW/m}^2$ (min-COE) Strawman design was subjected to PF coil set discretization using the CCOIL magnetics code. This design is illustrated in Fig. 5.3.-7. The PFC position and currents are summarized in Table 5.3.-V. Table 5.3.-VI compares coil performance parameters as obtained from the RFP9 systems code (LA-CC-86-4) and the CCOIL code (Sec. 4.4.2). Circuit parameter from the two codes are compared in Table 5.3.-VII. These parameters are used as input to the CRFPR time-dependent start-up code (LA-CC-86-6), which is being used to optimize the start-up transient (Sec. 4.5.2). Modeling of the early-time RFP formation phase is also under investigation (Sec. 4.5.1).

5.3.3. Alternative Configuration Options

Modification to the RFP9 parametric systems analysis (PSA) code to model alternative configuration options, including the integrated blanket coil (IBC) and water (pool) configuration are under development. Characterization of these options is less well-developed than are the models of the more conventional liquid-metal-loop configuration.

Fig. 5.3.-6. Elevation views of three TITAN Strawman design points compared with a fission PWR [36], the STARFIRE tokamak design [17], and the GENEROMAK tokamak design [16]. The TITAN coil set are not shown in discretized form.

TABLE 5.3.-III

TITAN STRAWMAN^(a) DIMENSIONS (m)
 (Refer to Fig. 5.3.-1 for dimension definition)

I_w (MW/m ²)	18.1 ^(b)	15.6	9.3	4.8
R_T	3.90	4.225	5.525	7.80
r_p	0.60	0.65	0.85	1.20
r_w	0.65	0.70	0.90	1.25
r_{bo} (c)	1.425	1.475	1.675	2.025
r_{TFo} (d)	1.453	1.501	1.695	2.041
r_{PFo} (e)	1.705	1.735	1.883	2.199
R_{EF}	6.244	6.605	8.069	10.67
z_{EF}	2.726	2.771	2.971	3.358
δ_{EF} (f)	0.697	0.680	0.634	0.594

(a) SC/EFC, $\beta_\theta = 0.20$, $A = R_T/r_p = 6.5$, $P_E = 1000$ MWe(net).

(b) Minimum-COE.

(c) $\Delta b = 0.775$ including first wall, blanket, and shield. $r_{bo} = r_w + \Delta b$.

(d) $r_{TFo} = r_{TF} + \delta_{TF}/2$.

(e) $r_{PFo} = r_{PF} + \delta_{PF}/2$.

(f) $\Delta s_{EF} = 0.5$ m.

TABLE 5.3.-IV
SUMMARY OF 1000-MWe(net) TITAN STRAWMAN
FOR THREE NEUTRON WALL LOADINGS(a)

Neutron Wall Loading, $I_w(\text{MW/m}^2)$	18.1	9.3	4.8
<u>Plasma Parameters</u>			
Minor plasma radius, $r_p(\text{m})$	0.60	0.85	1.20
Major plasma radius, $R_T(\text{m})$	3.90	5.525	7.80
Plasma volume, $V_p(\text{m}^3)$	27.7	78.8	221.7
Plasma current, $I_\phi(\text{MA})$	17.75	19.40	21.19
Toroidal current density, $j_\phi(\text{MA/m}^2)$	15.7	8.5	4.7
Plasma density, $n(10^{20}/\text{m}^3)$	4.35	2.59	1.55
Energy confinement time, $\tau_E(\text{s})$	0.25	0.42	0.71
Thermal diffusivity, $\chi_E(\text{m}^2/\text{s})$	0.27	0.32	0.38
Fusion power density, $P_F/V_p(\text{MW/m}^3)$	81.6	28.9	10.4
Plasma ohmic dissipation, $P_Q(\text{MW})$	8.0	6.8	5.8
<u>Poloidal-Field Quantities</u>			
Coil thickness, $\delta_{c\theta}(\text{m})$	0.252	0.188	0.158
Average minor radius, $r_{c\theta}(\text{m})$	1.579	1.789	2.120
Poloidal field at plasma surface, $B_\theta(\text{T})$	5.92	4.56	3.35
Coil field, $B_{c\theta}(\text{T})$	2.25	2.17	2.00
OHC current density, $j_{c\theta}(\text{MA/m}^2)(\text{b})$	17.4	16.9	15.5
Mass of coil, $M_{\text{OHC}}(\text{tonnes})$	322.3	386.0	543.1
EFC current density, $j_{c\theta}(\text{MA/m}^2)(\text{c})$	19.8	21.2	22.2
Mass of coil, $M_{\text{EFC}}(\text{tonnes})$	278.5	297.1	344.9
Poloidal-field stored energy, $W_{B\theta}(\text{GJ})$	4.6	5.2	6.4
OHC dissipation during back-bias (MW)	285.0	342.0	422.0
<u>Toroidal-Field Quantities</u>			
Coil thickness, $\delta_{c\phi}(\text{m})$	0.028	0.020	0.016
Average minor radius of coil, $r_{c\phi}(\text{m})$	1.439	1.685	2.033
Mass of coil, $M_{\text{TFC}}(\text{tonnes})$	28.5	37.7	54.1
Reversed-toroidal field during burn, $-B_{\phi R}(\text{T})$	0.36	0.28	0.22
Magnetic energy stored in coil, $W_{B\phi}(\text{GJ})$	0.78	0.96	1.22
TFC current density, $j_{c\phi}(\text{MA/m}^2)$	17.4	16.9	15.5
Ohmic dissipation during burn, $P_Q^{\text{TFC}}(\text{MW})$	34.0	42.1	51.0
Mass of divertor coil, $M_{\text{DFC}}(\text{tonnes})$	3.8	3.9	4.1
Ohmic dissipation in divertor, $P_Q^{\text{DFC}}(\text{MW})$	23.7	24.4	25.6

TABLE 5.3.-IV (cont)

<u>Engineering Summary</u>			
Engineering Q-value, $Q_E = 1/\epsilon$	7.84	7.47	7.09
Fusion power, P_F (MW)	2,261.0	2,279.0	2,298.0
Total thermal power, P_{TH} (MW)	2,866.0	2,886.0	2,910.0
First-wall minor radius, r_w (m)	0.65	0.90	1.25
FPC minor radius, r_s (m)	1.71	1.88	2.20
Masses (tonnes)			
♦ first wall/blanket	39.7	66.4	116.0
♦ shield	875.2	1,119.0	1,513.8
♦ total coil set	633.1	724.8	946.2
♦ FPC (FW/B/S/C)(d)	1,553.2	1,919.8	2,595.8
♦ FPC structure	663.5	912.9	1,317.5
System power density, P_{TH}/V_{FPC} (MWt/m ³)	12.8	7.5	3.9
Mass power density, $1000P_E/M_{FPC} = MPD$ (kWe/tonne)(d)	644.0	521.0	385.0
<u>Cost Summary</u>			
Cost of electricity, COE(mills/kWeh)	35.2(e)	36.5	39.0
Unit direct cost, UDC(\$/kWe)	1,409.0	1,463.0	1,569.0
Total cost, TC(M\$)	1,920.0	1,993.0	2,139.0
FPC unit cost (\$/kg)(e)	52.0	53.0	55.0
Fractions of total direct cost (TDC)			
♦ reactor plant equipment, RPE/TDC	0.35	0.37	0.41
♦ fusion power core cost, FPC/TDC(d)	0.057	0.069	0.091

- (a) All designs for baseline parameters: $T = 20$ keV, $A = 6.5$, $\beta_0 = 0.20$, $\eta_{TH} = 0.40$.
- (b) Peak current in back-biased state, decreases by factor of ~ 2 in forward-bias state; subsequently decays to zero upon initiation of OFCD.
- (c) Superconducting magnet.
- (d) Does not include structure.
- (e) Minimum COE.

TABLE 5.3.-V
PF COIL LOCATIONS AND PARAMETERS

	R(m)	$\pm z$ (m)	ΔR (m)	Δz (m)	A(m ²)	j(MA/m ²)	I(MA)
EFC	6.4959	2.4873	0.6973	0.6973	0.4862	18.2675	8.8821
OHC-1	5.8699	1.9473	0.4000	0.4000	0.1600	12.8372	-2.0540
2	3.9472	2.2299	0.4100	0.4100	0.1681	12.2187	-2.0540
3	3.1958	1.8533	0.3000	0.5000	0.1500	13.6930	-2.0540
4	2.7905	1.4625	0.2000	0.5000	0.1000	20.5396	-2.0540
5	2.5503	1.1031	0.2500	0.3300	0.0825	24.8965	-2.0540
6	2.4028	0.7705	0.3300	0.3000	0.0990	20.7470	-2.0540
7	2.3163	0.4557	0.3200	0.3000	0.0960	21.3954	-2.0540
8	2.2759	0.1508	0.3300	0.3000	0.0990	20.7470	-2.0540

TABLE 5.3.-VI
COMPARISON OF COIL PARAMETERS

<u>Parameter</u>	<u>Systems Code</u>	<u>CCOIL</u>
EFC current (MA) ^(a)	19.2	17.8
EFC volume (m ³)	38.1	39.7
EFC mass (tonne)	278.3	292.1
EFC Joule losses (MW) ^(a)	0.0	(378.0 NC)(0.0 SC)
EFC peak field (T) ^(a)	10.2	5.9
EFC current density (MA/m ²) ^(a)	19.8	18.3
OHC current (MA)		
* back bias	-31.4	-32.9
* forward bias	17.6	15.1
OHC volume	43.7	40.9
OHC mass (kg)	318.9	301.2
OHC Joule losses (MW)	0.0/287.1	(68. ^(c))/321. ^(b)
OHC Von Mises stress (MPa) ^(b)	186.	215.6
OHC peak field (T) ^(b)	--	8.3
OHC current density (MA/m ²) ^(b)	7.9/17.6	(12.2-24.9)
OHC stray vertical field (mT) ^(b)	--	1.25(< 2.45)
PFC transparency (%)	--	67.2
TFC transparency (%)	--	TBD

^(a) Steady-state values.

^(b) Back-bias values.

^(c) Forward-bias values.

TABLE 5.3.-VII
COMPARISON OF PF CIRCUIT PARAMETERS

<u>Parameter</u>	<u>Systems Code</u>	<u>CCOIL</u>
Self-Inductances (μH)		
* L_p	13.24	13.24
* L_{EF}	25.22	14.8
* L_{OH}	3.61	3.4
Mutual Inductances (μH)		
* $M_{OH,p}$	3.61	3.47
* $M_{OH,EF}$	--	3.08
* $M_{EF,p}$	3.70	3.87
Current Levels (MA)		
* I_ϕ	17.75	17.75
* I_{EF}	19.22	17.8
* ΔI_{OH}	45.42	48.0
Magnetic Fluxes (Wb)		
* Plasma	235.0	235.0
* EFC	71.1	68.8
* OHC	163.9	166.0

Fig. 5.3.-7. Elevation view of the $I_w \approx 18 \text{ MW/m}^2$ (min-COE) TITAN Strawman design point with the PF coil set determined by the CCOIL code. The EFCs are superconducting.

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6. DIVERTOR

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6. DIVERTOR

6.1. INTRODUCTION

An efficient mechanism for particle exhaust and impurity control will be an essential component of any magnetic fusion reactor, in order that the products of the fusion reaction can be removed and to avoid excessive contamination of the plasma core. In tokamak reactor studies much attention has been paid to this aspect of the design, both pumped limiters and magnetic divertors having been considered [1-3] but little work has been performed on impurity control systems for reversed field pinch (RFP) reactors. For the compact reversed field pinch reactor (CRFPR) [4,5], which represents the only major contribution to the field, a pumped limiter was used for the reference design although options for a divertor were also examined [5-7].

The limiter suffers from several drawbacks, particularly for a high power density device such as TITAN. Erosion is a potentially serious problem and because of the proximity to the plasma core this can represent a large source of impurities. Furthermore, to maintain the heat flux at an acceptable level in CRFPR [4], a core plasma radiation fraction, f_{RAD} , of 0.9 was used and 24 poloidal ring limiters covered 40 % of the first wall area. In a divertor design the region of high heat flux and plasma-surface interaction is removed to a separate chamber in a somewhat lower neutron flux and the probability of contamination of the core plasma by sputtered impurities is reduced. However, while divertors have been extensively employed with considerable success in present tokamak experiments, no RFPs have operated with a divertor, although divertor experiments are proposed in the ZT-H device at Los Alamos [8]. Nevertheless, a divertor has been selected as the primary approach for impurity control for TITAN.

In choosing the type of divertor to be used, a strong preference exists for selecting a configuration in which the minority magnetic field is nulled [6,7]. This choice minimizes the perturbations to the core plasma and reduces the engineering requirements in terms of coil currents, stresses, and power and energy requirements. For a RFP the toroidal field is weaker than the poloidal field and bundle divertors or toroidal-field divertors are the main options, whereas a poloidal-field divertor is more appropriate for a tokamak.

In the CRFPR study [5] the field line connection length was found to be too long for the bundle divertor, resulting in excessive cross-field diffusion to the first wall. On the other hand, the poloidally-symmetric toroidal-field divertor was considered a feasible design approach worthy of more detailed investigation. As the reactor parameters for TITAN are similar to those of CRFPR this recommendation has been followed and the symmetric toroidal divertor has been selected as the focus of the effort on impurity control for TITAN.

For the scoping phase of the project the work has concentrated on issues influencing the feasibility of the toroidal divertor concept. The magnet configuration is discussed in the next section followed by work on the modeling of the edge-plasma, including estimates of the conditions at the divertor target (see Sec. 6.3). The relationship between the first wall and divertor heat loads is described in Sec. 6.4 and the various heat removal concepts that have been considered are discussed in Sec. 6.5. Finally, conclusions from the work performed so far are presented, followed by plans for further work during the next phase of the project.

6.2. DIVERTOR MAGNETICS

6.2.1. Basic Configuration

For this phase of the project the coil layout has been determined using a two-dimensional analysis of the magnetics, including only the toroidal field (TF) coils and the divertor coils. A full three-dimensional simulation, including the effects of the plasma and the poloidal field (PF) coils, will be carried out in the future and the importance of this analysis is described in Sec. 6.2.3.

A plan view of a typical coil layout is shown in Figure 6.2.-1 and a magnified view of the outboard side is given in Fig. 6.2.-2. The figures show the divertor coils and the TF coils and field lines near the inboard and outboard edges of the plasma; in reality, the rotational transform provided by the plasma current and the PF coils would link these two sets of field lines. The divertor coils comprise the central nulling coil, whose current opposes that of the TF coils, and two flanking coils, which have currents in the same direction as the TF coils. The sum of the divertor coil currents is always zero to minimize the effect of the divertor on the global magnetic configuration.

Fig. 6.2.-1. A plan view of a typical coil layout for a symmetric toroidal - field divertor showing the TF coils, divertor coils and diverted field lines on the inboard and outboard sides (generated with a 2-D magnetics analysis).

Fig. 6.2.-2. A magnified view of the outboard side of Fig. 6.2.-1, also showing a notional access corridor (labeled "services").

In Fig. 6.2.-2 an access corridor is shown (labeled "services") behind the nulling coil to illustrate the region through which coolant and support structure must pass. This access is only provided at one poloidal location or over a limited range of poloidal angle. A poloidally continuous and cooled divertor target is located on each side of the service corridor to neutralize the incident plasma.

Several sets of divertor coils may be located around the torus. To minimize the heat flux at the divertor plate and to reduce the field line connection length a large number of divertors is desirable. The penalties for including more divertors are a larger Ohmic power loss in the divertor coils and a removal of potential breeding blanket volume which has a detrimental effect on the global tritium breeding ratio. Following references [5,7], the number of divertors has been set at 4 for the preliminary analysis for TITAN.

The divertor coils are located so as to produce the required degree of diversion of the field lines, i.e. to locate the separatrix at the plasma surface. This requirement forces the coil centers to be offset from the plasma centerline to ensure an equal diversion of inboard and outboard field lines at the same minor radius. In determining the coil radius, sufficient space must be allowed behind the first wall to provide a minimum shield for the coil from the high neutron flux. An examination of life-limiting processes for highly irradiated normal-conducting magnets is now in progress, and a shield thickness of 10 cm is currently specified to restrict radiation damage in the coil insulator to allowable levels. The coil current densities are chosen to ensure that no field line intersects any coil.

The field line plot in Fig. 6.2.-2 shows that the divertor configuration is not of the "open" nature which is generally obtained in tokamak reactors with poloidal divertors. This difference arises from the positioning of the divertor coils. For a tokamak reactor the poloidal divertor configuration is usually produced by remote PF coils which are external to the TF coils, resulting in a wide spreading of the open field lines outside the separatrix (i.e., similar to the expanded boundary geometry obtained in Doublet III [9]). The toroidal divertor configuration of the RFP reactor is generated with coils which are placed close to the plasma surface and the open field lines remain more tightly bound. This "closed" divertor configuration is beneficial as it allows the divertor chamber to be decoupled from the main plasma chamber, whereas in a tokamak with an open poloidal divertor the divertor chamber is merely an extension of the main plasma chamber. Baffles or constrictions in the wall of

the closed divertor geometry enable neutral gas backflow to be minimized and the neutron damage at the divertor plate should be substantially lower than at the first wall. A disadvantage of the closed geometry is that the spacing between the field lines tends to reduce in the divertor resulting in an increase in the heat flux on the divertor target as the plasma strikes a smaller area. The possibility of spreading these field lines is considered in Sec. 6.2.4.

6.2.2. Integrated-Blanket-Coil Divertor

6.2.2.1. Concept

The Integrated-Blanket-Coil (IBC) concept combines the blanket functions of tritium breeding and energy recovery with the coil function of magnetic field production in a single component. Specifically, electrical current is passed through a flowing liquid lithium blanket, which also serves a coil function. This concept is considered for use in conjunction with the liquid metal cooled blanket only. Several benefits can be anticipated from adopting the IBC approach for divertor coils:

1. Radiation damage to the conductor and insulator is not a concern. This minimizes the need for shielding and allows the coils to be placed closer to the plasma.
2. Magnetic coupling is improved when the coils are moved closer to the plasma, reducing current requirements.
3. The breeding and energy recovery medium is present in the volume normally displaced by the copper coils and shielding. This will improve the tritium breeding ratio and the thermal power output.
4. Ohmic losses incurred in the IBC are deposited at a temperature that allows recovery in the thermal cycle.

The major drawback to the IBC concept is the high electrical resistivity of liquid metals, such as lithium, which, at coolant temperatures, is about 13 times that of a wound copper coil operating at 300 °C. Some of this difference is compensated, as noted above, by recovering the Joule losses in the IBC coil,

so that some 35-40 % of the Ohmic heat reappears as electrical power. Further reduction of the power loss can be accomplished through improved coupling of the divertor-plasma magnetics and lower current density in the IBC.

Preliminary analysis indicates that a combination of vanadium alloy structure and lithium is attractive for use with the IBC. Selection of these materials is consistent with the Blanket Comparison and Selection Study [10], which rated the Li-V option as the primary choice for the self-cooled liquid metal blanket. For this study, therefore, the IBC has been taken to consist of: (1) liquid lithium as the tritium-breeding material, electrical conductor, and heat transfer medium; and (2) a vanadium alloy such as V-3Ti-1Si as the structural material.

6.2.2.2. Results

An IBC divertor configuration was determined for comparison with an earlier divertor design using conventional coils [5,7]. Table 6.2.-I summarizes the parameters of the two configurations, while Figure 6.2.-3 shows the field lines for a typical coil layout for the IBC version. The case considered corresponds to a neutron wall loading of about 20 MW/m². Lower wall loadings lead to lower field strengths on edge, and thus reduce the nulling current and corresponding Ohmic losses.

6.2.2.3. IBC Coil Design

The IBC divertor coils would use a two-pass flow pattern where the lithium would enter the coil and make a poloidal loop, then reverse direction and make another poloidal loop before exiting to the heat exchanger. This allows both the inlet and outlet to be at the same electrical potential (ground), which eliminates leakage current paths. A penalty is incurred, however, in that the coolant must flow about twice as fast to cover the longer flow path with the same rise in temperature, resulting in relatively high MHD pressure drops.

A sandwich design is used to mitigate these MHD pressure losses, with electrical insulator separating the structural material from a very thin surface conducting layer. The voltage in the divertor nulling coil is about 60 V, indicating that a ceramic insulator such as MgO or spinel would provide adequate insulation even with a degradation of resistivity of 3-4 orders of magnitude due to the ionizing radiation flux. Use of such a sandwich design would keep the maximum MHD pressure drop in the divertor system to less than 3 MPa.

Fig. 6.2.-3. A typical 2-D field line plot for the IBC divertor design.

TABLE 6.2.-I
Divertor Coil Design Parameters

	Conventional	IBC
NULLING COIL		
Current (MA)	0.8	0.3
Current Density (MA/m ²)	50	28
Coil radius (m)	1.088	1.002
FLANKING COILS		
Current (MA)	0.4	0.15
Current Density (MA/m ²)	40	24
Coil radius (m)	0.97	0.87
OHMIC POWER (MW)	48.4	118.1

6.2.3. Three-Dimensional Modeling

The two-dimensional modeling of the TITAN divertor only simulates the toroidal and radial magnetic fields and yields two-dimensional field-line tracings of the kind shown in Fig. 6.2.-4 for the CRFPR design [5,7]; these computations are used to locate the separatrix. The global alignment of the separatrix with the plasma surface is used to determine the divertor-coil currents and geometry. The two-dimensional field-line tracings can also be used to estimate the connection length between successive divertor throat openings, with an accuracy of ~ 10% based on the CRFPR results [5,7], to yield a qualitative estimate of the effect of the divertor on the toroidal-field ripple, and to locate the flux plume within the divertor chamber.

The three-dimensional modeling simulates the toroidal, radial, and poloidal components of the magnetic field and yields three-dimensional field-line tracings which are represented as puncture plots in Figs. 6.2.-4 and 6.2.-5 for the CRFPR design [5,7]. Adding the poloidal dimension to the two-dimensional (radial and toroidal) simulation permits the expression of the purely three-dimensional phenomena of magnetic islands and flux surface broadening. The magnetic islands are the result of the beating of the resonant helical pitch of a field line with the toroidal field ripple, whereas flux surface broadening occurs because of poloidal asymmetries in the toroidal field ripple. If the

Fig. 6.2.-4. The equatorial-plane view of the two-dimensional field-line tracings (solid lines above and to the left of divertor) for the four symmetric divertors of the CRFPR design [5] at minor radii of $r = 0.69, 0.705, 0.715, 0.73$, and 0.75 m. The plasma minor radius is 0.71 m. Also shown (below and to the right of divertor) are the puncture plots (the field-line intersections with the equatorial plane) for the three-dimensional simulation of plasma and all coils at minor radii of $r = 0.68$ and 0.73 m. The toroidal-field (TF) and divertor coils are also shown. This figure is taken from reference [7].

Fig. 6.2.-5. The cross-sectional view of the puncture plots (the field-line intersections with the divertor-coil plane) for the three-dimensional simulation of plasma and all coils. The Ohmic-heating (OH), equilibrium-field (EF) and toroidal-field (TF) coils are also shown, based on the coil set for CRFPR.

flux surfaces between the plasma surface and the reversal surface become sufficiently broadened to overlap the latter two surfaces, then both energy and particle confinement will be lost in this region, which is considered to be responsible for most of the confinement in the RFP. Consequently, tracing at least one field line between the reversal and separatrix surfaces is necessary to ensure that the divertor has not introduced a toroidal-field ripple that is too large.

The inboard-to-outboard asymmetry displayed in the two-dimensional field-line tracings of the divertor plume, which is the result of the toroidal field being inversely proportional to the major radius, produces effects that can only be seen with three-dimensional field-line tracings outside the separatrix. The stronger inboard toroidal field, relative to the outboard toroidal field, results in field lines bunching together poloidally, with a factor of more than four compression over a uniform distribution for the case shown in Fig. 6.2.-5. The precise location of the poloidal peak in field-line density is a function of edge-plasma q -value, plasma major radius, and average radial extent of the divertor plume; a three-dimensional field-line tracing within the scrape-off layer is needed to locate this maximum field-line density. If a collector plate is to be positioned within the divertor chamber to minimize this peaking, then three-dimensional field-line tracing in the scrape-off layer is the only method which yields the necessary geometrical information for positioning the collector plate. In addition, the poloidal variation in the toroidal field and the radial variation in the poloidal and toroidal fields make three-dimensional field-line tracings the only method for accurately determining the connection length between successive divertor collector plates with an accurate determination of the connection length between successive divertor-throat openings as a valuable by-product.

The divertor design for TITAN has not yet proceeded sufficiently to yield three-dimensional results. When a divertor configuration has been determined from the two-dimensional simulations, the configuration will be subjected to three-dimensional analysis and iterated upon until an acceptable design is obtained.

6.2.4. Flux Expansion in the Divertor Chamber

It was observed earlier that the "closed" nature of the divertor configuration for TITAN gives rise to a compression of the field lines in the divertor chamber. In order to reduce this effect, which tends to increase the

heat loading on the divertor target, the possibility of including extra divertor coils to modify the flux surfaces was briefly investigated.

Figure 6.2.-6 gives a view of the outboard field lines for a case with one extra nulling coil located in the same plane as the primary nulling coil but at a larger radius. The flux surfaces have clearly been altered and the spacing between the field lines has increased somewhat in the vicinity of the additional coil. However, the effect is only modest and only occurs over a relatively small region. Other cases with the addition of further coils were examined but no large scale expansion of the flux surfaces was found. The difficulty arises from the need to generate a large region of space in which the magnetic field is decreased (which tends to increase the separation between field lines) which is not possible with only a small number of coils located close to the main divertor coils. As the inclusion of the extra coils adds significantly to the complexity of the divertor design and the benefits are relatively small it was concluded that such a modification is not worthwhile.

6.3. EDGE-PLASMA MODELING

The main aims of edge-plasma modeling for a fusion reactor are to predict the plasma conditions at the first wall and divertor target and to estimate the requirements for particle removal. The heat flux distribution and erosion rate from sputtering are important considerations for the divertor design and the pumping speed required from the vacuum system governs the size of the vacuum ducts.

For the scoping phase of the TITAN project work has concentrated on estimating the peak heat flux on the divertor target and on ways to reduce the load to a manageable level. Both analytic and computational models have been used and more detailed analysis is planned for the next phase of the project.

6.3.1. Estimates of Scrape-off Layer Parameters

6.3.1.1. Analytic Model

A simple model developed by Harbour for a tokamak poloidal divertor [11] has been used to estimate the characteristic thickness for the radial decay of power flow in the scrape-off layer and the plasma temperature at the divertor target. The geometry of the model has been modified to make it applicable to an RFP reactor with several toroidal divertors. In this model the scrape-off layer

Fig. 6.2.-6. A view of the outboard field lines for a case with an extra nulling coil behind the primary nulling coil to investigate the possibility of flux expansion in the divertor.

is treated as a slab of thickness λ_{SOL} with no radial variation of parameters across the slab. The thickness evaluated in the model may be interpreted as a radial e-folding length or gradient scale length for power flow. Power flow parallel to the magnetic field is assumed to occur by electron conduction alone except for the convective energy transfer through the sheath at the target.

Input parameters for the model include the geometry of the scrape-off layer (major radius, minor radius, field line connection length etc.), the total power, P_D , crossing the separatrix, the number of divertors, N_D , and the radial thermal diffusivity, χ_1 . The plasma temperature at the divertor target, T_t , and at the midpoint between two divertor entrances, T_s , the density at the target, n_s , and the gradient scale length for power flow, λ_{SOL} , are then determined as a function of the density at the midpoint between two divertors, n_s .

Fig 6.3.-1 shows a set of results for the 20 MW/m² TITAN design, for the case when $P_D = 250$ MW ($f_{\text{RAD}} = 0.5$). For upstream plasma densities, n_s , of $\sim 1 - 2 \times 10^{20} \text{ m}^{-3}$, λ_{SOL} is ~ 1 cm and the plasma temperature at the target, T_t , exceeds 100 eV. Such a short scale length will lead to very high heat fluxes on the target, while the high temperature will result in severe sputtering of the target. The reason for the small thickness of the scrape-off layer for power flow lies in the dominance of parallel over radial heat transport. The high conductivity associated with parallel electron heat conduction ($\propto T^{5/2}$) causes heat to be lost axially at a rapid rate compared with the radial loss, even for the relatively high value of radial thermal diffusivity, χ_1 , of $4 \text{ m}^2/\text{s}$, which is assumed here.

At higher values of n_s the situation improves with λ_{SOL} increasing and T_t falling. However, an upper limit exists for n_s , as it clearly cannot exceed the average core plasma density, $\bar{n}$, indicated on the figure, and is likely to be lower by a factor $\sim 2 - 4$. If the total power, P_D , being transported to the divertor is reduced (i.e. higher f_{RAD}) then λ_{SOL} also increases and T_t decreases, but this change would also imply a higher heat flux on the first wall.

Thus, this simple analytic model suggests that the scrape-off layer thickness for power flow is unlikely to be much greater than 1 cm and that the plasma temperature at the target will be high, unless the total power flowing to the divertors can be reduced.

Fig. 6.3.-1. Results from an analytic model [11] of the TITAN scrape-off layer, showing the variation of the scrape-off layer thickness and the upstream and target plasma temperatures as a function of the upstream density.

6.3.1.2. Computational Model

Analytic models of the scrape-off layer are very valuable in identifying basic scalings and trends of the key plasma parameters but the broad assumptions which have to be made in such models render any quantitative conclusions somewhat uncertain. Numerical models, however, allow many more physical processes to be considered and are necessary for more accurate calculations at a well defined design point.

An axially averaged radial transport model of the scrape-off layer called ODESSA (One-Dimensional Extended Scrape-off Simulation and Analysis), is being used to model the TITAN edge plasma. A description of this code is given in references [12] and [13]. The two-dimensional scrape-off layer is divided axially into two regions, as shown in Fig. 6.3.-2, representing the upstream and downstream regions. The first region represents the edge-plasma adjacent to the main plasma and is fed with particles and energy from the core. In this region radial transport is dominant and parallel gradients are weak. The downstream region is dominated by recycling neutrals near the divertor target and radial transport is neglected in comparison with the rapid parallel transport due to the near sonic plasma flow in this region.

Output from the model is in the form of radial profiles of density, ion and electron temperature, and particle and energy fluxes for each region. Figs. 6.3.-3 and 6.3.-4 show density and temperature profiles respectively from a preliminary run for the TITAN scrape-off layer. The scrape-off layer width was taken as 5 cm as assumed for the systems code (Sec. 5) and the upstream separatrix density was taken as approximately $2 \times 10^{20} \text{ m}^{-3}$. The power transported to the scrape-off layer was in accordance with a core radiation fraction, f_{RAD} , of 0.5 and the recycling coefficient at the target was uniform across the radius at 0.99. There are several unsatisfactory features on these curves indicating the need for further work to adjust the plasma parameters to acceptable levels.

Firstly the upstream plasma temperature at the wall is high, $\sim 80 \text{ eV}$, which combined with the steep density gradient at the first wall, will lead to an unacceptably high sputtering rate of the wall. The peak downstream (or target) plasma temperature is also too high, at over 100 eV . Sputtering due to high energy ions accelerated through the sheath potential would be excessive. Both of these problems will be alleviated with the inclusion of impurity radiation and improved models of neutral recycling at the wall and target which will be incorporated into the code during the next phase of the project (Sec. 6.3.4).

Fig. 6.3.-2. Schematic of the edge-plasma showing the division into upstream and downstream zones as used in the ODESSA plasma transport code.

Fig. 6.3.-3. Upstream and downstream radial profile of plasma density for $f_{RAD} = 0.5$ ($P_D = 250\ MW$).

Fig. 6.3.-4. Upstream and downstream radial profiles of electron and ion temperature for $f_{RAD} = 0.5$ ($P_D = 250$ MW).

The heat flux into the sheath falls off approximately exponentially with radius with an e-folding length of close to 1 cm, in good agreement with the prediction of the analytic model.

6.3.2. Peak Heat Loading on the Divertor Target

The gradient scale length for power flow in the scrape-off layer, as determined in Sec. 6.3.1, can be used to estimate the peak heat flux on the divertor target. One point which should be borne in mind, however, is that the characteristic thickness of the scrape-off layer for power flow evaluated by plasma models applies in the region adjacent to the core plasma and, because of radial diffusion and changes in magnetic field strength, may be different at the divertor target. The flux plots in Section 6.2 show that the plasma will be slightly compressed in the divertor chamber (by a factor of ~ 2); but this tendency to increase the heat loading will be offset by radial diffusion within the divertor, especially in the vicinity of the sharp peak in the approximately exponential power profile as it enters the source-free region downstream of the null point. As an approximation in estimating the peak heat load on the target these two effects are assumed to cancel and an unchanged radial power profile is used.

For a target placed in the shadow of the nulling coil perpendicular to the flux surfaces (in the region labeled "services" on Fig. 6.2.-2), the peak heat load, q_{PK} , is given by

$$q_{PK} = \frac{P_D}{2N_D 2\pi a_D \lambda} \quad (6.3.-1)$$

where $P_D (= [1 - f_{RAD}][P_\alpha + P_Q])$ is the total power removed by the divertors, N_D is the number of divertors (the factor 2 allows for two targets per divertor), a_D is the average minor radius of the divertor target and λ is the e-folding length for the power flux at the divertor target.

Taking $P_D = 250$ MW ($f_{RAD} = 0.5$), $N_D = 4$, $a_D = 1.2$ m and $\lambda = 1$ cm this yields a peak power load of ~ 400 MW/m², ignoring any peaking factor due to the poloidal asymmetries described in Sec. 6.2.3. Such a heat flux is well beyond the limit for any steady-state heat removal system, but there are many ways to reduce the loading. The major factors which influence the peak divertor power load are indicated below:

- ♦ Radial scale length for power flow
- ♦ Core plasma radiation fraction
- ♦ Divertor plasma radiation
- ♦ Number of divertors
- ♦ Divertor connection length
- ♦ Divertor target inclination
- ♦ Flux expansion in divertor
- ♦ Diffusion in divertor
- ♦ Ergodic field lines
- ♦ Plume oscillation

The radial scale length for power flow is the key parameter in determining the divertor heat flux. Its magnitude depends on the relative rates of parallel and radial heat transport, as explained in Sec. 6.3.1, and the value of $\lambda = 1$ cm used for the estimates of heat load seems appropriate for the TITAN divertor conditions.

An increase in the core radiation fraction will reduce the power flowing to the divertors but it will increase the load on the first wall; the relationship between the first wall and the divertor heat loads is described in Sec. 6.4.

Radiation from the divertor plasma will spread the heat load over the divertor chamber walls, but the line-radiation from the DT exhaust plasma will be small at the high densities expected in the divertor. Impurities with high atomic number, Z , injected into the divertor plasma, however, will radiate strongly and might result in a large reduction of the target heat loading. This possibility is examined further in Sec. 6.3.3.

The number of divertors could be increased but this change would also remove breeding blanket space and increase the resistive losses in the divertor coils. The choice of $N_D = 4$ represents a first estimate of an overall optimum configuration.

Perhaps the most direct approach to reduce the heat load is to incline the target at an angle to the incident plasma stream. Uncertainties in the plasma position and, hence, in the location of the exhaust plume, impose a limit on how obliquely the target may be inclined and an angle of 8° was specified in reference [14]. In this case the loading is reduced by a factor of $(\sin 8^\circ)^{-1} = 7$.

The possibility of expanding the flux in the closed divertor configuration with the use of extra coils was considered in Sec. 6.2.4 and it was concluded that any benefit to be obtained was slight and localized. Radial diffusion in the divertor plasma will tend to increase the thickness of the plasma channel but this effect was included in the heat load estimated above.

The last two effects are expected to have a small effect on the divertor heat load. The ergodicity of the field lines in the divertor, discussed in reference [5], will smear the power over a slightly greater area than predicted above. Oscillation of the plasma due to the F-θ current drive (Sec. 4.6) may cause the point where the plasma intercepts the target to move back and forth along the target, but the relatively small modulation of the coil currents will not produce a large change in average heat loading.

Of the factors outlined above an increase in the core radiation fraction and inclining the divertor target are obvious candidates as means to reduce the peak heat load. An inclination of 8° coupled with an f_{RAD} of 0.75 will reduce the peak target heat flux to ~ 30 MW/m², which is still above the allowable levels described in Sec. 6.5. The possibility of impurity injection to radiate within the divertor plasma, therefore, is considered.

6.3.3. Impurity Radiation in the Divertor

A simple analytic model has been developed to make an initial assessment of the feasibility of the approach to reduce the divertor target heat load by injecting high Z impurities into the divertor plasma. The basic idea is to inject a high Z gas, such as Xenon, into the divertor plasma upstream of the target but downstream of the null-point or throat. The impurity should then be distributed over the plasma downstream of the injection point but should be impeded from flowing upstream towards the core plasma by the strong frictional force imposed by the background plasma.

The basis of the model is similar to that used in reference [15]. Power flow along the field line is assumed to be purely by electron conduction

$$q_{\parallel} = -\kappa_0 T^{5/2} \frac{dT}{dz} \quad (6.3.-2)$$

where $q_{\parallel}$ is the parallel heat flux, $\kappa_0 T^{5/2}$ is the Spitzer value for the electron

thermal conductivity, T is the electron temperature and z is the direction along the field line. Power radiated by the impurities reduces the parallel heat flux

$$\frac{dq_{||}}{dz} = - P_{\text{RAD}} = - n_e n_I L(T) \quad (6.3.-3)$$

where P_{RAD} is the power radiated per unit volume of plasma, n_e is the electron density, n_I is the impurity ion density and $L(T)$ is the radiative cooling rate for the impurity ion. Further assumptions are that the electron pressure, p ($\equiv n_e T$), and the impurity fraction, f_I ($\equiv n_I/n_e$), remain constant. Under these conditions,

$$\frac{dq_{||}}{dz} = - p^2 f_I \frac{L(T)}{T^2} \quad (6.3.-4)$$

Post [16] has given analytic fits to $L(T)$ for various impurities in coronal equilibrium at higher temperatures ($T > 80$ eV for Xenon), but data for $L(T)$ are virtually non-existent for high Z impurities at low plasma temperatures. To overcome this problem, it is assumed that the radiative cooling rate varies as follows with some power of the temperature

$$L(T) = L_0 T^\alpha \quad (6.3.-5)$$

for temperatures below the cut-off point given in Ref. [16]. As there is such a large uncertainty in the radiation data the rather poor assumption of coronal equilibrium, as the residence time of the impurity ion is too short for the equilibrium concentrations of the various ionization states to be reached, is not significant. It should also be emphasized that any conclusions are subject to this same uncertainty.

With these assumptions an analytic solution to the equations can be obtained if all of the divertor power is radiated, i.e. $q_{||} = 0$ and $T = 0$ at the divertor target. Fig. 6.3.-5 shows the fractional impurity concentration as a function of the core plasma radiation fraction, f_{RAD} , for two values of α , 0 and 2. The first case, $\alpha = 0$, corresponds to a constant $L(T)$ at low temperatures;

Fig. 6.3.-5. Variation of fractional impurity concentration with f_{RAD} for two values of the radiation parameter α ($\alpha = 0$ and 2).

for the other case $L(T)$ varies as T^2 at low temperatures. It is known that at very low temperatures (well below the ionization energy i.e. $T \leq 5$ eV) $L(T)$ must fall, but it will probably remain relatively high at higher temperatures. The case for $\alpha = 0$, therefore, is likely to be more realistic except at the lowest temperatures. The figure shows that a fractional impurity concentration of the order of a few per cent is necessary to radiate all the divertor power.

The main aim of injecting impurities is to spread the heat load over a large area. Fig. 6.3.-6 shows the normalized radiated power profile along the plasma for the two values of α . For $\alpha = 2$ the radiated power is constant; this is clear from equation (6.3.-4). For $\alpha = 0$, the radiated power rises rapidly towards the end of the field line, with a singularity at the target, which can also be explained from equation (6.3.-5); for constant $L(T)$ the radiated power becomes infinite at $T = 0$. This behaviour is clearly unphysical because, as explained earlier, $L(T)$ must fall at very low temperatures. It would be possible to solve the set of equations numerically with a more realistic expression for $L(T)$, but the approximations and assumptions in the model make such a detailed solution inappropriate. The solution obtained, therefore, is cut off somewhat arbitrarily at a point close to the target, the point where the temperature has fallen to 10 eV being chosen here. This cut-off still leaves a large peaking factor in the radiated power profile, implying that the objective of spreading the heat load as uniformly as possible over the whole divertor wall area has not been achieved to a satisfactory extent.

To convert these profiles of plasma radiated power to heat flux distributions on the divertor surfaces, an integration package has been written. The plasma is treated as a line radiation source located between two surfaces, with uniformity in the third dimension assumed, as illustrated in Fig. 6.3.-7. The heat load at any point on the plate is obtained by integrating the contribution from each element of the plasma.

Figures 6.3.-8 and 6.3.-9 present the results for the two cases ($\alpha = 0$ and 2) in the form of a peaking factor for the heat flux, a value of 1 representing a perfectly uniform power deposition on the surfaces. For $\alpha = 0$, the top surface, which is further away from the plasma, sees a relatively low and smoothly distributed heat flux, but a large peaking factor is predicted for the bottom surface. The size of the peak is strongly dependent on the position of the cut-off point for the radiated power profile, but the width of the peak is fairly constant at about 1 - 2 cm for a peaking factor of 5. For $\alpha = 2$ the heat

Fig. 6.3.-6. Variation of the normalized radiated power in the plasma with axial position along the field line for two values of the radiation parameter α ($\alpha = 0$ and 2).

Fig. 6.3.-7. Geometry used in estimating distribution of heat flux on divertor plate.

Fig. 6.3.-8. Heat flux distribution along divertor plate for case when the radiation parameter $\alpha = 0$.

Fig. 6.3.-9. Heat flux distribution along divertor plate for case when the radiation parameter $\alpha = 2$.

flux varies smoothly along the surface except for a small peak where the plasma intercepts the target.

Future modeling will remove the unphysical singularities in the heat flux which arise from the simplified form for $L(T)$ and the treatment of the plasma as a line source of radiation instead of a distributed source. Another modification will be to include a fraction of the power being transported to the target by particles, as it is not realistic to assume that all the power can be radiated.

A major consideration in assessing the feasibility of impurity injection to radiate the divertor power is whether the impurities remain confined in the divertor. As the high Z impurity concentration in the divertor plasma must be $\leq 1\%$, while in the core plasma it should be $\leq 0.01\%$ (Sec. 4.7.2), a very efficient mechanism is required for retaining impurities within the divertor. The "closed" magnetic configuration of the RFP toroidal divertor is of value in this respect, as the divertor chamber can be effectively separated from the main plasma chamber by only a narrow plasma channel. This decoupling is not possible in a tokamak with a poloidal divertor generated by external PF coils; in this case there is no separate divertor chamber and a greater tendency exists for neutral gas to leak from the divertor towards the main plasma.

The forces acting on an impurity ion in a background plasma have been considered by Neuhauser [17], using a test particle approach. There is a strong frictional force which tends to drag the impurity along with the background plasma but also a thermal force pointing in the direction of increasing temperature. An electrostatic force arising from the ambipolar electric field also exists and there may be other forces due to variations in the magnetic field strength.

For impurities in a high charge state the first two forces tend to dominate except near the target where electric fields may be important. Neuhauser has identified the following criterion, which when satisfied implies that highly charged impurities tend to be entrained and drift with the background plasma flow:

$$M > \frac{\lambda_I}{\lambda_T} \quad (6.3.-6)$$

where M is the Mach number of the drifting plasma, λ_I is the mean free path for

Coulomb collisions between impurity ions and the background plasma ions and λ_T is the axial scale length for the ion temperature.

The density and temperature profiles obtained with the simple impurity radiation model indicate that Mach numbers of the order of 0.1 are required to satisfy the criterion. For the high-recycling conditions expected in the divertor, however, the plasma flow will tend to stagnate upstream of the ionization zone and it is unlikely that a sufficiently high Mach number will be achieved except in the region close to the target. As the impurities will be injected far upstream of the target, near the throat of the divertor, the criterion implies that the impurities will be driven up the temperature gradient against the background plasma flow towards the core plasma. This conclusion is preliminary, however, and further work on the plasma flows and on the impurity transport is being undertaken.

An extra complication arises when two-dimensional simulations of the scrape-off layer plasma are examined. A number of these models have shown that the background plasma flow itself can reverse over a portion of the scrape-off layer such that it is flowing away from the divertor target [18-20]. If this flow reversal occurs in TITAN then it would seriously impact the viability of the proposed impurity injection technique. Plans for more detailed modeling of the edge-plasma during the next phase of the TITAN project, including a study of these effects, are discussed in the next section.

6.3.4. Future Edge-Plasma Modeling

6.3.4.1. First Wall and Divertor Erosion

First wall and divertor erosion rates for TITAN will be estimated by coupling the ODESSA plasma edge transport code to either the FENAT [21] or SPUDNUT [22] (or both) neutral particle transport codes. Although computationally fast, SPUDNUT has the disadvantage of being one-dimensional and uses an approximate ion distribution function for generating charge-exchange neutrals, whereas FENAT is a 2-D finite element code that uses a local Maxwellian distribution for the ions but is based on approximate diffusion theory, is slower than SPUDNUT and presents greater interfacing difficulties with ODESSA. Recent numerical results indicate that the plasma temperature adjacent to the first wall (5 cm from the separatrix) is in the range 30 - 50 eV, suggesting that a substantial contribution from ion sputtering may be anticipated especially under high divertor recycling conditions. The

dependence of erosion rates on the radiation fraction and the scrape-off layer width will be investigated.

6.3.4.2. Self-Consistent Plasma and Neutral Particle Transport

Under the high recycling conditions that are desirable for operation, the plasma flow in the scrape-off layer may reverse locally if the recycling coefficient locally exceeds unity. This happens because recycling neutrals do not ionize at the same radial location at which they were born. The flow of particles is consequently out of the divertor at certain radial locations, although the integrated or net flow into the divertor must be positive and equal to the core efflux. If this flow reversal occurs, it will have serious implications for the feasibility of radiatively cooling the divertor plasma as the impurities will no longer be confined within the divertor.

The simulation of this effect requires accurate modeling of neutral particle transport in the divertor. In the longer term, a Monte Carlo code will be used to carry out the simulation in three dimensions using an accurate representation of the geometry of the divertor and the pumping ducts. In the near future, however, for computational expediency, a simplified model will be developed that will be fully implicit with ODESSA while retaining the essential non-local effects that may drive flow reversal.

6.3.4.3. Coupling of Core and Edge-Plasma Models

All calculations using ODESSA thus far have been performed with prescribed particle and heat fluxes at the separatrix. These boundary conditions are supposed to be a measure of core particle and energy outfluxes. While this approach is reasonable for the heat flux (because there is little energy recycling between core and edge), the significant stalling and even reversal of particle flow at or near the separatrix that occurs under high divertor recycling conditions makes it difficult to prescribe the particle flux at the separatrix with any accuracy. This approach nevertheless is universally adopted in edge-plasma modeling [18,19] and is not restricted to ODESSA. To circumvent this shortcoming, a combined core-edge transport calculation will be carried out by coupling ODESSA to the RFP core transport code described in Sec. 4.6. The particle flux will then be self-consistent with the recycling in the divertor and, hence, the core particle confinement time will be a function of the recycling coefficient. Incorporation of this effect is expected to have an important impact on the ash exhaust.

6.3.4.4. Axial Resolution of the Edge-Plasma

A one-dimensional, axial (along magnetic field lines) code, known as ZCODE, will be used to resolve issues associated with the strongly varying plasma density and temperature along a diverted field line. The model is based on a two-fluid plasma description and includes impurity radiation based on coronal equilibrium and a one-dimensional neutral atom transport model (SPUDNUT) for recycling at the divertor target. In addition, the expansion and contraction of field line bundles is included since it can concentrate the heat and particle loads by a substantial factor (Sec. 6.2.4). Physical sputtering at the divertor plate due to plasma and charge-exchange neutral particles is calculated and the code checks for frictional entrainment by impurities. The heat load on the divertor plate due to radiation and plasma bombardment is also evaluated. These effects have been identified as crucial issues for the TITAN divertor design.

6.4. DIVERTOR AND FIRST WALL HEAT LOADS

The divertor configuration [5] shown in Fig. 6.4.-1 is used to formulate an expression for the peak heat load in the divertor chamber in order to identify high-leverage variables which could affect a reduction in the peak heat load below the design constraint of $q_D \approx 5 \text{ MW/m}^2$. The peak heat load is simply the ratio of the power entering the divertor and the available surface area multiplied by a peaking factor. Specifically,

$$q_D = \frac{(1 - f_{\text{RAD}}) P_{\text{TR}} f_{\text{PK}}}{N_D A_D}, \quad (6.4.-1)$$

where $P_{\text{TR}} = P_\alpha + P_Q$ represents the total steady state plasma heating power (i.e., alpha-particle, P_α , and Ohmic, P_Q , powers), f_{RAD} is the fraction of the transport power radiated (uniformly) to the first wall, f_{PK} is a peaking factor applied to the divertor chamber/plate region, N_D is the number of divertors, and A_D is the area available within a divertor for the absorption of the radiated and convected power. The divertor surface area is theoretically limited to a maximum of four times the annular cross-sectional area bounded by the first-wall and outer blanket/shield radii to prevent interference with either the plasma or the poloidal-field coils, under the assumption that one side of each of four annular discs enclosing the divertor flux bundle, as shown in Fig. 6.4.-2, form

Fig. 6.4.-1. An equatorial-plane view of a quadrant of the CRFPR divertor design [5].

Fig. 6.4.-2. An enlargement of the inboard (A) and outboard (B) location of the divertor in the equatorial plane. Also shown are the useable surfaces for absorbing the power entering the divertors and then being radiated.

this idealized divertor chamber. In practice, the divertor surface area is less than this limit by a factor $f < 0.5$ because of inboard versus outboard asymmetries. The entrainment of radiating high Z impurities within the divertor plasma has been assumed here, but if impurities are not entrained then the divertor surface area is reduced to just the target area, resulting in $f < 0.1$. Hence,

$$A_D = 8\pi(r_w + \Delta b/2) f \Delta b, \quad (6.4.-2)$$

where r_w is the first-wall minor radius, and Δb is the annular thickness of the first wall/blanket/shield.

In order to maintain the toroidal-field ripple below the 0.3% design value [4], the number of toroidal-field coils (TFCs), N_{TF} , should scale approximately linearly with aspect ratio, $A \equiv R_T/r_p = R_T/r_w x$, where $x \equiv r_p/r_w$ and r_p is the plasma minor radius. Furthermore, in order to preserve the fraction, f_B , of the blanket displaced by divertors as N_{TF} varies, the number of divertors, N_D , scales linearly with N_{TF} . Normalizing to an earlier divertor design [5] ($A = 5.35$, $N_{TF} = 24$, $N_D = 4$), the following scaling relationship holds:

$$N_D = 4.48 A R_N, \quad (6.4.-3)$$

where $R_N = N_D/N_{TF}$. A divertor for the earlier design [5] occupies sixty percent of the volume between TFCs; hence, the blanket-loss fraction is $f_B = 0.6R_N$. Furthermore, the first-wall radius can be derived from the neutron wall loading, $I_w(\text{MW/m}^2)$, as follows:

$$r_w = [P_{TR}/\pi^2 I_w A x (1 + 5/Q_p)]^{1/2}, \quad (6.4.-4)$$

where $Q_p \equiv P_F/(P_Q + P_{AUX})$ and P_F is the total fusion power. The following design equation results upon substituting Eqs. (6.4.-2) - (6.4.-4) into Eq. (6.4.-1),

$$q_D = \frac{0.0089(1 - f_{RAD})P_{TR}f_{PK}}{R_N A f \Delta b ([P_{TR}/I_w A x (1 + 5/Q_p)]^{1/2} / \pi + \Delta b/2)} \quad (6.4.-5)$$

where f is the fraction of the total divertor surface area available for heat recovery. Similarly the heat load on the first wall is given by

$$q_{FW} = f_{RAD} I_w (1 + 5/Q_p)/4 \quad (6.4.-6)$$

Values for the variables appearing in Eqs. (6.4.-5) and (6.4.-6) that are consistent with the design points described in Sec. 5.3.2. are listed in Table 6.4.-I. The value for f is based on the sketches of divertor surfaces shown in Fig. 6.4.-2. The useable surfaces do not extend radially inward to the first wall because two mechanisms assumed to confine the radiating impurities, mirror confinement and frictional entrainment, are active only near the divertor plate. The confining weak field mirrors (mirror ratio, $M \leq 1.5$) are located approximately where the field lines entering the divertor chamber from the plasma "bunch" together. Frictional entrainment of impurities by plasma flow is most effective in flows with high Mach number, which occurs near the divertor plate. The latter confinement mechanism provides the stronger confinement.

TABLE 6.4.-I
Typical TITAN Divertor Parameters

Parameter	Value
Transport power, P_{TR} (MW)	500.
FW/B/S thickness, Δb (m)	0.775
Useable fraction of divertor surface area, f	0.4
Heat load peaking factor, f_{PK}	2
Ratio of plasma to first-wall radii, $x = r_p/r_w$	0.92
Ratio of fusion to heating powers, Q_p	281
Neutron wall loading, I_w (MW/m ²)	10, 18
Plasma aspect ratio, A	6.5, 8.5
Ratio of divertors to TFCs, R_N	1/6, 1/4
Radiation fraction, f_{RAD}	0-1

Since a low value of f is assumed, only modest peaking of the heat load is assumed for a given value of q_D (MW/m^2). Future efforts will concentrate on the accurate determination of f_{PK} due to such effects as poloidal asymmetries and flux surface expansion or contraction. A value of $x = 0.92$ is consistent with an 0.05 m thick scrapeoff used for the $18\text{-MW}/\text{m}^2$ Strawman design (see Sec. 5.3.2), and the Q_p for the $18\text{-MW}/\text{m}^2$ Strawman design (Sec. 5.3.2) has been assumed. Two representative sets of I_w , A , and R_N values were considered and represent a reasonable range for those TITAN design parameters to lower q_D . It should be noted that $R_N = 1/6$ was used for the CRFPR [5], with $R_N = 1/6$ and $1/4$ corresponding to $f_B = 0.10$ and 0.15 , respectively. The radiation fraction was parametrically varied over the range $0 \leq f_{RAD} \leq 1$.

The results of the parametric evaluation of Eqs. (6.4.-5) and (6.4.-6) are shown in Fig. 6.4.-3. The variable exerting the highest leverage upon q_D , is f_{RAD} , followed in decreasing order of importance by R_N , A , and I_w . The relative insensitivity of heat flux to neutron wall loading is a result of the assumption of fixed blanket/shield thickness, which is the main factor in determining the divertor surface area. Only the variable f_{RAD} can be adjusted independently to yield $q_D \leq 5 \text{ MW}/\text{m}^2$. For the $18\text{-MW}/\text{m}^2$ Strawman design with a nominal $R_N = 1/6$, an $f_{RAD} \geq 0.8$ is required to satisfy the $5\text{-MW}/\text{m}^2$ heat load constraint. Lowering the neutron wall loading to $10 \text{ MW}/\text{m}^2$ or raising the aspect ratio to 8.5 and simultaneously satisfying the $5\text{-MW}/\text{m}^2$ heat load constraint would permit a reduction in f_{RAD} to 0.76 . Increasing R_N to $1/4$ would yield a larger reduction in f_{RAD} to 0.7 and satisfy the $5\text{-MW}/\text{m}^2$ heat load constraint. If $f_{RAD} < 0.6$ is more reasonable from a physics viewpoint, then R_N , A , and I_w must be adjusted simultaneously to $1/4$, 8.5 , and $10 \text{ MW}/\text{m}^2$, respectively, in order to hold q_D below $\sim 5 \text{ MW}/\text{m}^2$. These values of R_N , A , and I_w would adversely affect tritium breeding, plasma confinement, and the efficacy of single-piece FPC maintenance. From an engineering viewpoint, it is desirable to have the first wall and divertor at the same heat load and, therefore, similar thermal stress, which would necessitate $f_{RAD} \sim 0.75\text{--}0.9$. Finally, it is recommended that future design efforts focus on a design with $I_w = 18 \text{ MW}/\text{m}^2$, $A = 6.5$, $R_N = 1/6$, and $f_{RAD} \geq 0.8$.

$$I_w = 10 \text{ MW/m}^2$$

A

$$I_w = 18 \text{ MW/m}^2$$

B

Fig. 6.4.-3. The heat load in the divertor, q_D , and on the first wall, q_{FW} , as a function of radiation fraction for various plasma-aspect ratios and the ratio of divertors to TFCs for the 10-MW/m² Strawman design (A) and the 18-MW/m² Strawman design (B).

6.5. DIVERTOR COOLING

Several options have been considered for divertor cooling for TITAN. For the Li/V blanket concept, liquid metal cooling is attractive for reasons of compatibility with the overall design; this option is considered in Sec. 6.5.1. Water is an obvious candidate as a coolant for high-heat-flux components and is particularly suitable for use in conjunction with the water-cooled blanket (Sec. 6.5.2). Safety considerations, however, preclude the use of a water-cooled divertor in a design with a blanket containing liquid lithium. Helium cooling of the divertor is compatible with any of the blanket concepts considered and a study of this approach is described in Sec. 6.5.3. Finally, various innovative cooling concepts are considered in Sec. 6.5.4.

6.5.1. Liquid Metal Cooling

6.5.1.1. Introduction

A preliminary study of liquid metal cooling of the divertor has been made using either lithium or sodium cooling. Of all the liquid metals, Li and Na have the most favorable thermal properties such as large specific heat, which is very important in high-heat-flux applications. In using electrically conducting liquid metals in a fusion reactor environment, coolant pressure and pumping power due to the magneto-hydrodynamic effect are important. Because of the limitation on the maximum temperature of the structural material, it is advantageous to design for turbulent flow in the coolant channels to reduce the film temperature drop. As the heat flux on the divertor surfaces is predicted to be high, it may be necessary for the coolant channel walls to be electrically insulated to avoid incurring a prohibitively high coolant pressure drop. Such electrical insulation is not necessary for the first wall coolant tubes where the heat flux is somewhat lower and the magnetic field strength in the direction normal to the coolant flow is lower.

In this preliminary study, three cases have been examined for both Li and Na. These are: (a) laminar flow without insulation, (b) laminar flow with the channel walls electrically insulated, and (c) turbulent flow with the channel walls insulated. In the next section, the possible coolant channel configurations are discussed and the results of MHD and thermal hydraulic calculations are presented. Some design conclusions are drawn in Sec. 6.5.1.3.

6.5.1.2. Results

Figure 6.5.-1 shows possible arrangements of the coolant channels and the directions and the approximate magnitudes of the magnetic fields. In case (a), the channels run along the poloidal direction, parallel to the dominant magnetic field. In cases (b) and (c), the coolant channels run perpendicular to the dominant magnetic field. In case (b), the desired coolant exit temperature can be obtained by adjusting the length between inlet and outlet. Only one pass across the magnetic field occurs in case (c). The exit temperature will be low in this case unless the coolant velocity is very small. Because of the much higher coolant velocity for turbulent flow and the resulting large MHD pressure drop in cases (b) and (c), only case (a) has been selected for this study.

The following results are for the 18 MW/m² neutron wall loading design. It is assumed that there are four divertors each with 10.4 m² cooling surface area. Vanadium alloy coolant tubes with inside diameter of 8 mm and wall thickness of 1 mm have been used. Inlet temperatures for Li and Na are 300 °C and 200 °C, respectively. The allowable maximum coolant exit temperature is determined such that the outer surface tube wall temperature does not exceed 750 °C. The total pressure drop includes both MHD and friction pressure losses when the tube wall is not electrically insulated, whereas only the friction pressure loss occurs when the tube wall is electrically insulated. The pressure drop does not include any loss due to bends, inlet/outlet effect, variations in the magnetic field along the inlet/outlet ducts, etc. as the detailed coolant-tube configurations are not decided at this stage. The equations for MHD and friction pressure drops are given in Table 8.4.-I (Section 8). Thermal analyses in the tube wall and in the coolant for calculating the wall and film temperature drops are similar to those presented in Section 8.

Figures 6.5.-2 and 6.5.-3 show the results for laminar flow without and with electrically insulated tube walls respectively. Both of these figures show that, for laminar flow, the limiting factor is the wall temperature, even though insulating the wall almost eliminates the MHD pressure drop. The maximum cooling capability with laminar flow for both Li and Na appears to be about 3 MW/m² without insulation and about 3.5 MW/m² with insulation. The limiting factor is the high coolant pressure and the resulting high primary stress. The assumed primary design stress limit of 100 MPa is reached at a coolant pressure of about 25 MPa.

Fig. 6.5.-1. Possible configurations of cooling tubes for divertor cooling. The coolant tubes can be arranged along the poloidal direction as in case (a), normal to the poloidal direction with multiple passes as in case (b), or normal to the poloidal direction with a single pass as in case (c).

DIVERTOR COOLING WITH LIQUID METAL LAMINAR FLOW; NO INSULATION (Li: SOLID LINE Na: DOTTED LINE)

Fig. 6.5.-2. Variation of coolant exit temperature, pressure drop and pumping power with f_{RAD} or surface heat flux. The flow is laminar and the tubes are not electrically insulated.

DIVERTOR COOLING WITH LIQUID METAL

LAMINAR FLOW; TUBES INSULATED

(Li: SOLID LINE Na: DOTTED LINE)

Fig. 6.5.-3. Variation of coolant exit temperature, pressure drop and pumping power with f_{RAD} or surface heat flux. The flow is laminar and the tubes are electrically insulated.

Figure 6.5.-4 shows the results with turbulent flow with the tube wall electrically insulated. The minimum velocity for turbulent flow is calculated by equating the Reynolds number to the larger of $500 H_{\perp}$ and $60 H_{\parallel}$ where $H_{\perp}$ and $H_{\parallel}$ are the perpendicular and parallel Hartmann numbers, respectively. For the present case, the minimum velocity for turbulent flow is 33 m/s for Li and 17.6 m/s for Na. It is assumed that the coolant flows only one complete poloidal turn around the plasma. With turbulent flow and one complete turn, Figure 6.5.-4 shows that the maximum heat flux capabilities of Li and Na are about 7 MW/m² and about 4 MW/m² respectively. Using partial turns at high heat fluxes, a maximum heat flux capability of about 9 MW/m² can be reached for both Li and Na. The pumping power and coolant pressure are of the order of a few MW and below 5 MPa respectively.

6.5.1.3. Conclusions

Divertor cooling using liquid metal is feasible if the coolant tubes are electrically insulated and if the flow is turbulent. Liquid metal cooling is also desirable from the viewpoint of the efficiency of the thermal power cycle. The cooling channels should be aligned along the dominant magnetic field (i.e., the poloidal field) to reduce coolant pressure and pumping power. For the particular configuration and the magnetic field strengths used in this study, heat flux of up to 3 MW/m² can be handled by both Li and Na with laminar flow and up to about 9 MW/m² with turbulent flow. In the case of turbulent flow, the tube wall must be electrically insulated to avoid excessive coolant pressure and pumping power. In an actual design, a proper combination of laminar and turbulent flow, and of complete turns and fractional turn of the coolant tubes will have to be used.

6.5.2. Water Cooling

The ability to cool the TITAN divertor is one of the key factors in the successful operation of the device. The peak flux encountered in the divertor may be in excess of 10 MW/m² (see Sec. 6.3). The best coolant for such high heat flux environments appears to be water in the forced convective sub-cooled boiling regime. The heat flux is normally from only one side. The heat flux and fluid flow conditions for a horizontal water-cooled copper alloy tube have recently been simulated with an electron beam heating apparatus [23]. Flow velocities up to 10 m/s and peak heat fluxes up to 10 MW/m² were used to cover the conditions expected for high heat flux components. Inlet water temperatures

DIVERTOR COOLING WITH LIQUID METAL **TURBULENT FLOW, TUBES INSULATED** **(Li: SOLID LINE Na: DOTTED LINE)**

Fig. 6.5.-4. Variation of coolant exit temperature, pressure drop and pumping power with f_{RAD} or surface heat flux. The flow is turbulent and the tubes are electrically insulated.

of 30 °C and pressures of 17 atm were used throughout the test. The heated section of the tube was 8 cm long, leading to typical total temperature rises in the water of less than 15 °C. The tube inner diameter was about 1 cm, leading to a length to diameter ratio, L/D , of ~ 10 . The resulting sub-cooled level was about 150 °C. According to the results of this experiment the tube can handle 18 MW/m² heat flux at a coolant velocity of 12 m/s without reaching the critical heat flux (CHF) limits. Based on these results it could be concluded that the existing correlations for uniformly heated tubes could be used as a guide to predict the CHF of a tube heated only on one side when the peak heat flux is used as the correlation parameter.

The length to diameter ratio, L/D , for the TITAN divertor plate is about 50 ($L \sim 50$ cm and $D \sim 1$ cm, assuming that the coolant flows normal to the poloidal field, i.e., in the "short" direction along the divertor plate as shown in Fig. 6.4.-2) which translates into a range for the CHF of 10 - 20 MW/m² for coolant velocities of 10 - 20 m/s. For higher heat removal rates than these values, therefore, an alternative approach should be investigated.

It is known that swirl flow in tubes can enhance the single phase heat transfer coefficient and increase the CHF in sub-cooled boiling by a factor of ~ 2 over the corresponding straight flow values for conditions under which the tubes are uniformly heated in circumference and length [24]. The correlation for CHF derived by Gambill et al [24] from experimental results shows that the CHF is about 1.5 times higher than the corresponding CHF for straight flow for various L/D . For example, the CHF at $L/D = 50$ for $v = 10$ m/s is about 17 MW/m² for swirl flow compared with 11.5 MW/m² for straight flow.

Milora et al [25] report that application of this correlation to a tube heated only on one side results in an underestimation of the critical heat flux. The improvement in CHF evidently results from the circulating flow pattern. The large component of tangential velocity in swirl flow apparently creates a situation in which sub-cooled fluid is continuously swept past the heated side of the tube. In the high heat flux target experiment at Oak Ridge [26], water cooled copper swirl tubes were used for the heat transfer medium. Tube burn-out did not occur even at normal heat fluxes greater than 50 MW/m². Although additional experiments are required to obtain CHF values for swirl flow existing evidence indicates that CHF may not be a limiting factor for the heat flux range envisaged for the TITAN divertor. The other limiting factors are discussed below. It is suggested, therefore, that swirl flow be utilized for the divertor cooling.

For the water-cooled blanket design the proposed coolant inlet temperature is about 290 °C with an exit temperature of 320 °C. A possible inlet coolant temperature for the divertor could be the same as the first wall/blanket inlet temperature. The exit coolant temperature from the divertor depends on the heated length of the tube and the heat flux on the tube surface. The exit coolant from the divertor could be mixed with the exit coolant from the first wall/blanket before entering the heat exchanger. In this flow configuration the thermal energy from the divertor could be converted to electricity with the same thermal cycle efficiency as for the water-cooled first wall/blanket. If for reasons which are discussed below the divertor coolant inlet temperature cannot reach 290 °C, the thermal energy of the divertor could be used for feedwater heating. However, the safety aspects of this flow configuration should be investigated since part of the primary loop will then be located outside the containment building.

It was mentioned earlier that CHF may not be a limiting factor for the heat flux range anticipated for the TITAN divertor. One important factor is the maximum temperature that the tube wall material can withstand. Copper alloy was selected as a candidate material because of its high thermal conductivity. Data for the effects of radiation damage are extremely limited, but it is known that the transmutation of copper to nickel and zinc reduces the thermal conductivity. A value of 110 W/mK was used in this study, representing an end of life value and a factor of ~ 3 lower than for the unirradiated material. It is assumed that the maximum wall temperature limit applies to the mid-section of the tube wall (rather than the outside), and the maximum allowable temperature is taken as 450 °C (see Sec. 8.3.4). Based on these assumptions the maximum inner wall temperature of the copper alloy should not exceed 405 °C for a 1 mm wall thickness and a heat load of 10 MW/m². This temperature drops to 360 °C for the 20 MW/m² case.

The maximum heat removal capability of a coolant occurs in the fully developed sub-cooled boiling regime, where the main driving force is the difference between the wall temperature and the coolant saturation temperature (ΔT_{SAT}). The wall and coolant temperature distributions in a sub-cooled boiling regime are shown in Fig. 6.5.-5 [27]. In the divertor plate configuration where the plate is heated only on one side, the heat transfer at the wall-coolant interface is dominated by forced convective sub-cooled boiling on the heated side and by liquid single phase forced convection on the unheated side. Correlations for swirl flow heat transfer coefficients under these conditions

are not available. The correlation developed by Thom et al [28] for uniformly heated tubes in the forced convection sub-cooled boiling regime was chosen here to predict the ΔT_{SAT} at the point of peak heat flux. This method may be conservative since the lateral heat conduction in the tube wall reduces the heat flux to the coolant at the point of peak heat flux.

Table 6.5.-I shows the coolant conditions through a copper-alloy tube with $L/D = 60$ for heat fluxes in the range from 5 to 20 MW/m². The coolant is water with an inlet temperature and pressure of 290 °C and 150 atm respectively. The coolant velocity is taken to be 10 m/s. The second column of this table shows the upper temperature limit at the inner tube wall for a tube wall thickness of 1 mm. The next two columns give the temperature at this location at the tube inlet and outlet assuming that forced convection is the only mechanism of heat transfer operating. For these coolant conditions the wall temperature required for the onset of subcooled boiling, T_{ONB} , is 346 °C. As this temperature is higher than the outlet wall temperature for purely forced convection at a heat flux of 5 MW/m² no boiling will occur for this case.

TABLE 6.5.-I
Water coolant flow conditions for various heat fluxes
($L/D = 60$, $v = 10$ m/s, $T_{in} = 290$ °C, $p = 150$ atm)

q (MW/m ²)	T_{imax} (°C) ²⁾	T_{iw} (°C) ¹⁾		T_{FDB} (°C)
		inlet	outlet	
5	412	326	336	352
10	405	362	381	356
15	382	398	425	359
20	360	434	470	361

1) T_{imax} is the maximum allowable temperature at the inside of the tube wall and is based on a maximum allowable temperature of 450 °C for copper alloy at the mid-section of the tube wall and on a wall thickness of 1 mm.

2) T_{iw} is the inner tube wall temperature assuming that forced convection is the only heat transfer mechanism.

At heat fluxes of 10 MW/m^2 and higher the predominant heat transfer mechanism is sub-cooled boiling as the wall temperature is higher than the fluid saturation temperature. The wall temperature at which fully developed sub-cooled boiling starts, T_{FDB} , is shown in the last column of Table 6.5.-I. At a heat load of 20 MW/m^2 , $T_{\text{FDB}} = 361^\circ\text{C}$ which is only 1 degree above the upper temperature limit at the inner wall. This means that it is possible to transfer about 20 MW/m^2 of heat to the high temperature coolant by the forced convection sub-cooled boiling mechanism through a copper-alloy coolant tube.

6.5.3. Helium Cooling

6.5.3.1. Introduction

Helium can be a suitable coolant for high heat flux components but usually requires high temperature operation. Helium has a high heat capacity, is chemically inert and is transparent to neutrons. The gas itself does not impose any limitations on the operating temperature range of a system; the maximum coolant temperature is dictated by the structural material temperature limit. If high temperature materials are used, helium as a coolant can sustain high thermal power conversion efficiencies. But due to its low specific weight the use of helium is restricted by a limited gas-side heat transfer coefficient, high operating pressure and relatively high pumping power. These limitations do not always appear in isolated assessments of the gas-side heat transfer capabilities. The parametric study reported here aims at evaluating the maximum heat fluxes that can be removed from a realistically designed cooled surface exposed to a uniform, planar heat source. The focus is on the thermal-hydraulic aspects of the task.

6.5.3.2. Materials

<u>Symbol</u>	<u>Units</u>	<u>Definition</u>
α	K^{-1}	Mean thermal expansion coefficient
E	GPa	Modulus of elasticity (Young's modulus)
k	W/mK	Thermal conductivity
ν		Poisson's ratio
S_{mt}	MPa	Design stress limit
T	K	Temperature
T_{wmax}	K	Maximum wall temperature limit

The following materials and property data are considered:

Copper alloy (Cu). This material has an excellent thermal stress parameter and fabrication and manufacturing capabilities are well established. Although Cu is commercially available the data base for the irradiated material is sparse. The following property correlations, with estimated irradiation effects included for S_{mt} , are used in the calculations.

α	=	$3.257 \times 10^{-6} + 37.5 \times 10^{-9} T$	K ⁻¹	*)
E	=	$143.68 - 0.105 T$	GPa	*)
k	=	110	W/mK	
ν	=	0.34		
S_{mt}	=	100	MPa	
T_{wmax}	=	723	K	
*) based on AMAX-MZC [1]				

SiC/SiC composite. SiC is a ceramic with very high temperature capability. The irradiation resistance and activation is presumably relatively good. Manufacturing (woven fiber in a chemical vapor deposited matrix) is in the early stages of development. The following property correlations [29] are used here; irradiation effects and maximum wall temperature limits are estimated.

α	=	4.9×10^{-6}	K ⁻¹	
E	=	440	GPa	
k	=	15	W/mK	
ν	=	0.24		
S_{mt}	=	350 tensile	MPa	
		700 compressive	MPa	
T_{wmax}	=	1473	K	

Vanadium alloy (V). Vanadium is a high temperature, high strength material, developed mainly in LMFBR programs, and has good radiation resistance in alloys such as V-3Si-1Ti. The following property correlations are used here based on V-15Cr-5Ti [10]:

α	=	$8.95 \times 10^{-6} + 1.75 \times 10^{-9} T$	K^{-1}
E	=	$132 - 0.018 T$	Pa
k	=	$17.6 + 0.0135 T$	W/mK
ν	=	0.36	
S_{mt}	=	105	MPa for $T < 975 K$
		$300 - 0.26 T$	MPa for $T > 975 K$
T_{wmax}	=	973	K

6.5.3.3. Analysis

The configuration for the analysis is given in Fig. 6.5.-6. Straight tubes of a given inside diameter, d_i , outside diameter, d_o , and length, L , are arranged in a bank which is exposed to a unilateral and uniform heat flux, $\dot{q}$. These tubes are made of one of the materials referred to in Sec. 6.5.3.2. Helium at temperature T_{He1} enters the tubes at static pressure p_{He1} and leaves them at temperature T_{He2} and pressure p_{He2} . Complete mixing of the gas is assumed. The pressure drop calculation is based on a standard procedure with a recursive formula for the determination of the friction factor, to be multiplied with the mean pressure head [30].

The critical location for the heat transfer is at the tube exit where the maximum wall temperature limit, T_{wmax} , may not be exceeded. A one-dimensional radial heat transfer analysis is performed in the direction of the heat flux. This analysis consists of a conductive and a convective part. Depending on the material type two different procedures are followed to determine the linear temperature profile through the wall: for metals the limiting T_{wmax} is applied to the midsurface of the wall, whereas for ceramic materials T_{wmax} is applied to the outer surface. In both cases the material properties are evaluated for the respective temperature in the wall midplane. The convective gas-side heat transfer is based on a formula by DalleDonne [30]

$$Nu = 0.022 Re^{0.8} Pr^{0.4} \left[\frac{T_{if2}}{T_{He1}} \right]^{-0.18} \quad (6.5.-1)$$

where T_{if2} is the gas-side wall interface temperature at the tube outlet.

No corrections for either non-uniform heat flux or short tubes are made. For enhanced heat transfer 2-D surface roughening is assumed to increase the

Fig. 6.5.-6. Configuration for helium cooling: planar tube bank with uniform heat flux.

above-mentioned heat transfer coefficient by a factor of 2.4 while multiplying the friction factor by 4.

A thin wall tube stress estimation is also made. For the thermal stress a cosine outside temperature profile around the tube and a constant inside temperature are assumed. The tubes are treated as radially free while axially completely restrained. In the following formulae, which are exact for these conditions, σ_1 is the hoop stress and σ_{2rad} , σ_{2axc} and σ_{2axt} are the circumferential, axial compressive and axial tensile components of the thermal stress [31].

$$\sigma_1 = \frac{P_{He1} d_i}{(d_o - d_i)} \quad (6.5.-2)$$

$$\sigma_{th} = \alpha E \Delta T_{wall} \quad (6.5.-3)$$

$$\sigma_{2rad} = \pm \frac{\sigma_{th}}{2 (1 - \nu)} \quad (6.5.-4)$$

$$\sigma_{2axc} = - 0.75 \sigma_{th} \quad (6.5.-5)$$

$$\sigma_{2axt} = + 0.25 \sigma_{th} \quad (6.5.-6)$$

These components of the two-dimensional stress are combined in an equivalent von Mises stress, σ_{vM} , tensile for the inner, compressive for the outer surface, neither to exceed the $3 S_{mt}$ limit for secondary stresses.

6.5.3.4. Results

With respect to the high heat flux heat removal requirements of TITAN, particularly for the cooling of divertors, the analysis described here is used to determine the limitations of helium cooling. Based on a probable TITAN environment the following set of input parameters is chosen for a base-case comparison among the three materials selected in Sec. 6.5.3.2:

$$d_i = 10 \text{ mm}$$

$$d_o = 12 \text{ mm}$$

$$L = 0.75 \text{ m}$$

$$P_{He1} = 10 \text{ MPa}$$

$$\text{smooth surface}$$

$$T_{wmax} \text{ as stated in Sec. 6.5.3.2}$$

The mass flow rate through the tubes and the helium inlet temperature are varied such that the maximum wall temperature limit, T_{wmax} , at the tube exit cross section is exactly matched and at the same time none of the following limiting criteria is violated.

$$M_2 < 0.3 \qquad r_p < 3.1 \qquad f_p < 0.1$$

where M_2 is the Mach number at the tube outlet, r_p is the pressure ratio (pressure drop divided by the system pressure) and f_p is the pumping power fraction (based on the thermal power removed).

For the set of parameters chosen it is the pumping power fraction criterion which dominates; therefore all results presented here show the maximum achievable temperature level with $f_p = 0.1$. Among the stress limitations, $\sigma_1 < S_{mt}$ and $\sigma_{VM} < 3 S_{mt}$, the combined stress criterion is effective only in a few cases with heat fluxes in the 10 MW/m² range (see Fig 6.5.-10).

Some results of the base case calculations for the three selected materials are presented in Fig 6.5.-7. Due to its relatively low maximum wall temperature limit Cu cannot handle more than 8 MW/m², based on an inlet temperature limit set close to the environmental temperature of about 300 K. A coolant outlet temperature at which the heat removed is capable of generating electricity in a HTGR cycle would require $T_{He2} > 475$ K (based on extrapolated data from reference [32]). Applying this more restrictive criterion the maximum nominal heat flux for Cu is less than 5 MW/m². Both SiC and V can handle 8 - 10 MW/m² depending on the criteria. For nominal heat fluxes up to 7 MW/m² SiC has a considerable temperature advantage over V. This lead, however, quickly disappears for higher heat loads due to the more restrictive approach in the use of T_{wmax} and the somewhat lower thermal conductivity.

Fig 6.5.-8 shows the corresponding figures for SiC only. In addition to the base case data, the results for roughening the surface, doubling the wall thickness or doubling the tube length are given. Due to the aforementioned differences in the conductivity evaluation, SiC is particularly sensitive to an increase of the wall thickness. (It should be noted that the base case wall thickness of 1 mm, which probably represents a lower feasibility limit for a ceramic composite, is also applied for SiC here.) Due to its superior thermal conductivity, Cu is the least affected by thicker walls.

Fig. 6.5.-7. Results of base case calculations for the three materials indicated. (Base case: $d_i = 10 \text{ mm}$, $d_o = 12 \text{ mm}$, $L = 0.75 \text{ m}$, $p_{He1} = 10 \text{ MPa}$, smooth surface)

Fig. 6.5.-8. Comparison of SiC base case results with calculations for roughening the tube surface, doubling the tube length to $L = 1.5 \text{ m}$ or doubling the tube wall thickness to $d_i = 10 \text{ mm}$ and $d_o = 14 \text{ mm}$. (Base case: $d_i = 10 \text{ mm}$, $d_o = 12 \text{ mm}$, $L = 0.75 \text{ m}$, $p_{\text{He1}} = 10 \text{ MPa}$, smooth surface)

The suitability of the different materials for different wall thicknesses and nominal heat fluxes can be assessed without considering the gas-side heat transfer. The gas-side wall interface temperature, T_{if} , as a function of wall thickness and heat flux is governed by the maximum wall temperature limit and the conductivity of any given material. Fig 6.5.-9 shows the lines of equal T_{if} for the three material combinations. Along a line of equal T_{if} the two respective materials produce identical gas-side heat transfer conditions and are therefore equivalent from a thermal-hydraulic point of view. The material with higher thermal conductivity, but lower maximum wall temperature limit becomes preferable when moving away from this line in the direction of higher $\dot{q}$, higher wall thickness, W_t , (as indicated in Fig. 6.5.-9), and vice versa. Fig. 6.5.-10 presents the thermal stress limits and the combined stress limits at 10 MPa pressure difference across the tube wall. As the primary stress affects this limit only marginally, combining Fig. 6.5.-9 and Fig. 6.5.-10 results in a chart (Fig. 6.5.-11) which shows the thermal-hydraulically preferred material for a given combination of wall thickness and heat flux while at the same time satisfying the stress criterion.

6.5.3.5. Conclusion

This study shows that helium cooling is capable of handling nominal heat fluxes of up to 10 MW/m². Vanadium and SiC would permit high temperature operation. Vanadium is the material of choice for tubes with wall thickness between 0.5 and 1.0 mm, thicknesses that are thin for a ceramic composite.

To increase the reliability of the results presented the material data base needs a substantial expansion for the properties of irradiated materials. The evaluation of stresses could be improved with a 2-D analysis of the conductive heat transfer in the exit cross section of the tubes. Finally the asymmetric heat flux in the tubes enhances the build-up of hot streaks which are not accounted for in this study.

6.5.4. Innovative Concepts

Modeling of the divertor plasma suggests that the peak heat flux at the divertor target will be high. Some effort has therefore been spent in examining innovative concepts which may have the potential to accommodate higher heat loads and at the same time are resistant to damage by sputtering.

Fig. 6.5.-9. Equal interface temperature, T_{if} , lines

Fig. 6.5.-10. Thermal stress limit and combined (primary and secondary) stress limit at 10 MPa gas pressure difference across the tube wall.

Fig. 6.5.-11. Preferred material selection for a given combination of nominal heat flux and wall thickness; combined stress limit satisfied for 10 MPa pressure difference across the tube wall.

One approach involves allowing the divertor plasma to strike a liquid metal surface. The liquid will heat up, vaporize and condense on a cooler surface, such as the divertor chamber wall. As the condensing material will carry heat the load will have been spread over a larger area. The liquid metal could be supplied by seepage through a porous wall material such as for the INPORT design in the HIBALL inertial fusion reactor design [33].

To assess the feasibility of the concept a simple energy balance calculation was carried out. Assuming that all the power transported to the divertor is used to heat the liquid from its melting point to its boiling point and then to vaporize it, the mass flow rate required for various liquids was evaluated. For a value of f_{RAD} of 0.5 (corresponding to ~ 250 MW of power transported to the divertor) the mass flow rate varies from 10 kg/s for lithium to ~ 100 kg/s for liquid tin. From the mass flow rate an estimate was made of the neutral pressure of the evaporating material in the divertor chamber. Because the volume of the pressure chamber is relatively small the steady state neutral pressure is high, > 0.1 atm. Such a high pressure would cause a very large backflow of neutrals to the core plasma and the resultant contamination would be excessive.

An alternative approach which has been briefly examined involves the formation of a cloud of lithium droplets to intercept the divertor plasma stream. This concept was described in detail in references [34-36]. A cloud of fast moving lithium droplets is formed by an array of nozzles fed by high pressure liquid in a region of low magnetic field strength. The charged particles in the divertor plasma strike the droplets, are captured (lithium acts as a strong getter for hydrogen) and deposit their energy. To prevent significant evaporation of the droplet occurring (leading to the problem of high neutral pressure encountered above) the velocity of the droplet must be high, so that it spends only a short time subjected to the high heat flux. Although the divertor plasma forms a poloidally continuous ring it may only be possible to intercept it at one poloidal location (or over a limited range of poloidal angle), on the outboard side of the torus, to avoid the danger of the droplet cloud entering the main plasma. The reduced area for collecting the plasma increases the heat load correspondingly. Two possible configurations are shown in Fig. 6.5.-12.

A simple calculation has been performed to estimate the velocity of the droplet in order that the overall temperature rise (assumed uniform through the pellet) is limited to 500 °C. For the first option in Fig. 6.5.-12 a velocity

Option 1 Option 2

Fig. 6.5.-12. Schematic diagram showing two options for the configuration of the lithium droplet cloud.

of ~ 1 km/s is required, which is far too high for a feasible design. The velocity necessary for option 2 is ~ 50 m/s, implying that a pressure of ~ 1 MPa at the nozzle is required. Further investigation of the approach will need to include the temperature distribution through the droplet [37] to ensure that hot spots do not cause localized vaporization. A major design issue will be for a mechanism to collect the lithium droplets after they have passed through the plasma.

6.6. CONCLUSIONS AND FUTURE WORK

A poloidally-symmetric toroidal field divertor is to be used for the impurity control system in TITAN. This choice is based on the problems of erosion and plasma contamination associated with limiters and on the original work on divertors for reversed field pinches in the CRFPR design [5,7].

Magnet configurations for the divertor have been produced using a two-dimensional analysis. The closed geometry which is obtained (unlike the open geometry which is generally employed in poloidal divertors for tokamak reactors), due to the proximity of the divertor coils to the plasma, allows the divertor chamber to be decoupled from the plasma chamber and leakage or backflow of neutral particles from the divertor to the main plasma should be minimal. This closed configuration also tends to cause the flux surfaces in the divertor to be compressed, increasing the heat load on the divertor plate. The use of additional coils in order to expand the flux in this region was investigated but found not to be cost-effective. Examination of an open divertor is warranted on the basis of its apparent lack of poloidal asymmetries and flux expansion.

The Integrated-Blanket-Coil (IBC) approach has been considered for the divertor with the liquid metal cooled blanket design and provides several advantages over a design with conventional copper coils. The loss of breeding blanket coverage due to the multiple divertors ($\sim 10\%$ for the CRFPR [5]) is greatly reduced and the coils can be located closer to the plasma (offsetting the higher Ohmic losses in the IBC coils).

Modeling of the edge-plasma, using both analytic models and one-dimensional radial transport codes, indicates that the characteristic thickness for the radial decay of power flow in the scrape-off layer will be small, ~ 1 cm, implying high power loads on the divertor target. Injection of high Z impurities into the divertor plasma to radiate the incident power over a wider area has been examined and impurity fractions on the order of a few per cent are

necessary to reduce the heat flux to the divertor target significantly. An increase in the core plasma radiation fraction to > 0.75 is also suggested to reduce heat loads further. The possibility of using impurity radiation in the scrape-off layer to reduce the divertor heat load and to reduce the plasma temperature at the first wall and divertor target (hence, reducing the sputtering rate) appears attractive and will be investigated in the future.

Several cooling options for the divertor have been examined. Liquid metal cooling in the turbulent regime with electrically insulated tube walls (to minimize the MHD pressure drop and pumping power) allows heat loads of up to 9 MW/m^2 to be accommodated. Water-cooled copper tubes using swirl flow permit about 20 MW/m^2 of heat flux in the forced convection sub-cooled boiling heat transfer regime. With helium cooling up to 10 MW/m^2 is achievable although at this heat flux the heat is not removed at temperatures of interest for power generation. A brief investigation of innovative concepts has been made. Spreading the heat load by vaporization and remote condensation of a liquid metal has been shown to be infeasible because of the high pressure of the vaporized material which results. The use of a cloud of lithium droplets to intercept the divertor plasma may be possible but the droplet must have a high velocity to minimize its temperature rise.

Future work on the divertor will concentrate on the IBC approach for the Li/V blanket design. For the water-cooled design a study will be made of life-limiting processes for highly irradiated copper coils to determine their shielding requirements. A three-dimensional analysis of the magnetic configuration is necessary to study magnetic islands introduced by the divertor coils and the inboard-to-outboard asymmetries of the field lines to ensure that an acceptable divertor design is obtained.

The feasibility of confining the injected impurities in the divertor plasma will be examined with edge-plasma models. Improved neutral particle models will be incorporated to simulate recycling in the divertor and to allow a more accurate estimate of the plasma conditions at the divertor target to be made. The core and edge-plasma models will be coupled to ensure self-consistency of heat and particle fluxes. Profiles of plasma parameters in the scrape-off layer will be used to calculate the erosion rate at the first wall.

As the design progresses more detailed calculations on the thermal hydraulics and stress analysis of the divertor cooling will be made. The vacuum pumping system will be analysed to ensure that a large enough pumping speed can be attained to accommodate the required gas throughput.

These efforts will allow a more complete and credible divertor design to be achieved which will be integrated with the rest of the fusion power core design.

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7. REACTOR ENGINEERING

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7. REACTOR ENGINEERING

7.1. MAINTENANCE APPROACH

The primary objective of increasing the power density of a fusion reactor is to decrease the size (i.e., volume, mass) of the fusion power core (FPC), which in turn will decrease capital cost, development cost and lead to reduced installation and maintenance costs (i.e., reduced repair and down times and increased capacity factor). This overall approach is expected to reduce the cost of electricity (COE) and to reduce the time and cost required to develop fusion as a commercial power source [1].

Parametric studies show that the minimum COE for the RFP occurs at a power density corresponding to a neutron wall loading in the range 15-20 MW/m². The TITAN study elected to develop designs at this point, even though the systems studies predict that the COE increase in going from 20 to 10 MW/m² is minor (about 10-15%). One reason for the selection of the absolute minimum COE high neutron-wall-loading design point in the scoping phase is to determine quantitatively the engineering limits to power density for specific integrated designs. A second reason is that there are many issues relating to plant availability that may show a significant advantage for the high-power-density option. Specifically, single-piece FPC maintenance of a totally operational and pre-checked FPC may be possible above a power-density or below a total FPC mass threshold. The power density corresponding to the minimum COE may shift once these issues are quantified into a more elaborate availability model than used so far. Separate availability models are required for the single-piece, or "block", maintenance approach and for the multiple module replacement approach. The TITAN study will quantify the maintenance times in the conceptual design phase by the development of a design that allows single-piece maintenance of the FPC. The advantages of single-piece maintenance will be contrasted quantitatively with the problems of high neutron-wall-loading designs.

Single-piece maintenance of the FPC is expected to reduce maintenance time and risk, and to increase reliability relative to the modular approach. The time-reduction estimate is based on the elimination of component fit-up and sealing in an activated assembly. These operations can be performed on the replacement FPC in the shop prior to FPC replacement while the plant is at power. The financial risk associated with remote operations is also reduced with the single-piece approach. Complex maintenance procedures can result in

extended outages, particularly if FPC parts have been deformed, while the single-piece approach establishes a limit on the time required to recover from any failure; a shorter time is required to replace an expended FPC in toto with one that has undergone full non-nuclear testing in conditions that can be more severe than those encountered in actual nuclear service. Reliability improvement is achieved by the complete assembly and testing of the FPC prior to installation. The combined improvements in reliability and maintainability can result in improved plant availability.

The availability of the power plant is determined by the planned and unplanned outages of the reactor and the balance of plant. Typical planned outages for existing large plants are on the order of 40 days/year for coal plants, and up to 60 days/year for nuclear plants. Scheduled maintenance for the balance of plant alone is estimated to require an annual shut-down of 25 days. Typical unplanned outage periods are 50-60 days/year for existing U.S. large plants and this value is also adopted by fusion reactor studies [2], with the majority of the forced outages caused by the reactor (or coal plant boiler) components. For the purpose of the RFP reactor systems model, a typical plant with an aggressive availability goal is assumed to require $\tau_s = 40$ days/event for planned maintenance and $\tau_u = 60$ days per year for forced outages, resulting in an overall availability of 73% for one planned maintenance event per year. As discussed in Sec. 5.2.2, the TITAN studies couple the scheduled maintenance period per event to the neutron wall loading $I_w(\text{MW}/\text{m}^2)$ and the first wall/blanket fluence lifetime, $I_w\tau(\text{MWyr}/\text{m}^2)$ to give

$$P_f = \frac{\tau_u}{365 \left[1 + \left(\frac{\tau_s}{365} \right) \left(\frac{I_w}{I_w\tau} \right) \right]} \quad (7.1.-1)$$

A major goal of the TITAN study is to quantify the expected availability advantage of the compact reactor approach using single-piece maintenance. There is considerable leverage in designing a fusion system that can be maintained in an annual shutdown of 25 days, and this goal appears credible for single-piece maintenance [3]. The unscheduled maintenance time reduction expected because of pre-testing of the FPC and the upper limit on single failure downtime cannot be quantified without the development of an integrated design and equipment specification. If a reduction from 60 to 40 days were achievable, corresponding

to a scheduled outage reduction from 40 to 25 days, then the availability would increase from 73% to 82%. A reduction in COE of approximately 10% results and is a major motivation for further development of the single-piece maintenance approach.

Single-piece maintenance requires that the size and mass of the replaceable unit allow routine transport within the reactor cell and maintenance areas. The heaviest single piece considered in conceptual tokamak designs is the TF coil, and reactor cell crane capacities of 600 tonne are specified by STARFIRE and INTOR. This capacity is several times that of standard cranes, but the larger cranes can be supplied at a cost of about 5 M\$. An upper limit on crane capacity will be determined by economic trade studies, considering the building space and structural requirements, as well as the crane cost. The trade studies must also consider special horizontal transporters for the heavier components; lifts on the order of 1000 tonnes can be performed with gantry cranes. For guidance during the scoping study phase, the mass at which single-piece maintenance may become unattractive is anticipated to occur at ~500 tonnes.

Transportation of the complete FPC from the factory to the power plant was also considered. The diameter of the FPC is greater than 11 m, which limits the transportation method to barging and special overland transporters. Although special transportation is possible, with limits on plant site selection, the reference approach assumes that the final assembly and testing of the FPC will be performed at the plant site.

A general plant arrangement was developed (Figures 7.1.-1 and 7.1.-2) that takes advantage of the simplicity of the single-piece maintenance approach. A central reactor building containing an enclosed reactor cell connects the shop area at one end to the hot cells and waste processing areas at the other. A straight-through process is envisioned for the FPC replacement, in which the expended FPC is taken to the hot cell for disassembly, and the complete new FPC is brought in from the shop. The intent is to minimize the operations that require the re-assembly of activated or contaminated parts. This approach will simplify the remote maintenance equipment, since the only operation required in the reactor cell is external connections.

7.2. CONFIGURATION DEVELOPMENT

The TITAN study scoping phase considered a broad range of blanket and coolant options, rather than aiming at a complete integrated design. The

OVERALL PLAN

Fig. 7.1.1-1. General Plant Arrangement of TITAN.

SECTION E-E

Fig. 7.1.-2. TITAN Reactor Building Elevation.

general issues of the definition of the fusion power core compatible with the maintenance approach and the PF coil structural design were addressed during this phase.

The FPC includes the first wall, divertor (or limiter), blanket, shield, TF and PF coils, and the integral structure. The PF coils of the RFP are massive, are designed for the life of the plant, and are not connected to the rest of the FPC, so they are not considered to be a part of the replaceable FPC. The TITAN study has considered configurations using a completely open PF-coil set, but has not yet found a set that meets the electromagnetic requirements and allows direct torus removal. Therefore, some of the PF coils must be removed or relocated before the replaceable FPC can be removed.

In the compact RFP reactor (CRFPR) design study [3], the replaceable FPC weight (first wall, blanket, shield and TFCs, but not PFCs) is 300 tonnes and it is removed as a unit. The blanket breeder and coolant is PbLi, and most of the drained mass is accounted for by the shield and TF coils, which are reusable. Separate removal of the shield and TF coils in the reactor cell was considered in the scoping phase of the TITAN study (Fig. 7.2.-1) to avoid hot-cell operations to recover and re-assemble these components on a new blanket. This approach would require simple attachments, such as TF coil joints, to limit the downtime. The cost of the TF coils and shield is comparable to the cost of the blanket, and is low enough that re-use of these components may not be justified if complex removal operations are required. The specification of the replaceable FPC components will be investigated further when the integrated design of the reference TITAN blanket, shield and TF coil is completed. In addition, while the long-term radioactivity generated per unit electrical energy is invariant to the FPC (i.e., first wall, blanket, shield, and TFC) replacement schedule, the concentration of the rad-waste depends on replacement schedule, and class C burial considerations may dictate more frequent replacement of these components than is required by damage considerations alone.

The PbLi blanket used in CRFPR, which is unique in its large drainable mass, was not selected as an option in the TITAN scoping phase. The preliminary shield specified for the lithium blanket option, drained of coolant, weighs about 400 tonnes more than that of the drained shield in the PbLi design. The total removable FPC mass is greater than 600 tonnes, so that separation of at least part of the shield from the rest of the FPC may be preferred to single-piece removal. For designs with a lower wall loading, the FPC weight can become so large (e.g., FPC weights over 1000 tonnes for 10 MW/m² case) that

partitioning of the shield must be considered. The split-shield design would be simplified if the integrated-blanket-coil (IBC) concept [4] is used, so that separate TF coils do not need to be removed to gain access to the shield. Detailed design of the service connections and of the structural supports will be required to determine whether the advantages of single-piece maintenance can be retained with a split-shield design. Further system studies on the blanket and shield design, which give credit to low mass designs, are also warranted. Because of the large mass of the FPC, single-piece maintenance that includes the more massive shield may not be selected for the TITAN reference design. However, the design of the first wall and blanket as a continuous toroidal structure, which is replaceable as a unit, remains a strong possibility. This will retain the ability to assemble and test fully the FPC subsystems such as the primary coolant channels before installation.

The structural support of the TITAN reference PF coil set was investigated to determine the access to the replaceable FPC. The forces on the coils were calculated for cases with and without plasma current. The maximum force is on the superconducting EF coils and is carried by columns between the upper and lower coils. The structural arrangement has not been optimized pending better definition of the design and of the maintenance procedures for the FPC.

7.3. ENERGY CONVERSION SYSTEMS

A wide range of reactor coolant fluids and conditions were considered during the scoping phase of the TITAN study [5]. The cycles listed in Table 7.3.-I were reviewed for possible application to the TITAN requirements of high power density and efficiency.

Analysis of the energy-conversion system was later focused on the lithium and aqueous blanket designs, for which Rankine steam cycles were selected. This cycle is considered to be the best thermal cycle for source temperatures up to 600°C, which is the upper limit for the lithium coolant in the blanket. The superheated steam cycle was used for the lithium design, while a saturated steam cycle was used for the aqueous breeder design, because of its lower blanket temperature of 320°C.

The PRESTO computer code [6] was used to calculate the power plant thermal efficiencies for various superheat levels. An intermediate heat exchanger and intermediate coolant are specified, with a total temperature difference of 60°C across the heat exchangers. The calculated efficiencies without reheat are

Fig. 7.2.-1. Shield and TF Coil Removal.

TABLE 7.3.-I
Power Cycles Considered for TITAN.

Rankine

Single (steam, organic, Kalina)
Supercritical (SO_2 , CO_2 , H_2O)
Binary (mercury, potassium, cesium)

Brayton (closed cycle gas turbine,
dissociating gas cycle)

Rankine/Brayton

Field cycle

Combined Cycle

Brayton/Rankine (gas turbine-steam/organic)

Radiation-Catalyzed MHD

41.0% at a blanket outlet temperature of 600°C and 38.4% at 500°C. Reheat would increase these efficiencies by about 2 percentage points. Higher plant efficiencies are possible by employing a supercritical steam cycle. For example, with a 24 MPa steam pressure and 538°C peak cycle temperature, the efficiency could reach 43%. This plant would use seven feedwater heating systems, with a final feed water temperature of 250°C, and one reheat process. Pinch-point considerations for this cycle forces the inlet liquid lithium temperature to the blanket to be about 350°C. The final decision as to which cycle should be utilized depends on which power plant minimizes the COE, which trades off the increased efficiency with the cost of equipment needed to achieve reheat conditions.

The ORCENT computer code [7] was used to calculate the plant thermal efficiency of the saturated cycle for the aqueous breeder case. The outlet temperature and pressure of the blanket are estimated to be 320°C and 15 MPa, respectively. The plant thermal efficiency is estimated to be about 34.5%,

assuming a double-wall heat exchanger with a 40°C temperature drop. This efficiency corresponds to a cycle with a moisture separator and without reheat.

An alternative approach was proposed to cool the divertor of the lithium design with pressurized helium if the heat flux on the divertor plate is more than the heat-removal capability with liquid lithium coolant. A study has been initiated (Sec. 6.5.3) to investigate the heat removal capability of pressurized helium, and to determine the maximum possible exit temperature of the helium coolant for various divertor plate materials. This helium would be used for feed water heating if the temperature were too low for energy conversion.

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8. FUSION POWER CORE ENGINEERING

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8. FUSION POWER CORE ENGINEERING

8.1. INTRODUCTION

The TITAN research program is a multi-institutional effort to determine the potential of the reversed-field pinch (RFP) magnetic fusion concept as a compact, high-power-density, and "attractive" fusion energy system from economic (cost of electricity, COE), environmental, and operational view-points. In particular, a high neutron wall loading design (18 MW/m^2) has been chosen as the reference case in order to quantify the issue of engineering practicality, to assess significant benefits of compact systems, and to illuminate the main drawbacks. The program has been divided into two phases, each roughly one year in length: the Scoping Phase and the Design Phase.

During the first half of the scoping phase of the TITAN project several fusion power core (FPC) concepts were proposed and studied in various degrees of detail (Sec. 3). The four concepts which were selected for further analysis are presented in this section. The "final four" designs are:

- ♦ A self-cooled, lithium loop design with a vanadium alloy structure.
- ♦ An aqueous, self-cooled design with a copper first wall, beryllium neutron multiplier and Primary-Candidate-Alloy (PCA) structure.
- ♦ A self-cooled FLiBe pool design using a vanadium alloy structure.
- ♦ A helium-cooled ceramic design with a solid breeder and silicon carbide structure.

These four designs are described in Sec. 8.2 to 8.5, respectively. The performance of these designs was investigated in several areas: materials, neutronics, thermal hydraulics, structural analysis, tritium and safety analysis. A brief description of the general activities in each of these areas follows. A list of the general reactor operating parameters is shown in Table 8.1.-I.

Material properties were studied in the scoping phase to determine the feasibility and lifetime of materials in the fusion environment. The compactness of the FPC inevitably introduces a unique set of materials issues

TABLE 8.1.-I
GENERAL REACTOR OPERATING PARAMETERS

14 MeV neutron wall load, (MW/m^2)	18.
Plasma major radius (m)	3.9
Plasma minor radius (m)	0.60
Plasma aspect ratio	6.5
Reversed toroidal field during burn (T)	0.36
Poloidal field at plasma surface (T)	5.9
Net electric power (MW)	1,000.

associated with high power densities. These include management of very high surface heat loads, relatively short blanket material chronological lifetime because of higher radiation-damage rates and strong coupling between mechanical, chemical and electromagnetic environments. An effective irradiation-temperature operational lifetime window can be established if stress, swelling, embrittlement and creep behaviour are assessed. The limited data base for fusion reactor materials impedes efforts to establish such a lifetime window. For example, the stress environment can be estimated fairly accurately at beginning of life (BOL). However, changes in the microstructure of materials make it difficult to determine accurate stress responses of the materials as a function of time. Without sufficient data only crude estimates can be made based on an understanding of basic and fundamental processes taking place. The study has concentrated, therefore, on filling the gaps of experimental data with phenomenological equations which rely heavily on an understanding of fundamental processes.

During the scoping phase the neutronics analysis was primarily limited to one-dimensional investigations using the discrete ordinates transport code, ANISN [1].

For the TITAN reactor, the following neutronics design goals were chosen:

1. Local (full coverage, one-dimensional result) tritium breeding ratio should be larger than 1.2.
2. Life-of-plant (30 full power year) OH coils is desirable.

3. The blanket energy multiplication (M_n) should be maximized. Values of M_n greater than 1.2 are desired.

The neutronics scoping studies were performed to understand, parametrically, the general nuclear performance of the TITAN FPC design. All neutronics calculations in the scoping phase were performed using ANISN [1], with P_3S_8 approximations in cylindrical geometry. The cross section library employed is the 30 neutron 12 gamma-ray group MATXS5 library, processed at Los Alamos and based on the ENDF/B-V files [2]. The results of these calculations are expected to be within 2 to 3% of that calculated from the Monte Carlo code, MCNP [3], and continuous energy library, RMCCS [4], as will be shown later in the discussion of the reference design.

In a compact high neutron wall load device (18 MW/m^2) the first wall could have surface heat fluxes as high as 4.5 MW/m^2 and volumetric heat generation in the 100 MW/m^3 range. Detailed thermal-hydraulic analysis has been performed to ensure that all temperature limits are satisfied and that the power is removed at maximum efficiency for use in the thermal energy conversion cycle. The analysis made during the scoping phase involved a wide range of configurations (e.g., materials, coolant and structure). One-dimensional, analytical solutions and two-dimensional finite-element analyses were employed for all aspects of the thermal-hydraulic analysis.

Reversed-field pinch experiments appear to operate well even when the dominant core-plasma loss mechanism is radiation rather than conductive or convective energy transport. This highly radiative plasma mode of operation is particularly advantageous for high wall loading systems, as it distributes the plasma energy loss uniformly on the walls. This approach has been adopted for TITAN. A key parameter, f_{RAD} , has been defined as the ratio of radiative losses to the total plasma energy losses. For the 18 MW/m^2 base line, the heat flux on the first wall is roughly $4.5 \times f_{\text{RAD}} \text{ MW/m}^2$. For the purpose of the scoping phase, it was assumed that f_{RAD} can be varied, and for each design the maximum heat flux limitation of the first wall was determined. Although any first wall design is feasible if f_{RAD} is made sufficiently small, the decreased radiation results in higher heat fluxes in the divertor. Therefore, the first-wall thermal analysis must be closely coupled to the divertor thermal analysis (Sec. 6.5) for an optimized design.

The structural analysis of the FPC must consider both the thermal and hydrostatic loads. The high surface heat flux can lead to severe thermal

stresses, just as it can lead to excessive peak temperatures. Efficient energy recovery often requires high-pressure coolant, which generally leads to the dominant primary stress. Both analytical and numerical thermo-structural analyses have been performed during the scoping phase for the four FPC concepts.

The major issues for tritium systems are adequate tritium breeding and concerns in handling and containing the tritium. Tritium inventory and permeation relate to the safety of tritium containment and release during operation and maintenance. Tritium is a beta-emitter with a half-life of 12.3 years and an activity of 10,000 Ci/g. With a daily tritium production of about 360 g in the blanket, a very large radioactive inventory must be contained. Assuming a burn-up fraction of 10%, over 3 kg of tritium must flow through the plasma chamber and be cleaned and recycled each day, along with removal and cleaning of at least 360 g from the breeder. With atmospheric release limits of 10 to 100 Ci/d required, especial attention should be given to tritium containment. Containment becomes a greater concern with high-temperature operation as hydrogen isotopes can permeate through metals and other containment structures. A tritium systems flow diagram is shown in Fig. 8.1.-1, which is used to calculate inventory and leakage rates for the TITAN blanket designs.

The TITAN study is aiming towards a fusion reactor with four major features: minimum cost of electricity, high availability, design simplicity, and improved safety. These goals may not be achieved simultaneously and trade-offs are required. For example, if add-on safety equipment is needed, the design can become more complex and have lower availability and higher cost. On the other hand, if the safety features are incorporated in the design from the beginning, the reactor can potentially be inherently safe, simpler, and have higher availability and lower cost. The TITAN study, therefore, has designated safety as an integral part of the design activity.

8.2. SELF-COOLED LITHIUM VANADIUM BLANKET DESIGN

8.2.1. Concept Description

The TITAN lithium-vanadium blanket concept is a self-cooled design with liquid-lithium as the coolant and breeder and a vanadium-alloy structure. The general configuration is shown in Figs. 8.2.-1 and 8.2.-2. Operating parameters are listed in Table 8.2.-1. Both the blanket and first wall are cooled with lithium. The first wall and blanket coolant flow paths are poloidal, single pass with different exit temperatures as shown in Figs. 8.2.-1 and 8.2.-2. The

Fig. 8.1.-1. Tritium flow paths through the various reactor sub-systems.

Fig. 8.2.-1. The poloidal cross section of TITAN lithium-vanadium design.

Fig. 8.2.-2. Isometric view of TITAN lithium-vanadium blanket design.

TABLE 8.2.-I
OPERATING PARAMETERS FOR THE LITHIUM-VANADIUM DESIGN

FIRST WALL

Description: Bank of lithium-cooled, seamless circular tubes;
poloidal single pass flow; inter-tube welds for
toroidal electrical circuit and to reduce fretting.

Structural material	V-3Ti-1Si
Tube o.d. (mm)	10.5
Tube i.d. (mm)	8.0
Erosion allowance (mm)	0.25
Design lifetime (full power year, FPY)	1.
Poloidal radius (m)	0.68
Number of tubes	2,440.
First wall area (m ²)	166.
Surface heat flux, peak (MW/m ²)	4.7
Volumetric heat generation (MW/m ³)	
- Lithium	76.
- Vanadium	107.
Inlet temperature (°C)	300.
Outlet temperature (°C)	393.
Mass flow rate (kg/s)	1,460.
Volume flow rate (m ³ /s)	3.08
Velocity, peak (m/s)	22.5
Pressure drop (MPa)	11.2

two streams are mixed at the exit to achieve the desired bulk outlet temperature. Details of the first wall and blanket thermal hydraulics are discussed in Section 8.2.4.

The first wall consists of a bank of tubes with a nominal diameter of 10 mm. The relative tube positions vary with poloidal position to accommodate the wedge effect of the toroidal geometry. Typical first-wall cross sections are illustrated in Fig. 8.2.-3 for the inboard, top/bottom and outboard first wall locations. The change in toroidal width of the first-wall tube bank from inboard to outboard is 43%.

The breeding blanket consists of a bank of large tubes (75 mm o.d.), stacked 4-deep to form a 0.30 m thick blanket. These tubes must have decreased toroidal width on the inboard side to accommodate the toroidal geometry. The maximum increase in tube width from inboard to outboard is 64%. A typical blanket sector cross section is shown in Fig. 8.2.-4.

The shield for the lithium design is a two-piece, hot shield located behind the breeding blanket. The shield is designed to be a life-of-plant component because the radiation-damage rates are low relative to the first wall and

TABLE 8.2.-I (cont.)
OPERATING PARAMETERS FOR THE LITHIUM-VANADIUM DESIGN

BLANKET

Description: 4 rows of varying cross section, seamless tubes increased structural fraction with depth into blanket to maximize shield lifetime.

Structural material	V-3Ti-1Si
Tube o.d. (mm)	75.
Tube wall thickness (mm)	
- Row 1	2.75
- Row 2	3.25
- Row 3	4.00
- Row 4	4.50
Blanket thickness (m)	0.30
Volume fractions,	
- Lithium	0.64
- Vanadium	0.14
- Void	0.22
Volumetric heat generation, peak (MW/m ³)	
- Lithium	65.
- Vanadium	102.
Inlet temperature (°C)	300.
Outlet temperature (°C)	681.
Mass flow rate (kg/s)	667.
Volumetric flow rate (m ³ /s)	1.4
Velocity, peak (m/s)	0.3
Pressure drop (MPa)	1.2

SHIELD

Description: Lithium cooled, 2-piece hot shield; double-pass poloidal flow.

Structural material	V-3Ti-1Si
Moderator/absorber	HT-9
Volume fractions	
- Lithium	0.445
- Vanadium	0.044
- HT-9	0.511
Lifetime (dpa)	200.-250.
Damage rate, peak (dpa/FPY)	47.2
Inlet temperature (°C)	300.
Outlet temperature (°C)	681.
Mass flow rate (kg/s)	667.
Volumetric flow rate (m ³ /s)	1.4

Fig. 8.2.-3. Cross section of the first wall of the lithium-vanadium design, (a) inboard, (b) top/bottom, (c) outboard.

Fig. 8.2.-4. Horizontal, mid-plane cross section of the lithium-vanadium design.

blanket. Re-using the shield following a first-wall and blanket replacement reduces the yearly waste disposal from ~400 tonnes to ~50 tonnes. Approximately 40% of the neutron energy is recovered in the hot shield. The lithium coolant in the shield is mixed with first wall and blanket streams upon exit. The two-piece design requires a separate header set for each half of the shield. The lower shield segment is permanently installed.

The inlet and outlet ring headers for the first wall and blanket are located below the torus. A separate inlet header for the first wall is necessary because of the pressure requirements of the first wall thermal hydraulics (see Sec. 8.2.4). The first wall and blanket coolant streams are mixed together prior to entering the intermediate heat exchanger (IHx).

The remote connect/disconnects for the reactor torus (including the first wall, blanket, shield, and the divertor sections) are located on the pump/IHx side of the ring headers. This location minimizes the number of remotely handled connections. A bottom-access header system is chosen as opposed to an outboard midplane location because of the limited clearance between the reactor torus and the outer OH and EF coils (Fig. 8.2.-2) for headers and remotely activated shear joints. Non-moveable outer coils are chosen to minimize the number of components moved during first wall and blanket replacement. In addition, the delicate nature of the large superconducting EF coils may place limits on their mobility.

The upper shield segment is removed and placed aside for access to the first wall and blanket during replacement. After a new pre-tested first wall and blanket is installed the upper shield segment is placed back on top of the torus for operation.

A unique feature of this design is the integration of the breeding blanket with the toroidal field coils. The integrated-blanket-coil (IBC) concept [5] is discussed in Sec. 8.2.5. Key features of the IBC are: FPC simplification, reduced shielding requirements, easier access to the first wall and blanket, and reduced toroidal-field ripple.

8.2.2. Materials

8.2.2.1. Structural Material

The high power density of the TITAN reactor calls for a first wall and blanket structural material which is capable of high temperature operation. Comparison between thermal-stress factors and lithium corrosion limits of

vanadium-based alloys, HT-9, and PCA steels of BCSS [6] clearly points to the superiority of vanadium alloys, particularly at temperatures above 600°C. Three vanadium based alloys: V-15Cr-5Ti, VANSTAR-7 and V-3Ti-1Si, were studied and compared for the TITAN lithium-vanadium design.

Physical properties of vanadium-based alloys are not sensitive to moderate compositional variations [7]. Data for similar vanadium-based alloys can be regarded as representative for V-3Ti-1Si. Table 8.2.-II summarizes some of the properties as selected from Ref. 7.

To simplify assessment of material behaviour the complex interaction of the irradiation, stress, chemical, thermal and electromechanical environments can be divided into two categories: (1) high temperature irradiation effects on physical properties and (2) coolant compatibility issues. Point defect production during irradiation causes hardening of the matrix because of

Table 8.2.-II.
PHYSICAL PROPERTIES ADOPTED FOR V-3Ti-1Si [7]

Melting point (°C)	1,890.
Density (kg/m^3)	6,100.
Poisson's ratio	0.36
Young's modulus (GPa)	127.
Linear thermal expansion ($10^{-6}/\text{K}$)	
400°C	10.2
500°C	10.3
600°C	10.5
Thermal conductivity (W/m-K)	
400°C	26.8
500°C	28.0
600°C	29.5
Electrical resistivity ($\mu\Omega\text{-m}$)	
400°C	0.67
500°C	0.74
600°C	0.81
Specific heat (J/kg-K)	
400°C	535.
500°C	560.
600°C	575.

dislocation obstacle interactions. Hydrogen generation in structural materials during irradiation raises the concern of hydrogen embrittlement. Since the hydrogen concentrations required for the formation of hydrides in metals are large, hydrogen production and pickup has not been of great concern [8]. Loomis [9] has reported that hydrogen concentrations above 5000 appm are required to raise the ductile-to-brittle transition temperature (DBTT) of vanadium above room temperature [9]. Table 8.2.-III shows anticipated hydrogen and helium production rates in a vanadium first wall exposed to a 20 MW/m² neutron wall loading. Figures 8.2.-5.a,b illustrate the unirradiated and irradiated stress-strain characteristics of the candidate vanadium alloys.

Helium production from nuclear reactions enhances embrittlement. Latest results of irradiation hardening and helium production of the three vanadium-based alloys [10] show clearly that V-3Ti-1Si outperforms the other two in resisting the effects of neutron irradiation and helium generation. Because of lack of creep data for irradiated vanadium-based alloys, creep behavior under irradiation can only be estimated. Preliminary calculations [11] show that V-3Ti-1Si has a marginal creep behavior for TITAN design limits (lifetime ~ 1 yr and allowable stress range of 100 to 150 MPa). More detailed calculations of

Table 8.2.-III
HYDROGEN AND HELIUM PRODUCTION RATES IN THE FIRST WALL
OF THE LITHIUM DESIGN AT 20 MW/m² NEUTRON WALL LOADING [1]

<u>Isotope</u>	<u>appm per year</u>
H	6,287
D	332
T	36
<hr/>	
Total Hydrogen	6,655
³ He	nil
⁴ He	1,500
<hr/>	
Total Helium	1,500

(a)

(b)

Fig. 8.2.-5. Unirradiated (a) and irradiated (b) stress-strain relations for three vanadium alloys.

the creep behavior based on the amount of helium retained coupled with the Orr-Sherby-Dorn (OSD) parametric method [12] are planned.

The BCSS has rated vanadium-based alloys to be least corrosive in liquid lithium compared with iron-based alloys. Furthermore, vanadium alloys do not show liquid-metal embrittlement in liquid lithium. Based on a radioactive mass transport limit of $0.5 \mu\text{m/yr}$, the highest allowable vanadium/lithium operating temperature is 750 to 800°C . This mass transport limit as suggested by BCSS was adopted from 316 stainless-steel/sodium systems and may be overly stringent for vanadium/lithium systems.

The high cost of vanadium components necessitates that only the first wall and blanket be made out of vanadium-based alloys. The remainder of the coolant loop is made out of steel. Steels contain a large amount of nonmetallic additions such as C, N and O in solid solution. These nonmetallic impurities are leached from the steel by lithium through the formation of lithium compounds such as Li_3N , Li_2C_2 , Li_2O . Since the carbides and nitrides of vanadium are thermodynamically more stable than those of lithium, the lithium will give up the carbon and nitrogen leached from the steel. The transfer of nonmetallic impurities between two different alloys in contact with the same coolant is generally referred to as the Bi-Metallic Impurity Pickup (BMIP) mechanism.

While some options promise to mitigate BMIP (see Sec. 8.2.2.2), certain nonmetallic impurity levels in liquid lithium have to be assumed. Recent experiments conducted on a V-3Ti-1Si and a Ti-stabilized ferritic steel loop with liquid lithium have shown favorable results [13]. The formation of a dense, adhesive and exposure time independent vanadium and titanium nitride layer (~ 2 to $5 \mu\text{m}$) was observed which promises to be a barrier to complete bulk nitridation or carbonization of the reactor torus structure. Since these surface nitride layers are ceramics, they possibly will act as an electrical insulator to mitigate MHD pressure drops. However, the integrity of these layers in an irradiation environment remains to be investigated.

8.2.2.2. Piping

The BMIP issue plays a crucial role in determining the choice of material for liquid lithium piping outside the FPC. Ferritics are highly susceptible to decarbonization when in contact with liquid lithium. Addition of Ti and/or V to ferritics stabilizes their carbides. Austenitic steels, on the other hand, have more stable carbides than ferritics, but are more easily corroded in liquid lithium [14]. A protective layer, such as aluminum nitride (AlN), will protect

the steel from corrosion since AlN is stable in liquid lithium. Either one or a combination of the following options is available to mitigate BMIP: (1) use stabilized ferritic steels, or (2) use aluminized austenitic or ferritic steels for the remainder of the coolant loop.

Recent experimental findings show that stainless steel AISI 304 was only moderately attacked by highly purified liquid lithium at 550°C [15]. The material loss was measured to be not more than 2 $\mu\text{m}/\text{yr}$ at a flow velocity of 70 mm/s. Although the experiments were conducted at low velocities, they indicate the feasibility of using steel piping outside the FPC.

8.2.2.3. Coatings

The chemical and electromagnetic environment of a fusion device may necessitate the use of coatings as chemical barriers or electrical insulators. Experiments have shown the formation and compatibility of nitride layers on steels and vanadium-based alloys in a liquid-lithium environment [13,15]. In addition to constituting a chemical interaction barrier, these nitride layers also act as hydrogen diffusion barriers and electrical insulators. This latter property is most useful in the electromagnetic environment of a liquid-metal-cooled fusion device because electrically insulating coatings minimize MHD pressure drops. While the formation of electrically insulating nitride layers on V-3Ti-1Si alloys in a liquid-lithium environment has been shown experimentally [13], the integrity of this layer in an irradiation environment is questionable. The behavior of the vanadium and titanium nitride layer under irradiation is not known at this time. Boron nitride (BN), for example, swells anisotropically, which leads to cracking. Assuming that vanadium and titanium nitrides also have hexagonal crystal structures, these compounds would behave similar to BN under irradiation. The self-healing process of the vanadium and titanium nitride layer however, may compensate for cracking and loss during operation. Because of the uncertainties in the stability of insulating layers under irradiation, the use of laminated insulation structures is also under study in order to reduce the MHD pressure drop.

8.2.2.4. Magnets

The critical issue regarding magnet lifetime is the increase in resistivity of the conductor and swelling of the insulation material in an irradiation environment. The response of insulating materials to neutron irradiation has a direct bearing on the shield thicknesses required. Coil shielding thicknesses

directly influence the reactor dimensions and mass. The insulating material, therefore, has a strong impact on the design and economic performance of the device. Special attention is being given to the development of design equations for the radiation response of candidate insulating materials such as magnesium alumina or spinel.

8.2.2.5. Materials Summary

The interaction of the irradiation, chemical, thermal, stress, and electromagnetic environments in a high-power-density compact device poses stringent performance criteria on materials. Quantification of lifetime performances is difficult not only because of the lack of sufficient data base in most aspects of material behavior, but also because fundamental processes have not yet been studied in detail. The development of phenomenological equations to determine material lifetimes, therefore, can only serve as first-order approximations until more experimental support becomes available.

Efforts are continuing to study the irradiation response of the vanadium alloy, V-3Ti-1Si; the corrosion effects of liquid lithium on this alloy; the stability and viability of electrical insulation coating materials in a liquid-lithium environment; and the radiation response of electrically insulating materials.

8.2.3. Neutronics

8.2.3.1. Scoping Studies

The base case considered in the scoping phase is described in Fig. 8.2.-6. The neutronics model for the lithium-vanadium design consists of a plasma zone in the center of the cylinder with a radius of 0.60 m. A 50 mm scrape-off region is located between the plasma and the first wall. The first-wall zone consists of three regions: 6 mm vanadium alloy, 10 mm 50% vanadium alloy and 50% lithium, and 10 mm vanadium alloy. Two lithium zones follow the first wall zone, which are 0.375 m thick each and are divided by a 5 mm separation plate made of vanadium. The lithium zones are followed by a 5 mm vanadium wall and a normal copper magnet. The neutronics performance of this base case blanket is summarized in Table 8.2.-IV.

Table 8.2.-IV shows the tritium breeding ratio and nuclear heating rate in the base case design. The tritium breeding ratio is 1.42, of which 0.524 is from the ${}^7\text{Li}(n,n'\alpha)\text{T}$ reaction in lithium. The nuclear energy deposited in the

TABLE 8.2.-IV

SUMMARY OF THE NUCLEAR PERFORMANCE OF THE LITHIUM-VANADIUM DESIGN

Tritium breeding (T/DT neutron):

${}^6\text{Li}(n,\alpha)\text{T}$	0.898
${}^7\text{Li}(n,n',\alpha)\text{T}$	0.524
Tritium breeding ratio	1.42

Nuclear heating (MeV/DT neutron):

First wall zone	1.69
Lithium zones	12.59
Total blanket nuclear heating	14.28
Nuclear heating in OH coils	2.58

blanket is about 14.3 MeV per DT neutron corresponding to a blanket energy multiplication M_n of 1.01. The nuclear energy leaking into the magnet is about 2.58 MeV per DT neutron, which is about 18% of the energy deposited in the blanket. The fast neutron flux ($E_n > 0.1$ MeV) at the magnet is estimated to be 10^{26} n/m²/yr at 20 MW/m² neutron wall loading. This damage rate implies that the radiation lifetime of the magnet because of radiation-damage to the spinel insulator is four full-power years if the criterion given in Ref. 16 is used.

To improve the blanket energy multiplication and to increase the lifetime of the magnet, the base-case design was modified to employ a hot reflector/shield. Figure 8.2.-7 gives the schematic model of the modified design. The hot reflector/shield made of 10% lithium, 10% vanadium alloy, and balance of manganese steel, Fe1422, and is located between the lithium zone and the normal conducting magnets. A parametric study was performed varying the thicknesses of the lithium zone, the hot reflector/shield and the ${}^6\text{Li}$ content in lithium. The results are presented in Figs. 8.2.-8 to 8.2.-11.

Figure 8.2.-8 shows the fast neutron flux at the magnet as a function of total blanket/shield thickness and for several lithium zone thicknesses. The flux values are given at 20 MW/m² neutron wall loading. As shown in this figure, a lower fast neutron flux at the magnet can be obtained for a thinner lithium zone design, provided that the total blanket/shield thickness is kept constant. For a magnet design with a 30 full-power-year-lifetime, the required total blanket/shield thickness will be 0.70, 0.77, and 0.83 m with the lithium zone thickness of 0.4, 0.3, 0.2 m, respectively. Note these analyses were made

Fig. 8.2.-6. Schematic of the one-dimensional neutronics model for the base-case lithium-vanadium blanket design.

Fig. 8.2.-7. Schematic of the one-dimensional FPC neutronics model for the modified base-case design. The significant departure from the base case design is the inclusion of a hot reflector/shield between the lithium zone and the TF/OH magnets.

Fig. 8.2.-8. Fast neutron flux at the TF/9H magnets as a function of total blanket and shield thickness. The flux values are given for 0.2, 0.3 and 0.4 m thick lithium zone designs at 20 MW/m² neutron wall loading.

Fig. 8.2.-9. Tritium breeding ratio as a function of lithium zone thickness for the TITAN lithium-vanadium design with the total blanket/shield thickness fixed at 0.77 m. The tritium breeding ratios are given for several ^6Li enrichments in lithium, 7.4% (natural Li), 20% and 40%.

Fig. 8.2.-10. Tritium breeding ratio as a function of percent ^6Li in the lithium. The tritium breeding ratios are given for three lithium zone thickness designs: 0.2, 0.3, and 0.4 m. Note that the total blanket/shield thickness in these designs is fixed at 0.77 m.

Fig. 8.2.-11. Nuclear heating distribution in the TITAN lithium-vanadium blanket design.

with the results from the design calculations employing natural lithium. When the lithium is enriched with ^6Li , the fast neutron fluxes at the magnet will be further reduced, hence giving a longer lifetime than estimated based on natural lithium designs.

The tritium breeding ratio as functions of lithium zone thickness and ^6Li enrichment in lithium is shown, respectively, in Figs. 8.2.-9 and 8.2.-10. In these two figures, the total blanket/shield thickness is kept constant at 0.77 m. Figure 8.2.-9 shows that the tritium breeding ratio will exceed 1.20 when the lithium zone is thicker than 0.25, 0.28, and 0.35 m, respectively, at 40%, 20%, and 7.4% (natural lithium) ^6Li in lithium. However, when the lithium zone thickness is reduced to 0.3 m, the minimum ^6Li content in lithium to obtain a tritium breeding ratio of no less than 1.2 would increase to 14% or more. At 0.2 m lithium zone thickness, a tritium breeding ratio in excess of 1.20, is probably not achievable, as is also shown in Fig. 3.2.-10. From the above discussions it appears that, within the total blanket/shield thickness constraint of 0.77 m, the design with a lithium zone thickness of 0.3 m and a ^6Li enrichment of 20% in lithium is a reasonable reference case for more detailed engineering study of the blanket concept.

8.2.3.2. Reference Design Point

The nuclear performance of the reference design was explored in detail and is reported in this subsection. In these calculations the first wall zone was modified as: 1 mm vanadium alloy, 8 mm 20% vanadium alloy and 80% lithium, and 6 mm vanadium alloy. The modification was made to reflect an iterated design improvement in the first wall region.

Tritium breeding ratios and nuclear energy deposition rates in the reference design for 20% (enriched) and 7.4% (natural lithium) ^6Li in lithium are given in Table 8.2.-V. Results from both ANISN and MCNP calculations are compared in this table. The results from MCNP calculations, which are used as reference values, are statistically accurate to about 1%. Furthermore, the results from ANISN calculations are within about 3% of that calculated from MCNP, as shown in Table 8.2.-V. The significant difference occurs in the energy deposited in the magnets, where the ANISN calculations could underestimate the nuclear heating up to 50%.

Table 8.2.-V shows that the tritium breeding ratio for the case of 20% enriched ^6Li in lithium is about 1.29 and decreases to 1.21 when natural lithium is used. For the enriched Li case, the nuclear energy deposited in the

TABLE 8.2.-V

NEUTRONICS RESULTS FOR THE LITHIUM COOLED REFERENCE DESIGN.

PERCENT ^6Li IN LITHIUM:	20%		7.4%	
TRANSPORT CODE:	ANISN	MCNP	ANISN	MCNP
Tritium breeding (triton/DT neutron):				
$^6\text{Li}(n,\alpha)\text{T}$	0.869	0.890	0.716	0.733
$^7\text{Li}(n,n',\alpha)\text{T}$	0.391	0.404	0.458	0.475
Total TBR	1.250	1.294	1.174	1.208
Nuclear heating (MeV/DT neutron):				
First wall zone	0.86	0.88	0.87	0.89
Lithium zones	10.08	10.10	9.54	9.50
Reflector/shield	6.07	6.07	7.13	7.19
Total blanket/shield	17.01	17.06	17.54	17.57
Magnet nuclear heating	0.27	0.35	0.31	0.47

blanket/shield is about 17.1 MeV, which is equivalent to a blanket energy multiplication of 1.21. The blanket/shield nuclear heating rate in the system with natural lithium, however, is higher, about 17.6 MeV, corresponding to a blanket energy multiplication of 1.25. The nuclear energy deposited in the magnet behind the blanket/shield is about 0.35 MeV for the enriched Li case, however, this value increases to about 0.47 MeV if natural lithium is used.

The volumetric nuclear heating rate in the blanket/shield is depicted in Fig. 8.2.-11 as a function of distance from the first wall. The heating rate distribution is given for a neutron wall load of 20 MW/m^2 . The heating rate is about 88 MW/m^3 at the first wall. The volumetric heating rate in the lithium zone immediately behind the first wall is about 63 MW/m^3 and falls off steadily to about 27 MW/m^3 in front of the reflector/shield zone. The maximum heating in the reflector/shield zone, occurs at the region close to the lithium zone and is 43 MW/m^3 . The volumetric heating rate decreases as the location moves away from the lithium zone and is about 2 MW/m^3 at the end of the reflector/shield (45 cm from the lithium zone).

8.2.4. Thermal Hydraulics

8.2.4.1. Coolant Selection

The desirable features of the coolant are high thermal conductivity, high heat capacity, and compatibility with structural materials. Also, the coolant should improve, or at least should not degrade the blanket nuclear performance (i.e., tritium breeding ratio and energy multiplication). Furthermore, for a liquid-metal coolant, the electrical conductivity should be low enough so that the coolant pressure drop, caused mainly by the MHD effects, and the associated pumping-power are acceptable.

Of all the liquid metals, lithium has the largest specific heat capacity. Although sodium has slightly better electrical properties, the advantages of better thermal capacity of lithium outweigh its higher electrical conductivity. Liquid lithium combines the function of the coolant and the tritium breeder. Furthermore, liquid lithium is compatible with the proposed vanadium-alloy structural material (Sec. 8.2).

8.2.4.2. Thermal and MHD Analysis

In this section, a brief outline of the thermal and MHD analysis is presented. The results of this analysis are presented in the next section. The first wall consists of a bank of tubes, behind which are followed by blanket coolant channels. A diagram of the first wall and blanket coolant channels is given in Fig. 8.2.-2. As the poloidal magnetic field is much larger than the toroidal field, the coolant channels are aligned along the poloidal field to reduce the MHD pressure drop.

The thermal and MHD analysis is performed to determine the maximum structure temperature. Heat conduction in the first wall tube is assumed one-dimensional with a uniform heat flux equal to the maximum radiation heat flux, (q_0''') on the tube wall. Volumetric heat generation (q''') from nuclear heating in the tube wall is assumed uniform. The temperature distribution in the tube wall as a function of radial position, r , is given by:

$$T(r) = T_i + \left[\frac{q_0''' b}{k} + \frac{q''' b^2}{2k} \right] \ln \left(\frac{r}{a} \right) - \frac{q''' (r^2 - a^2)}{4k}, \quad (8.2.-1)$$

where T_i is the inner wall temperature, a is the inner radius, b is the outer

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where T_i is the inner wall temperature, a is the inner radius, b is the outer

radius and k is the thermal conductivity. The actual heat flux on the tube wall varies along the circumference and can be approximated by a cosine distribution. Because of the strong variation of the film temperature drop along the circumference, a 2-D solution of the energy equation in the coolant has been obtained with a method similar to that used by Reynolds [17]. To incorporate the effect of the magnetic field on the coolant velocity profile, the following velocity profile has been assumed.

$$v(r) = \frac{m+2}{m} \bar{U} \left[1 - \left(\frac{r}{a} \right)^m \right], \quad (8.2.-2a)$$

$$\bar{U} = \frac{2}{a^2} \int_0^a v(r) r dr \quad (8.2.-2b)$$

where $\bar{U}$ is the average velocity. Large values of the power index, m , simulate a high Hartmann number, $H \equiv Bd_h(\sigma/\eta)^{1/2}$ where B is the magnetic field, d_h is the hydraulic diameter, σ is the electric conductivity and η is the liquid viscosity. The resulting film temperature drop as a function of circumferential angle, θ , is then given by,

$$\Delta T_f(\theta) = f(m) \int_0^{2\pi} q''(\theta') d\theta' - \frac{a}{\pi k} \int_0^{2\pi} q''(\theta') \ln \left[2 \sin \left(\frac{(\theta - \theta')}{2} \right) \right] d\theta', \quad (8.2.-3a)$$

where

$$f(m) = \frac{a}{\pi k} \left[\frac{(m+2)^2 - 4}{4m(m+2)} - \frac{2(m+2)^2}{m^2} \left\{ \frac{1}{16} - \frac{1}{(m+2)(m+4)} - \frac{1}{4(m+4)} + \frac{1}{2(m+2)^3} \right\} \right], \quad (8.2.-3b)$$

and $q''(\theta')$ is the heat flux at the circumferential angle, θ' , on the surface.

The coolant exit temperature and coolant flow rate are calculated from an energy balance. The blanket is treated as a single channel as the final blanket channel configuration has not yet been decided.

The coolant pressure drop includes both friction and MHD effects although the latter is the dominant contributor. The coolant flow paths for the first

wall and blanket coolant channels are shown schematically in Fig. 8.2.-12. The perpendicular magnetic-field strength varies along the inlet/outlet ducts (AB, CD, EF and GH), but is constant along the first wall tubes and blanket channels (BC and FG). Bends and contractions/expansions are located at B, C, F and G.

Analytical expressions for MHD pressure drop for flow in straight ducts in a uniform perpendicular magnetic field have been obtained by solving the Navier-Stokes and Maxwell's equations under the assumptions of high Hartmann number, high magnetic interaction parameter and low magnetic Reynolds number, $Re_m = \sigma v d_h$ where σ is the electric conductivity, v is the velocity, μ is the magnetic permeability, and d_h is the hydraulic diameter [18,19,20]. These assumptions are generally valid in a fusion environment and allow the induced magnetic field to be neglected in Maxwell's equations and the inertia and viscous terms to be neglected in Navier-Stokes equation.

Hunt and Holroyd [21], and Hoffman and Carlson [22] have provided semi-empirical equations for MHD pressure drops for the cases of bends, inlet/outlet contractions and magnetic field gradient. The equations in Ref. 21 have been used to calculate the MHD pressure drops along the coolant paths shown in Fig. 8.2.-12. These equations are given in Table 8.2.-VI. In Table 8.2.-VI, $\Phi \equiv \sigma_w t_w / \sigma$, σ_w and σ are the electrical conductivity of the channel wall and the fluid, respectively, L is the flow length path, t_w is the thickness of the channel wall, u is the fluid velocity, and a is one half of the width of the coolant channel in the direction of B . Leakage paths for current in the coolant channels can seriously affect the MHD pressure drop [23]. Preliminary analysis has shown no leakage current effect for the coolant channel configuration used here.

The first-wall tubes will be subjected to high coolant pressure because the high coolant velocity required results in a large MHD pressure drop. Two-dimensional stress analysis of the coolant tubes has been performed. The primary stress is caused by the coolant pressure and is maximum at the entrance of a tube. The thermal or secondary stress is due to the temperature variation in the tube wall. The equations for these stresses are given by,

$$\sigma_{pr}(r) = \frac{a^2 p}{b^2 - a^2} \left(1 - \frac{b^2}{r^2} \right) \quad (8.2.-4a)$$

- A - Entrance
- AB- Straight Duct
 - Varying B-Field
- B - Inlet/Contraction
 - Bend
- BC- Straight Tube
- C - Bend, Expansion
- CD- Straight Duct
 - Varying B-Field
- D - Expansion

FW Flow Circuit

- E - Entrance
- EF- Straight Duct
 - Varying B-field
- F - Inlet/Contraction
 - Bend
- FG- Straight Channel
- G - Bend, Expansion
- GH- Straight Duct
 - Varying B-Field
- H - Expansion

Blanket Channel Flow Circuit

Fig. 8.2.-12. Illustration of lithium flow paths for FIRST WALL and blanket.

TABLE 8.2.-VI
EQUATIONS FOR THE MHD PRESSURE DROP

1. Straight channel (Magnetic field component normal to flow)	$\Delta P = \sigma u B^2 L \frac{\Phi}{1+\Phi}$
2. Inlet/contraction	$\Delta P = 0.2 \sigma u B^2 a \Phi^{1/2}$
3. Varying B-field	$\Delta P = 0.2 \sigma u B^2 a \Phi^{1/2} + \sigma u B^2 L \frac{\Phi}{1+\Phi}$
4. Bend, one leg normal to B-field	$\Delta P = \sigma u B^2 a \Phi^{1/2}$
5. Bend, both legs normal to B-field	$\Delta P = 0$

$$\sigma_{p\theta}(r) = \frac{a^2 p}{b^2 - a^2} \left(1 + \frac{b^2}{r^2} \right), \quad (8.2.-4b)$$

where σ_{pr} and $\sigma_{p\theta}$ are the radial and circumferential components, respectively, of the pressure stress, and

$$\sigma_{tr}(r) = \frac{\alpha E}{r^2} \left[\frac{r^2 - a^2}{b^2 - a^2} \int_a^b T(r) r dr - \int_a^r T(r) r dr \right] \quad (8.2.-5a)$$

$$\sigma_{t\theta}(r) = \frac{\alpha E}{r^2} \left[\frac{r^2 - a^2}{b^2 - a^2} \int_a^b T(r) r dr + \int_a^r T(r) r dr - T(r) r^2 \right], \quad (8.2.-5b)$$

where σ_{tr} and $\sigma_{t\theta}$ are the radial and circumferential components, respectively, of the thermal stress.

The equivalent stresses are:

$$\sigma_{eq,p}(r) = \left[\frac{1}{2} (\sigma_{pr} - \sigma_{p\theta})^2 + \sigma_{pr}^2 + \sigma_{p\theta}^2 \right]^{1/2} \quad (8.2.-6a)$$

$$\sigma_{eq,t}(r) = \left[\frac{1}{2}(\sigma_{tr} - \sigma_{t\theta})^2 + \sigma_{tr}^2 + \sigma_{t\theta}^2 \right]^{1/2}, \quad (8.2.-6b)$$

where r is the radius, θ is the angle along the circumference, a is the inner radius, b is the outer radius, E is Young's modulus and α is the coefficient of linear thermal expansion of the tube material. The temperature distribution in the tube wall (Eq. 8.2.-1) is $T(r)$, and p is the coolant pressure. A more accurate description is given by a complete 2-D temperature distribution that includes the effect of poloidal curvature of the coolant tubes. A detailed thermo-mechanical analysis is discussed in Sec. 8.2.6. Similar analysis will be performed for the blanket channels later.

8.2.4.3. Thermal-Hydraulic Design Window

In this section, the results of design calculations are cast in the form of design windows. The logic and sequence of the calculations are shown in the flow chart in Fig. 8.2.-13. The results presented here are for the 18 MW/m² neutron wall loading design. The relevant reactor parameters and the limiting design values are shown in Table 8.2.-VII. The TITAN reactor operates with a highly radiative plasma (Sec. 4). A key parameter, f_{RAD} , has been defined as the ratio of the radiation losses to the total losses from the plasma. For the 18 MW/m² reference case, the heat flux on the first wall is roughly

TABLE 8.2.-VII
REACTOR PARAMETERS USED FOR PRELIMINARY MHD ANALYSIS

$R = 3.9 \text{ m}$	$\eta_{th} = 0.4$
$a_w = 0.65 \text{ m}$	Blanket thickness = 0.775 m
$B_p = 6 \text{ T at first wall}$	$T_{w,max} = 750 \text{ }^\circ\text{C}$
$= 3 \text{ T at back of blanket}$	$T_{in} = 300 \text{ }^\circ\text{C}$
$B_T = 0.36 \text{ T}$	$\sigma_{p,design} = 100 \text{ MPa}$
$P_{th} = 2866 \text{ MW}$	$\sigma_{s,design} = 300 \text{ MPa}$
$M = 1.33$	$t = 1.5 \text{ mm}$
$d_i = 8 \text{ mm}$

Fig. 8.2.-13. Flow chart of the calculation steps for the MHD analysis.

$4.5 \times f_{\text{RAD}}$ MW/m². For the purpose of the scoping phase, it was assumed that f_{RAD} can be varied in order to determine the maximum heat-flux limitation of the first wall.

Fig. 8.2.-14 shows the coolant pressure increment above the exit pressure for 3.6 MW/m² ($f_{\text{RAD}} = 0.8$) of heat flux on the first wall tubes. The maximum pressures at the inlets of the first wall loop (point A) and blanket flow loops (point E) are 8.4 MPa and 1.2 MPa, respectively. Using electrical insulation in the inlet/outlet ducts makes the pressure drops in the ducts (AB, CD, EF and GH) negligible. In this case the maximum pressures are 8 MPa and 0.6 MPa for the first wall and blanket flow loops, respectively. The large pressure drop in the first wall tubes is caused by the high coolant velocity necessary to remove the high surface heat flux.

The maximum equivalent stresses in the first wall tubes are shown in Fig. 8.2.-15 as a function of heat flux (or f_{RAD}). Even at a surface heat flux of 4.0 MW/m² ($f_{\text{RAD}} = 0.9$), the stresses are below the allowable design stress, which for vanadium alloys are taken as 100 MPa for primary stress and 300 MPa for secondary or thermal stress.

Figure 8.2.-16 shows the design window plotted in terms of T_{EXIT} and the surface heat flux. The upper line shows the maximum coolant exit temperature possible without exceeding the maximum allowable V-alloy temperature, which is taken as 750°C based on the recommendation in BCSS [6]. The lower limit on T_{EXIT} is determined by the limit of 5% of the gross electric power recirculated for pumping. Using electrical insulation in the inlet/outlet ducts lowers the lower limit on T_{EXIT} and enlarges the design window. The curves for the stress limit are below the pumping power limit curves and, hence, do not affect the design window. This design window shows that the first wall can be effectively cooled by lithium up to a heat flux of 4.5 MW/m² ($f_{\text{RAD}} = 1.0$). A coolant exit temperature of more than 500°C can be obtained, which results in good thermal efficiency. This conclusion is in contrast to the results of previous reactor studies, which have placed a much lower limit on the surface heat flux with liquid metal cooling [6].

Removal of high surface heat fluxes in the TITAN design is possible for two reasons; (a) use of turbulent flow when necessary and (b) two different exit temperatures for first wall and blanket flow loops and then mixing them to obtain the desired average exit temperature. The transition to turbulent flow is possible in the RFP reactor configuration because of the low perpendicular magnetic field, 0.36 T. The onset of turbulence occurs at about 20 m/s.

TYPICAL DISTRIBUTION OF COOLANT PRESSURE

Fig. 8.2.-14. Coolant pressure along the length of the first-wall and blanket channels.

Fig. 8.2.-15. Stress in first wall tubes as a function of incident surface heat flux.

Fig. 8.2.-16. First wall and blanket design window with different exit temperatures from the first wall and blanket.

Fig. 8.2.-17 shows the design window for the case of no mixing or equal exit temperatures for both the first wall and blanket flow loops. For this case, the limiting heat flux on the first wall is about 2.3 MW/m^2 ($f_{\text{RAD}} = 0.5$).

8.2.4.4. Conclusions

The MHD and thermal-hydraulic analysis of the first wall and blanket for the 18 MW/m^2 TITAN reactor design shows that liquid-lithium cooling is feasible even with a first wall heat flux as high as 4.5 MW/m^2 ($f_{\text{RAD}} = 1.0$). Several reasons lead to this highly encouraging result: (a) the dominant field in a RFP is in the poloidal direction, while the coolant flows across the much weaker toroidal field (0.36 T); (b) separate flow loops for the first wall and blanket are used; (c) provisions are made for different coolant exit temperatures for the first wall and blanket loops and subsequent mixing, and (d) turbulent flow is used where necessary. Liquid-metal cooling allows operation with a high coolant exit temperatures ($>500^\circ\text{C}$), which results in a high thermal power cycle efficiency of about 40%.

Work in several areas is ongoing. Detailed MHD and thermal hydraulic analysis of the blanket will be considered shortly. Additional MHD analysis, including 2-D and 3-D investigations as well as spatial variation of volumetric heating in the blanket, is planned. Although thermal considerations dictate the use of a thin first wall, consideration must be given to the erosion rate anticipated from plasma sputtering.

8.2.5. Integrated-Blanket-Coil (IBC)

One of the unique features of the lithium-vanadium blanket design is the integration of the breeding blanket and the toroidal field coils into a single component. The Integrated-Blanket-Coil (IBC) [5] concept combines the blanket functions of tritium breeding and energy recovery with the coil function of magnetic field production in a single component. Several benefits anticipated from adopting the IBC approach are listed as follows:

1. Simplification of the FPC by combining the function of two components into a single unit.
2. Shielding normally required for copper toroidal field (TF) coils is no longer needed.

Fig. 8.2.-17. The design window for the first wall and blanket with equal exit temperatures.

3. Easier access for maintenance to first wall and blanket because TF coils need not be removed.
4. Reduced toroidal-field ripple because of the full coverage coil design.

Possible drawbacks to the IBC concept are as follows:

1. High-current, low-voltage power supplies are required. These power supplies can be quite large and the associated bussing connections may be cumbersome.
2. Trade-offs between thermal-hydraulic flow paths and electrical flow paths need to be addressed.
3. Field errors produced in the vicinity of the current connections should be minimized.

The IBC concept is illustrated schematically in Fig. 8.2.-18. The simplest configuration is dictated by the hydraulic flow path, which results in a single-turn TF coil. The current required per coil (16 coils per torus) is 480 kA and is driven through the main lithium channel in the blanket. Typical operating parameters for the TF IBC are listed in Table 8.2.-VIII.

Although the electrical resistivity of liquid lithium is about 20 times that of copper, the resistance of the IBC is comparable to that of the normal copper TF coils. The comparable resistance results because of a larger cross sectional area for current flow (lower current density) and a shorter current length because of the smaller minor radius of the IBC coils. The Joule losses in the IBC coil set are 16 MW, as compared with 34 MW in the copper TF coils. In addition, the power loss in the IBC can subsequently be recovered in the thermal energy conversion cycle resulting in a net power loss of 9.6 MW, not accounting for losses in the bussing which may be appreciable.

The most serious drawback to the IBC concept is the need for high-current, low-voltage power supplies (480 kA, 2 V/coil). After accounting for losses associated with leakage current in the headers and resistive losses in the

Fig. 8.2.-18. Illustration of the IBC concept.

TABLE 8.2.-VIII
PRELIMINARY IBC TOROIDAL FIELD COIL PARAMETERS

Total current (MA-turns)	7.5
Number of single turn coils	16
Current per coil (kA)	470.
Voltage drop per coil (V)	2
Number of coils per power supply	4
Power supply characteristics ^a	
Voltage (V)	18.
Current (kA)	600.
Power (MW _e)	10.8
Cost (M\$)	7.1

^a including losses in the bussing and the coolant headers.

bussing, the power-supply requirement for each loop is 600 kA at 18 V. Four of these power supplies are required.

Large 18-V/60-kA rectifier modules can be constructed with conventional components. To provide 600 kA, ten modules must be connected in parallel. The cost for four power supplies and the required bussing would be 28 M\$. An alternative to the conventional power supply is to use motor driven homopolar generators (HPGs). Because the HPGs can be located closer to the machine than the rectifier power supplies, 12 V/quadrant is required instead of 18 V. Medium size HPGs have been built for short pulse application. No reason could be identified why a water-cooled steady state generator could not be built for this application. The breakeven cost for these generators is estimated at \$885/kW. An accurate estimate of the cost of the motor driven HPGs cannot be made because the large steady state HPG must be developed for this application. It is possible that this power supply could be developed and produced for substantially less than \$885/kW, which would make HPGs the more attractive power-supply option.

The trade-offs between the thermal-hydraulic flow path and the electrical current path are being investigated. To reduce the Joule losses through the primary loop piping and components, the blanket coolant inlet and outlet should be at the same electrical potential. Such a configuration is illustrated in Fig. 8.2.-19a. The disadvantages with this flow geometry are as follows:

TWO
HYDRAULIC
TURNS

SINGLE
HYDRAULIC
TURN

Fig. 8.2.-19. Two options for the hydraulic and electrical arrangement of the IBC circuit.

1. The flow must make two poloidal passes, which requires a coolant velocity that is twice as high as that in a single pass configuration. The higher velocity results in larger friction and MHD pressure drops.
2. An additional bend in the flow path is necessary. The bend, as discussed in Sec. 8.2.4, is a large contributor to the MHD pressure drop.

The preferred layout from a thermal hydraulic viewpoint is a single poloidal pass design as shown in Fig. 8.2.-19. This layout has an electrical loss associated with current leakage through the primary loop. Grounding of the primary loop before the IHX and after the primary coolant pumps will be necessary to prevent current flow through these components. Locating the current leads close to the blanket and increasing the electrical resistance of the primary loop will reduce the leakage current to an acceptable level. The present design has primary-loop electrical losses equal to about 10% of the power consumed in the IBC. Further study of the IBC concept is ongoing. Research is required in areas such as electrical grounding requirements, power supply development, and electrical current lead attachment methods.

8.2.6. Structural Analysis

8.2.6.1. Thermo-mechanical Analysis of the First Wall

The first wall geometries and loading conditions suggest that both the thermal and mechanical fields will exhibit significantly smaller gradients in the poloidal direction than in the radial and/or toroidal directions. A two-dimensional modeling of the cross section at the location of the extreme value of the loading in the poloidal direction, therefore, is appropriate. This approach has been used in several analyses [6,24], but the assumptions involved were not clearly indicated or were overly conservative. One possible reason for the conservatism is the virtual invariance of the temperatures and, to a lesser degree, the pressure stresses to the assumptions for structures with large radius of curvature. The thermal stresses, however, exhibit more pronounced dependence on the modeling.

The six components of the strain tensor for small deformations, ϵ_{ij} , in the Cartesian coordinate system are given in terms of the displacements as follows:

$$\epsilon_{ij} = \frac{1}{2} \left[\frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} \right], \quad (8.2.-7)$$

where x_i ($i=1,2,3$) represents a Cartesian coordinate system and u_i denotes the displacements. If certain assumptions regarding the behavior in a direction are considered, the number of independent stress and strain components can be decreased significantly.

Plane-Strain Assumptions. The displacement components shown on Fig. 8.2.-20 are assumed to have the following form:

$$u_1 = u(x,y) \quad (8.2.-8a)$$

$$u_2 = v(x,y) \quad (8.2.-8b)$$

$$u_3 = w = 0. \quad (8.2.-8c)$$

These assumptions describe exactly a situation when a cylinder of arbitrary length is entirely constrained from axial movement and the deformation is independent of the axial dimension.

$$\epsilon_{xx} = \frac{\partial u}{\partial x}, \quad \epsilon_{yy} = \frac{\partial v}{\partial y}, \quad \epsilon_{xy} = \frac{1}{2} \left[\frac{\partial u}{\partial y} + \frac{\partial v}{\partial x} \right]. \quad (8.2.-9)$$

The constitutive equations including thermal strains are:

$$\epsilon_{ij} = \frac{1}{E} \left[(1 + \nu) \sigma_{ij} - \nu \sigma_{kk} \right] + \alpha T \quad (8.2.-10a)$$

$$\sigma_{kk} = \sigma_{xx} + \sigma_{yy} + \sigma_{zz}, \quad (8.2.-10b)$$

where σ_{ij} is the stress tensor, E is the Young's Modulus, α is the thermal expansion coefficient and T is based on some reference temperature. From Eq. (8.2.-10) the following expression results:

Plane Problem

Axisymmetric Problem

Fig. 8.2.-20. Displacement components.

$$\epsilon_{zz} = \frac{1}{E} \left[(1 + \nu)\sigma_{zz} - \nu(\sigma_{xx} + \sigma_{yy} + \sigma_{zz}) \right] + \alpha T = 0, \quad (8.2.-11)$$

which yields

$$\sigma_{zz} = \nu(\sigma_{xx} + \sigma_{yy}) - E\alpha T. \quad (8.2.-12)$$

If equivalent properties $E' = E/(1-\nu^2)$, $\nu' = \nu/(1-\nu)$ and $\alpha' = \alpha(1+\nu)$ are introduced, then the constitutive equations assume the following form:

$$\epsilon_{xx} = \frac{1}{E'} (\sigma_{xx} - \nu' \sigma_{yy}) + \alpha' T \quad (8.2.-13a)$$

$$\epsilon_{yy} = \frac{1}{E'} (-\nu' \sigma_{xx} - \sigma_{yy}) + \alpha' T \quad (8.2.-13b)$$

$$\epsilon_{xy} = \frac{1 + \nu'}{E} \sigma_{xy}. \quad (8.2.-13c)$$

Additional mechanical field equations include equilibrium and compatibility equations.

Piane-Stress Assumptions. These assumptions approximately correspond to the behavior of a thin disk that is free of loading at the flat ends:

$$u_1 = u(x, y) \quad (8.2.-14a)$$

$$u_2 = v(x, y) \quad (8.2.-14b)$$

$$\sigma_{zz} = \sigma_{xz} = \sigma_{yz} = 0. \quad (8.2.-14c)$$

The tractions have to vanish there, which is exactly the condition given in Eq. (8.2.-18). Since the disk is thin, the variation of these stress components must be small compared with the in-plane stresses. The strains for small deformations are the same as those in Eq. (8.2.-9).

$$\epsilon_{xx} = \frac{1}{E} (\sigma_{xx} - \nu \sigma_{yy}) + \alpha T \quad (8.2.-15a)$$

$$\epsilon_{yy} = \frac{1}{E} (\sigma_{yy} - \nu \sigma_{xx}) + \alpha T \quad (8.2.-15b)$$

$$\epsilon_{xy} = \frac{1 + \nu}{E} \sigma_{xy} \quad (8.2.-15c)$$

$$\epsilon_{zz} = -\frac{\nu}{E} (\sigma_{xx} + \sigma_{yy}) + \alpha T . \quad (8.2.-15d)$$

Comparing these expressions with Eqs. (8.2.-13) shows that the in-plane quantities of a plane-strain problem can be obtained by solving the corresponding plane-stress problem with the equivalent properties. The out-of-plane stress component can be calculated from Eq. (8.2.-12). For thermal problems in toroidal fusion applications the out-of-plane forces can be high, since the αE product ranges from 1 to 3 for the structural materials considered, and the temperature difference between the current and the reference temperature can exceed 300°C.

Axisymmetric Deformation Assumptions. The displacements are assumed to be given by:

$$u_1 = u = u(x, y) \quad (8.2.-16a)$$

$$u_2 = v = v(x, y) \quad (8.2.-16b)$$

$$u_3 = w = 0 . \quad (8.2.-16c)$$

The remaining strain components from Eq. (8.2.-7), expressed in the cylindrical coordinate system, are as follows:

$$\epsilon_{xx} = \frac{\partial u}{\partial x} , \quad \epsilon_{yy} = \frac{\partial v}{\partial y} , \quad \epsilon_{xy} = \frac{1}{2} \left[\frac{\partial u}{\partial y} + \frac{\partial v}{\partial x} \right] , \quad \epsilon_{zz} = \frac{u}{r} . \quad (8.2.-17)$$

There are four stress components. The out-of-plane stress is not zero, and its value is generally less than that obtained from a plane strain analysis of the same plane section under the same loading conditions. If the geometry and the loading of the structure considered is nearly axisymmetric, then the modeling should also be axisymmetric. Plane-stress modeling underestimates, whereas plane strain formulation overestimates the out-of-plane stress component.

Since the structural material is metallic, the relevant uniaxial stress measure is the von Mises stress, which is given by

$$\sigma_{eq} = \left[0.5 \left((\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2 \right) \right]^{1/2}, \quad (8.2.-18)$$

where σ_1 , σ_2 and σ_3 are the principal stresses. Yielding in metals has been observed to occur as a result of distortion and not volume change; therefore, the equivalent stress is based on the equality of the distortional strain energy due to the given three-dimensional stress state and the distortional energy due to the uniaxial equivalent stress. Note that a hydrostatic state produces zero equivalent stress.

In this section the results obtained from plane-stress and axisymmetric analyses are discussed. Pressure stresses as well as thermal stresses are calculated. The geometry analyzed is a circular tube with inner diameter of 8.0 mm, outer diameter of 11.0 mm, and radius of curvature of 0.65 m. The internal pressure is 7.4 MPa. Two cases of thermal loadings are considered, uniform heat flux and one-sided heating emanating from the center of curvature. In both cases the peak heat flux is 3.65 MW/m^2 , the internal heat-generation rate is 115 MW/m^3 , the fluid bulk temperature is 398°C and the Nusselt number is 10.5. The calculations were performed using ANSYS, a general purpose finite-element code [25]. The cross section of the first-wall tube shown in Fig. 8.2.-21a was modeled employing symmetry. The boundary conditions for the thermal and mechanical analyses are explained on Fig. 8.2.-21b.

Pressure Stresses. The in-plane pressure stresses exhibit the same axial symmetry within the tube cross section in both plane-stress and axisymmetric analyses. The effect of poloidal curvature appears only in the presence of a near constant poloidal stress. The maximum equivalent stress is 28 MPa in the plane-stress analysis and 26.7 MPa in the axisymmetric analysis. The latter

Fig. 8.2.-21. Model of first wall tube with thermal and mechanical boundary conditions.

stress is lower, since the curvature produces tensile stresses in the poloidal direction, and the stress state becomes closer to a hydrostatic state.

Temperatures - Uniform Heat Flux. The axisymmetric and two dimensional heat transfer analyses gave approximately the same maximum temperature of 677°C occurring at the outer perimeter and minimum temperature of 471°C at the inner wall surface. The large radius of curvature caused only a slight asymmetry in the thermal field.

Thermal Stresses - Uniform Heat Flux. The axisymmetric and the plane-stress results are significantly different. The plane-stress values are axisymmetric within the tube cross section and show good agreement with the exact results. The maximum equivalent stress is 129 MPa, which occurs at the points of the highest temperature, while the minimum stress is 14 MPa at the inner radius. The axisymmetric analysis showed that the highest stress component is the poloidal stress, which by assumption is set to zero in the plane-stress results. The source of these stresses arises from the condition of axisymmetry, which ensures no motion out of the plane of the cross section. This condition induces compressive out-of-plane stresses in the high-temperature zone and tensile stresses in the cold zone. Coupling between the in-plane and poloidal stresses makes the in-plane stresses higher in the axisymmetric case than in the plane stress case. Consequently, the maximum equivalent stress is 129 MPa at the outer radius, and the minimum stress is 13.8 MPa at the inner radius for the plane-stress model, with the maximum and minimum stresses being 200 MPa and 21.1 MPa, respectively, in the axisymmetric case.

Temperatures - One-Sided Heating. With less total heat transmitted to the tube, the maximum temperature and the temperature drop across the wall are lower than in the uniform-heat-flux case. The temperature contours in Fig. 8.2.-22 show asymmetry and are from the axisymmetric analysis. The two-dimensional results are very close to those given in Fig. 8.2.-22, and show the weak dependence of the thermal field on the fairly large radius of curvature. The maximum temperature of 662°C occurs at the point closest to the plasma, and the minimum temperature of 398°C is at the point farthest from the plasma at the inner radius.

Thermal Stresses - One-Sided Heating. Similar effects to those in the uniform heating can be observed. Fig. 8.2.-23 shows the equivalent stresses in the plane-stress model, Fig. 8.2.-24 gives the stress contours of the poloidal stresses and Fig. 8.2.-25 depicts the equivalent stresses resulting from the axisymmetric analysis. The asymmetry of the temperature field causes

TEMPERATURE CONTOURS (K)

C=680	F=800
D=720	G=840
E=760	H=880
MN=671	I=920
MX=935	

Fig. 8.2.-22. Temperature contours in the first wall of the lithium design.

significant poloidal stresses with contours resembling the temperature contours. The maximum and minimum stresses are 88.3 MPa and 0.3 MPa in the plane-stress model, and 235 MPa and 11.5 MPa in the axisymmetric model, respectively.

While ignoring the poloidal stresses when calculating the pressure stresses does not cause significant errors and proved to be conservative, the thermal stresses develop entirely differently in a thin disk and in an axisymmetric structure. Plane stress results still give a good idea about the magnitude of the stresses but have to be treated cautiously due to their nonconservative nature. Also, the asymmetric temperature distribution due to one-sided heating raises the stress level even further.

Summary. Assuming 100 MPa allowable value for the primary stresses and 750°C maximum wall temperature for the vanadium alloy, all structural design criteria are satisfied and the current design is feasible. Future analysis will address the effect of the poloidally varying coolant temperature and possible methods to relieve some of the poloidal thermal stresses.

8.2.6.2. Structural Analysis of the Lithium Blanket

In this section the pressure stresses in the lithium blanket are calculated using the finite element method. Initially, a baseline configuration is chosen, and simplifying assumptions are made so the structure can be modeled with 2-D axisymmetric elements. Subsequently, the blanket dimensions are varied to reduce the bending stresses at the corners and to study the effects of possible design changes. Finally, the likely effects of the pending thermo-structural analysis are discussed.

In order to perform a 2-D axisymmetric analysis the toroidal curvature and poloidal break at the inlet/outlet header must be ignored. The resulting model is shown in Fig. 8.2.-26, where toroidal symmetry is assumed so only half of the blanket needs to be modeled. The analysis was performed with the ANSYS finite-element code [25], using 484 nodes and 360 elements. The baseline model dimensions and material properties are given in Table 8.2.-IX.

For the baseline module dimensions and a uniform pressure of 1.5 MPa, the maximum equivalent stress occurred on the outside surface at the center of the side plate and was 398 MPa. The major components of this stress are a hoop stress of -175 MPa and a large bending stress of -470 MPa. Obviously, this design requires modification.

Before discussing any design modifications, the baseline blanket is used to study the effect of increasing the outlet pressure to 2 MPa. In this case the

EQUIVALENT STRESS CONTOURS (MPa)

B=12.5	E=50.0
C=25.0	F=62.5
D=37.5	G=75.0
MN=0.305	MX=88.3

Fig. 8.2.-23. Equivalent stress in the first wall of the lithium design.

POLOIDAL STRESS CONTOURS (MPa)

B=-250	F=-50
C=-200	G=0
D=-150	H=50
E=-100	MX=83.9
MN=-268	

Fig. 8.2.-24. Poloidal stresses in the first wall of the lithium design.

EQUIVALENT STRESS CONTOURS (MPa)

D=40

G=160

E=80

H=200

F=120

MN=11.5

MX=235

Fig. 8.2.-25. Equivalent stresses in the first wall with axisymmetric analysis.

Table 8.2.-IX

<u>Baseline Dimensions</u>		
$R_p = 70 \text{ cm}$	$R = 30 \text{ cm}$	$W = 68$
$t_1 = 1 \text{ cm}$	$t_2 = 1 \text{ cm}$	$t_3 = 1 \text{ cm}$
$t_4 = 1 \text{ cm}$		
<u>Material Properties</u>		
$E = 140 \text{ GPa}$	$\nu = 0.36$	$S_{mt} = 100 \text{ MPa}$

peak hoop stress did not change significantly but the maximum equivalent stress increased from 398 to 539 MPa. This increase is significant, but its effects can be mitigated by decreasing the bending through selective strengthening of the structure.

To reduce both the hoop and bending stresses, the back thickness, t_1 was increased to 2 cm and the side thickness, t_4 was increased to 1.5 cm. It is desirable to keep the other thicknesses small because increased structure in the portions of the module nearest the plasma will decrease the tritium breeding ratio and the high volumetric heating in this region will increase temperatures in the thickened structure. For this selectively reinforced blanket, the peak equivalent stress of 244 MPa occurred at the plasma-side corner, and the peak hoop stress was 112 MPa. The excessive bending can be reduced by filling the corner, so minimal design modifications should yield a blanket module capable of withstanding the coolant pressure with peak equivalent stresses within the limit of 100 MPa.

The blanket module must also tolerate the nuclear heating (81 MW/m^3 at the front for the 18 MW/m^2 wall loading case). Preliminary calculations indicate that the peak temperature (rather than the thermal stress) is the limiting factor and that a 10 mm thick front section is unacceptable because of the high heating rate and poor heat transfer in such large channels. The present design approach is to attempt to reduce the coolant pressure at inlet and outlet, thus allowing thinner walls and correspondingly lower peak temperatures. The thermal analysis of this blanket is being performed.

(a)

(b)

Fig. 8.2.-26. Model used for axisymmetric blanket stress analysis.

8.2.7. Tritium

In this section the tritium inventory and permeation in the high-heat-flux components near the plasma, i.e., the first wall and divertor is discussed. Calculations of inventory and permeation are made using the DIFFUSE code [26], which is based on a one-dimensional diffusion equation with various source and boundary conditions [26]. Calculations of first-wall inventory and permeation require estimation of high-energy triton implantation from the plasma, for which the TRIPOS code was used [27]. This code estimates the implantation fraction and range distribution of charged particles impinging on a solid material. The results are used as initial input for a DIFFUSE analysis.

The vanadium alloy V-3Ti-1Si is proposed for the first-wall and blanket structure. Since experimental values of permeation constants for these alloys are not available, permeation and material data for pure vanadium is used as an approximation.

First-wall calculations require an estimate of the impinging plasma flux and energy. The first-wall tritium flux is assumed to be about $1.6 \times 10^{16} \text{ cm}^{-2} \text{ s}^{-1}$. An energy of 50 eV and an incident angle of 45 degrees is assumed, which gives a TRIPOS result of 38.8% of impinging particles retained by the vanadium first wall. The implantation profile is used as input to DIFFUSE. A thickness of 0.15 cm for both first wall and divertor is used for calculations. First-wall temperatures of 700°C toward the plasma side and 500°C toward the blanket side near the lithium outlet are used. The lithium temperature is set at 600°C at the outlet and 400°C at the inlet. DIFFUSE allows the use of several boundary conditions. As was used in BCSS [6] for first-wall inventory and permeation calculations, a boundary condition of tritium diatomic molecular recombination at the surface is used. A coefficient used to reflect the ease of recombination is required by DIFFUSE: a value of 1 represents fast recombination (i.e., a clean surface), while lower values represent rougher surfaces. With a sputter-cleaned inner surface and an outer surface in contact with the lithium (with its large affinity for hydrogen isotopes), recombination coefficients of 1 are used for both surfaces, as was assumed in BCSS. All DIFFUSE runs simulate steady-state values after 30 days of continuous reactor operation.

At the first wall near the lithium inlet, a permeation rate of 47 g/d (scaled up to the total first wall area of 103 m^2) and a first wall inventory of 0.16 wppm is computed. Near the hotter lithium outlet, a total first-wall permeation rate of 44 g/d and inventory of 90 wppb is calculated. Thus, the

first wall will have a net permeation rate of approximately 45 g/d of tritium into the coolant. With the first wall represented as a sheet of area 103 m^2 and thickness 1.5 mm, the total first-wall inventory will be between 0.08 and 0.15 g. Actual volumetric first-wall calculations (considering the geometry of the first-wall pipes) might double this inventory.

For divertor calculations, the plasma flux at the plates is taken as $2 \times 10^{20} \text{ cm}^{-2} \text{ s}^{-1}$ and the particle energy as 75 eV (after acceleration through the sheath potential). For an incident angle of 45 degrees, TRIPOS indicates 42.2% of the impinging tritons are retained by the divertor. For a 1.5 mm thick divertor plate with 1 m^2 area, front and back temperatures of 700 and 500°C give a permeation rate to the coolant of 62 g/d and an inventory of 9.0 wppm (0.08 g) while a temperature profile of 600 to 400 °C gives 83 g/d permeation and 14 wppm (0.13 g) inventory. Since vanadium has a negative heat of diffusion, higher temperatures actually inhibit tritium diffusion, an advantageous effect in high temperature operation. Future design evolution and refinements of the flux and divertor models will alter the first estimates given above, particularly when the edge-plasma and divertor-plate conditions become better resolved.

To evaluate permeation through the outer shell of the lithium-containing structure, a Sievert's law boundary condition on the lithium-side is used in DIFFUSE; this condition considers solubility effects arising from an ambient gas pressure. Shell temperatures of 600°C toward the plasma and 650°C away are used. A tritium partial pressure is used typical of that for a lithium system containing about one wppm tritium at 600°C (i.e., about $2.9 \times 10^{-12} \text{ atm}$ [28]). The outer side of the shell employs a recombination boundary condition with an assumed recombination coefficient of 0.01. With vanadium known to oxidize to vanadium pentoxide, an outer permeation-resistant oxide layer is assumed. For an oxide layer resulting from a water-cooled first wall, BCSS assumes a low recombination coefficient of 5×10^{-5} . For a shell area of 190 m^2 and a thickness of 0.5 cm, a permeation rate of 0.047 g/d and a total shell inventory of 0.031 g tritium (5.4 wppb) are obtained. Varying the recombination coefficient does not significantly change the inventory, but the permeation rate does vary proportionately, (i.e., 0.73 g/d permeation for a coefficient of one and $5.1 \times 10^{-4} \text{ g/d}$ for a coefficient of 0.0001).

For blanket piping of 0.15 m diameter, a wall thickness of 3.0 mm and surface area of 400 m^2 (200 m^2 for the hot leg and 200 m^2 for cold leg) are assumed. Two temperature profiles are assumed: 550°C wall temperature for the hot leg and 300°C for the cold leg. As for the shell, Sievert's law is used for

the inside boundary condition and recombination outside (coefficient of 0.01). The hot-leg temperature gives a permeation rate of 0.61 g/d and average pipe inventory of 3.9 wppb (0.014 g), while the cold-leg conditions give a permeation rate of 2.7×10^{-3} g/d and an average inventory of 1.5 wppb (0.005 g). Total permeation of 0.61 g/d and inventory of 0.02 g can be expected for this piping. It should be noted that the effects due to the thermal insulation on the pipe and outer metal sheaths were not taken into account. These additional permeation barriers should reduce the net tritium leakage as was found in the MARS design [16].

In summary, first wall and divertor permeation rates of more than 100 g/d can be expected. Fortunately the tritium will pass into the lithium coolant, where it is recoverable with the bred tritium, mitigating permeation hazards but increasing extraction requirements. Permeation through the shell into the shield region may be 0.05 g/d, and permeation through blanket piping may be as much as 0.6 g. Total inventories within these structures should be less than 1 g. A greater concern may be long-term permeation through the shell and piping, with contamination of shielding and pipe insulation and subsequent release to the reactor hall environment. This possibility requires further study. All numerical results are tentative given further design advance and/or changes.

8.2.8. Conclusions

The preliminary engineering studies have shown the lithium-vanadium design to be feasible for the compact RFP reactor. Main features of the design are as follows:

1. Single heat transfer medium for first wall, blanket and shield. High-velocity liquid lithium appears adequate to cool the first wall with surface heating rates up to 4.5 MW/m^2 . The favorable magnetic-field topology of the RFP reactor allows the use of high-velocity liquid-metal coolant without severe MHD problems.
2. Use of the vanadium alloy V-3Ti-1Si shows good resistance to the effects of neutron irradiation, in particular to helium embrittlement, relative to V-15Cr-5Ti.
3. Tritium breeding ratio ≥ 1.2 is easily achieved and with further

neutronics optimization the blanket energy multiplication can be increased from its present value of 1.21 to a value optimized for this design.

4. Because of the low toroidal-field requirements of the RFP reactor, the IBC concept is readily adapted to this configuration for both the toroidal-field coils and the divertor coils. The power-supply and bussing requirements do not appear to present an insurmountable problem and several options are being investigated.
5. The maximum vertical lift required during maintenance is ~300 tonnes which is well within the limits set for single-piece maintenance (Sec. 7).

Engineering analysis of this design has continued beyond the initial scoping phase and the design has evolved greatly from the results reported herein. Several key areas have been identified where further detailed engineering analysis is required to evaluate the attractiveness of this overall design. Efforts are continuing in the areas of single-piece maintenance and time-motion studies, tritium removal and processing systems, first wall, blanket and shield optimization, safety analysis of the consequences of off-normal events, and evaluation of the engineering requirements for start-up.

8.3. AQUEOUS LOOP-IN-POOL BLANKET DESIGN

In the early period of the scoping phase of this study, the safety advantages of the pool-type concept, introduced by D. K. Sze [29], and the aqueous-solution lithium breeder concept identified by D. Steiner [30], were recognized. Also the basic safety design principle of the Swedish PIUS fission reactor design [31], which proposes to submerge the hot-water loop of a PWR in a cold pool of water, appears attractive. Based on these designs, an aqueous "loop-in-pool" design emerged as part of the TITAN study.

Both the hot-water loop and the cold-water pool are at the high pressure of 9 MPa in the PIUS design. The pressure would be contained in a pre-stressed Concrete Reactor Vessel (PCRV). This pressurized "loop-in-pool" approach is essential for PIUS, since the fast introduction of borated water from the cold pool into the hot loop is necessary for emergency reactor shutdown. The loop

and pool mass transfer is controlled by density-gradient valves. The proper functioning of these density valves is a design issue that has received criticism from the fission industry. Another criticism for the PIUS reactor is the size of the PCRV, which is defined by the submersion of the fission core and primary heat exchangers. These requirements would not apply to the TITAN aqueous loop-in-pool blanket design, since fast introduction of the water from the cold pool to the hot loop is not necessary. Furthermore, an atmospheric pressure pool rather than a pressurized pool was chosen for the aqueous blanket design; density valves and construction of a large high pressure pool, therefore, will not be needed. The cold pool fulfills the functions of diluting the thermal and afterheat energy, thereby providing assurance against a loss of coolant accident and controlling the concentration of radioactivity, mainly tritium, under a loss-of-coolant-accident (LOCA) condition.

8.3.1. Concept Description

Figure 8.3.-1 shows the design approach. The high-pressure primary loop, including the torus and heat exchangers operating at 15.8 MPa, is submerged in a water pool at 0.1 MPa (atmospheric pressure). The reactor design characteristics are given in Table 8.3.-I. The parameters of the power-conversion loop were selected to be those of the advanced PWR design [32] being

TABLE 8.3.-I
DESIGN CHARACTERISTICS OF THE AQUEOUS BLANKET DESIGN

Major radius (m)	3.9
Minor first wall radius (m)	0.65
Neutron wall loading (MW/m ²)	20.0
Surface heat loading (MW/m ²)	5.0
Thermal power (MW)	2,948.
Net electric power (MW)	1,000.
Tritium breeder	LiNO ₂ or LiNO ₃
Neutron multiplier	Be
Tritium breeding ratio	1.25
Blanket energy multiplication	1.39
Coolant	water at 15.8 MPa
Inlet temperature (°C)	291.
Outlet temperature (°C)	326.
First wall material	Cu-Al25
Structural material	PCA or HT-9
First wall thickness (mm)	1.5
First wall temperature, peak (°C)	425.
Gross thermal efficiency	35%

Fig. B.3.-1. Schematic plant layout for TITAN aqueous blanket design.

constructed in Germany. To breed tritium, a lithium compound (LiNO_2 or LiNO_3) is dissolved into the hot-water loop.

As shown in Fig. 8.3.-2, the coolant at 291°C enters the bottom of the torus and exits at the top. The inboard and outboard channels are physically separated, as is illustrated in Fig. 8.3.-3, to satisfy the physics requirement that there be a poloidal non-conducting break in the first wall and blanket surrounding the plasma.

To make a thin first wall with high heat flux removal capability, a copper-alloy lobe construction is used, where the main structure supporting the vacuum wall is some distance behind that wall, as is shown in Fig. 8.3.-4. Between this main structure and the first wall is an open poloidal channel to permit high-speed coolant flow; immediately behind this channel is a 0.1 m thick zone containing beryllium for neutron multiplication. The lobes balance lateral pressure forces against each other and are tied back to washer-like poloidal rings made of stainless steel, which in turn take the short-direction compression.

The torus is divided into four quadrants by divertor chambers, which interrupt the lobed surface, effectively dividing the first wall and blanket into four separate pressurized assemblies. The back of these assemblies is not a pressure shell, but is only a sealing membrane that butts against the hot shield, as is shown in Fig. 8.3.-5. The massive hot shield serves as the main pressure-containing structural member.

The hot shield is an assembly of stainless-steel castings with integral cooling passages and forms a structural, but non-sealing, shell that contains the blanket assembly. The four toroidal quadrants will be further split in the toroidal direction to allow installation of the blanket assembly. The large forces on these pieces make it necessary to join them with machined serrated straps, to avoid major bolting problems.

Investigations show that care is required to ensure that the poloidal stainless steel rings are sufficiently rigid to inhibit the development of toroidal compressive stresses in the first-wall lobes. One of the possibilities considered for improving the design is to bolt the washer plates to the hot shield. This arrangement would assist the task of jointing the cast hot shield and also reduce the first wall toroidal stress. These bolts would require neither penetration of the blanket pressure boundary nor great precision in the shield.

Fig. 8.3.-2. The torus and heat exchanger of the TITAN aqueous blanket design.

a. TOP VIEW, DISTRIBUTOR SCHEMATIC

b. SIDE VIEW, INBOARD / OUTBOARD MODULES

Fig. 8.3.-3. Coolant flow distribution in the aqueous blanket design.

Fig. 8.3.-4. Aqueous blanket first wall and breeder structure.

Fig. 8.3.-5. Torus structure of the aqueous blanket design.

8.3.2. Materials

8.3.2.1. Breeding Materials

Breeding in the TITAN aqueous blanket concept is accomplished by dissolving 5 to 10 wt.% ^6Li in the water circulating through the first wall and blanket. A summary of solubility limits of various lithium compounds is given in Table 8.3.-II. Short-lived radionuclides of Cl, I, and Br would increase shielding requirements up to and including primary loop heat-exchangers. Using LiOH would avoid this problem, but LiOH has a relatively low solubility and increases the pH value of the solution. Copper tends to form a corrosion inhibiting oxide layer in water. Since the stability of this oxide layer is a function of temperature and the pH value of the solution, as is shown in Fig. 8.3.-6, LiOH cannot be considered as a primary breeding candidate. The most promising candidates are lithium compounds containing N and C. However, ^{14}C is produced from these compounds, and the design implications of this radionuclide needs to be studied.

8.3.2.2. Structural Materials

The structural materials proposed for the TITAN aqueous blanket concept are combinations of Cu-alloys and/or vanadium alloys with PCA. Experimental results have shown good compatibility between water and V-15Cr-5Ti, mainly as a result

TABLE 8.3.-II
CANDIDATE LITHIUM COMPOUNDS

<u>Compound</u>	<u>Solubility limits (at.%)</u>	<u>pH</u>	<u>Remarks</u>
LiCl	7.2 at 160°C		Potential problems of induced radio-activity
LiBr	4.6 at 100°C		
LiI	3.6 at 120°C		
LiNO ₂	6.4 at 99°C	~7.25	Most promising
LiNO ₃	4.5 at 71°C	~7	Promising
LiCHO ₂	5.1 at 120°C	~7.4	Promising
LiC ₂ H ₃ O ₂	4.3 at 158°C	~7.9	Promising
LiOH	3.4 at 322°C	~14.8	Too basic

of the formation of protective Cr_2O_3 and rutile (TiO_2) layers [33,34]. The stability of these layers is sensitive to the coolant pH level; the minimum solubility of Cr_2O_3 occurring at a pH of approximately 7 [35]. Fluctuation of the pH level above or below 7 must be suppressed to avoid breakdown of the protective layer followed by further oxidation. Other vanadium-based alloys (V-20Ti and VANSTAR-7) have shown unacceptable corrosion levels in 288 °C water [34], while the V-15Cr-5Ti (the vanadium alloy most compatible with water) has less desirable radiation resistance behavior [36] compared with V-3Ti-1Si and VANSTAR-7 (Sec. 8.2). The use of copper alloys as a first-wall and structural material was therefore investigated.

Copper is known to be corrosion resistant in pure water because of the formation of a CuO layer. The stability diagram of CuO (Fig. 8.3.-6) shows a minimum solubility for a pH of 9 at 25°C. Increasing the temperature to 300°C would reduce the pH level of water by about unity. Furthermore, it is apparent from Table 8.3.-II that a solution of a lithium compound containing nitrogen or carbon will increase the pH level only slightly above 7. To assure a pH value of about 9, controlled amounts of LiOH could be added to the solution.

The question of candidate Cu-alloys for fusion applications has been investigated in detail [37-44]. The effect of radiation on mechanical and physical properties has been documented up to 63 dpa at 450 °C by Brager et. al. [37]. The most promising candidate alloys considered in this study are Cu-Al25 (Cu, 0.25 at.% Al_2O_3), Cu-Be (Cu, 2.0 at.% Be), and MZC (Cu, 0.025 at.% Mg, 0.1 at.% Zr, 0.9 at.% Cr). Irradiation data up to 10^{23} neutrons/cm² ($E > 0.1$ MeV) show low swelling ($< 0.3\%$) for all candidates (up to 63 dpa at 450°C). Experiments on yield strength at 16 dpa and 450°C [38] show a modest decrease in candidate Cu-alloys (see Fig. 8.3.-7).

The hard neutron spectrum at the first wall produces Ni and Zn from transmutation reactions in Cu. Rough estimates for 20 MW/m² neutron wall loading using Butterworth's neutronics results [39] indicate the production of approximately 3.9 wt.% Ni and approximately 0.8 wt.% Zn. Figure 8.3.-8 shows the effects of the Ni and Zn contents on the thermal conductivity of copper. Although the conductivity drops from about 350 W/m K to about 100 W/m K with a 5 wt.% Ni content, it is still approximately 3 times higher than that of HT-9.

The thermal conductivity can also be estimated from the electrical conductivity using the Wiedemann-Franz relationship ($k\rho/T = \text{constant}$ where k is the thermal conductivity, ρ is the electrical resistivity, and T is the absolute temperature). Experimental data [37] up to 63 dpa are available as shown on

Fig. 8.3.-6. Influence of pH on the solubility of CuO and $Cu(OH)_2$, at $25^\circ C$ [35].

Fig. 8.3.-7. Yield strength behavior of commercial copper alloys irradiated at 450 °C to 16 dpa [38].

Fig. 8.3.-8. Effect of nickel and zinc contents on the room temperature thermal conductivity of copper [39].

Fig. 8.3.-9. Effect of neutron irradiation on the electrical conductivity of commercial copper alloys at 450 °C to 63 dpa [37].

Fig. 8.3.-9. Cu-Al25 shows a better electrical conductivity behavior than Cu-Be.

The potential material compatibility problems related to electrolytic and radiolytic effects in the presence of solution of Li-compounds, Cu-alloy, Be-metal and PCA in the blanket module at elevated temperature will need to be addressed in the future.

In summary, copper corrosion levels in water are minimized at a pH of approximately 9. Solutions of lithium seem possible from a corrosion view point. The alloy Cu-Al25 appears to be a good choice for a high-conductivity, high-strength, and irradiation resistant material. At 450°C and 63 dpa this alloy shows: (1) low swelling (approximately 0.3%), (2) a small drop in yield strength (approximately 90 MPa), and (3) a acceptable loss of electrical conductivity. Further investigation would require examination of (1) the electrolysis and radiolysis of water and consequent effects on the mechanical and physical properties of Cu-alloys, (2) the effects of the electromagnetic environment on a Cu-PCA-water system, and (3) the buildup of ^{14}C in the coolant loop.

8.3.3. Neutronics

The neutronics calculations were performed using both a deterministic transport code, ANISN [1], and Monte Carlo code, MCNP [3]. The nuclear-data libraries employed are 30-group neutron and 12-group gamma-ray MATXS5, and continuous energy RMCCS coupled neutron-gamma ray libraries, both processed at Los Alamos National Laboratory, based on the basic ENDF/B-V general purpose data files. The comparison of tritium breeding values calculated from these codes and nuclear-data libraries revealed large discrepancies between the results of the two calculations. These differences are attributed to the self-shielding effect in the absorption cross section in copper. Because the multigroup cross-section library processed from the MATXS5 data set is not self-shielded, it overestimated the copper absorptions. All subsequent neutronics calculations were then performed with the Monte Carlo code, MCNP, and the continuous energy nuclear data library, RMCCS. Only the results obtained from these Monte Carlo calculations are presented.

Table 8.3.-III describes the one-dimensional zones and material compositions of the neutronics model employed in the calculation for the aqueous blanket design. As shown in Table 8.3.-III, the model consists of a 20 mm first wall made of 15% Cu and 85% H_2O , a neutron multiplier zone of variable thickness (70 to 140 mm), a reflector/shield made of 15% Cu, 70% stainless steel, 15% H_2O ,

TABLE 8.3.-III

ZONES AND MATERIAL COMPOSITIONS OF THE ONE-DIMENSIONAL NEUTRONICS MODEL
FOR THE TITAN AQUEOUS BLANKET DESIGN^a

<u>Zone</u>	<u>Thickness (m)</u>	<u>Composition</u>
Plasma	0.6	Void (neutron source)
Vacuum	0.05	Void
First wall	0.02	15% Cu + 85% H ₂ O ^b
Neutron Multiplier	x (variable)	15% Cu + Be + H ₂ O ^c
Reflector/shield	0.41-x	15% Cu + 70% PCA ^d + 15% H ₂ O
TF/OH magnets	0.4	10% H ₂ O + 10% insulator + 80% Cu

^a minor-radius model in cylindrical geometry

^b 5 at. % ⁶Li is assumed in all water coolants except in the TF/OH coils

^c beryllium and water volume fractions change as desired

^d Primary Candidate Alloy: a titanium-modified stainless steel.

and a magnet zone. Beryllium is selected as the neutron multiplier because of its superior neutron multiplication compared with any other nonfissionable multipliers. The content of beryllium in this neutron multiplier zone can be determined according to design options. One of these options is to recycle beryllium because of resource considerations. This option would lead to a pellet design approach for the beryllium material and result in a 60% - 40% volume fraction distribution for the beryllium and water coolant, respectively, in the neutron multiplier zone, excluding the 15% of volume space taken up by the copper structure. The other design option for the beryllium material is a plate design which maximizes the volume fraction of beryllium in this multiplier zone. This could lead to a 80% - 20% volume fraction distribution for beryllium and water coolant, respectively. The tritium breeding ratio and nuclear heating rate of these two design options are discussed later in this section.

The tritium breeder in the TITAN aqueous blanket design is a lithium compound dissolved in the water. The ⁶Li content in water is an important parameter to determine whether the reactor system is able to breed adequately. Preliminary studies showed that a aqueous solution containing 5 at.% ⁶Li is

probably acceptable from the viewpoint of tritium breeding. For lower ${}^6\text{Li}$ concentrations it is difficult to achieve a TBR greater than 1.15 because of the large fraction of copper in this system. Many lithium compounds such as LiOH , LiNO_2 , and LiNO_3 show 3 to 7 at.% ${}^6\text{Li}$ solubilities in water at the desired operating temperatures. In the present design the solute is chosen to be LiNO_2 , on the grounds of adequate solubility and compatibility with the structural materials. A value of 5 at.% ${}^6\text{Li}$ concentration in water was used in all neutronics calculations. The tritium breeding ratio and nuclear heating rate are given in Table 8.3.-IV for different beryllium multiplier zone thicknesses and compositions.

As shown in Table 8.3.-IV, a tritium breeding ratio of 1.2 can be obtained for the beryllium pellet design with 42.5% and 42.5% beryllium and water volume fractions at a multiplier zone thickness of 0.14 m. The effective beryllium thickness is about 0.06 m. The blanket energy multiplication is 1.32 for this design. For the same thickness but a higher beryllium content of 68%, the tritium breeding ratio becomes 1.32. The blanket energy multiplication also

TABLE 8.3.-IV
TRITIUM BREEDING RATIOS AND NUCLEAR HEATING RATES IN THE AQUEOUS BLANKET
DESIGN BY VARYING NEUTRON MULTIPLIER ZONE COMPOSITION AND THICKNESS

Neutron Multiplier Zone composition	15% Cu 42.5% Be ^a 42.5% H ₂ O ^b	15% Cu 68% Be 17% H ₂ O		
Zone thickness (m)	0.14	0.07	0.14	0.10 ^c
Tritium Breeding (T per DT neutron):				
First wall	0.22	0.38	0.37	0.39
Neutron multiplier	0.80	0.51	0.68	0.51
Reflector/shield	0.18	0.22	0.27	0.35
Total TBR	1.20	1.10	1.32	1.25
Nuclear Heating (MeV per DT neutron):				
First wall	3.24			4.23
Neutron multiplier	10.87			8.13
Reflector/shield	4.47			7.31
Total blanket/shield	18.6			19.7
Blanket energy multiplication	1.32			1.39
Heating to magnets	0.02			0.02

^a 90% dense

^b 5 at. % ${}^6\text{Li}$ in H₂O for all cases

^c Reference design

increases to 1.4. The neutron-multiplier zone thickness, therefore can be reduced to optimize the beryllium inventory in the blanket while maintaining an adequate tritium breeding ratio. The tritium breeding ratios of 1.10 and 1.25 are obtained for multiplier-zone thicknesses of 0.07 and 0.10 m, respectively (Table 8.3.-IV). A 0.1 m thick neutron multiplier zone is necessary to assure a TBR of 1.2. Note that for the reference design, the aqueous blanket energy multiplication is about 1.39, of which 21% is deposited in the first wall, 41% in the neutron multiplier zone, and the balance in the reflector/shield zone. The energy deposited in the TF/OH magnets is only 0.1% of the blanket energy (0.021 MeV per DT neutron).

Because of the excellent neutron moderation in the beryllium/water blanket, the neutron flux at the TF coil, which is 0.45 m from the first wall, is $\sim 10^{21}$ n/cm²/yr ($E > 0.1$ MeV) at 20 MW/m² neutron wall loading. Based on the design lifetime limit of 4×10^{22} n/cm² for the spinel insulator in the magnet coil, the TF coil can survive for 40 FPYs.

8.3.4. Thermal Hydraulics

The TITAN aqueous blanket design shown in Figs. 8.3.-4 and 8.3.-5 consists of approximately 1400 poloidal ring units, which form the first-wall and blanket assembly. The cross section of these segments resembles a narrow U with the lobe acting as the first wall. The cooling of this lobe section could impose thermal restrictions on the concept. Calculations are performed with an assumed neutron wall load of 20 MW/m² and the surface heat flux (or, f_{RAD}) as a parameter.

Each ring unit is divided into an inboard and an outboard channel. Cooling water enters at the bottom and exits at the top of the torus; this configuration is suitable for natural circulation under loss-of-flow accident (LOFA) conditions. The water inlet temperature is set to $T_{in} = 291^\circ\text{C}$, and the outlet temperature is $T_{out} = 326^\circ\text{C}$ at a coolant pressure of 15.8 MPa similar to the KWU PWR [32]. The mass flow rate is adjusted accordingly. Because of the difference in the inboard and outboard areas, and, hence, power, the channel separation in the segments is shifted outboard to 80.6° poloidal elevation from the torus midplane, as illustrated in Fig. 8.3.-3. For the given torus aspect ratio, this geometry assures equal power at equal mass flow rate in all channels, but results in a higher pressure drop at the inboard side. As an alternative, calculations show that this difference in the pressure drop could be avoided by separating the ring segment at exactly the top and bottom

positions. The difference in inboard/outboard frictional losses then adjusts the mass flow in compliance with the temperature requirements. The former option was selected, because of the relative ease of positioning the inlet/outlet coolant pipes without disturbing the magnetic coil set, as is shown in Fig. 8.3.-3.

The location of the coolant-channel lobe which faces the plasma directly experiences the maximum local heat flux. This maximum heat flux varies between 0.24 MW/m^2 and 5.24 MW/m^2 (corresponding to f_{RAD} ranging from 0. to 1.0); the former value is from volumetric power generation. To evaluate the design, a one-dimensional heat-transfer analysis was performed for this maximum-heat-flux location at the channel outlet. The temperature difference in the wall includes a minor contribution for the volumetric heat production in copper, 160 MW/m^3 at 20 MW/m^2 neutron wall loading.

Where applicable, a standard Nusselt correlation is used for convective heat transfer. High heat fluxes, however, are likely to lead to subcooled flow boiling (SFB) heat transfer. This mode change substantially improves the water-side heat transfer. With the above-mentioned coolant outlet condition, the bulk water never reaches saturation. Therefore, SFB can develop when the bulk temperature exceeds the criterion for the onset of nucleate boiling (ONB) [45]. This requires a certain overhear at the interface surface to overcome the coolant surface tension. The ONB condition is satisfied at the coolant-channel exit when the heat flux exceed about 1 MW/m^2 ($f_{\text{RAD}} > 15\%$); it applies for the whole channel when the heat flux is greater than about 3 MW/m^2 ($f_{\text{RAD}} > 55\%$). With the comparatively long hydraulic lengths of $L/D = 150$ for the inboard and $L/D = 110$ for the outboard channel, the initial SPB could transform into an annular-flow boiling. The determination of the exact regime is not well established and the subject of ongoing research [46]. This uncertainty particularly applies to the proposed design, which features a curved coolant channel with varying, non-circular cross sections and is exposed to non-uniform heat flux around the tube surface. Annular film boiling, however, shows excellent heat-transfer properties as well. Thus, it is not expected to affect considerably the above-estimated wall temperatures. The nucleate-boiling heat transfer is calculated following a procedure by Kutateladze [47], which superimposes a forced convective and a boiling term, the latter being a function of pressure and heat flux. Based on the formulation by Jens and Lottes [48], the present design operates below the critical heat flux (CHF) for the entire range of first-wall heat flux (or f_{RAD}) values.

The resulting temperature differences, the interface temperature and the maximum wall temperature are plotted in Fig. 8.3.-10. It should be noted that the SFB temperature difference refers to the saturation temperature of 346°C while the convective heat transfer refers to the bulk temperature of 326°C at the coolant channel exit. As shown in Fig. 8.3.-10, the calculated maximum wall temperature is below the 450°C limit of the proposed copper alloy.

According to Ref. 45, the pressure drop is only marginally affected by SFB. Although the coolant velocity is relatively high (minimum of 4 m/s outboard at 0.24 MW/m² heat load at $f_{\text{RAD}} = 0$, maximum of 12 m/s inboard at 5.24 MW/m² at $f_{\text{RAD}} = 1.0$), the relative pumping power for the first-wall channels remains on the order of 0.1% of the heat removed. High water speeds (~ 10 m/s) are also required in the manifolds, which have to cope with the restricted space available between the toroidal coils. Total pumping-power requirements have not been assessed, but are expected to be relatively small.

8.3.5. Structural Analysis

Because of the high water pressure and the high thermal gradients, with potential for thermal stress, stress analysis of the first wall is important for the aqueous blanket design. To calculate pressure stresses, the first-wall lobe is calculationaly lumped as a ring with a stiffness based on the cross-sectional area. The notation and the coordinate system are shown on Fig. 8.3.-11. Neglecting the stresses in the annulus along the y direction (i.e., plane-stress condition), the general solution for the displacement in the supporting annulus in the x direction is given by,

$$U = C_1 + \frac{C_2}{x}, \quad (8.3.-1)$$

where C_1 and C_2 are solution constants.

By imposing a continuity condition between the ring and the annulus at the inner radius and by prescribing the radial stress at the outer radius, C_1 and C_2 can be determined, and stresses can be estimated in the annulus. The displacement at the inner radius is calculated and imposed on the finite-element model of the first-wall lobe as is shown on Fig. 8.3.-12.

Since a uniform temperature does not cause stresses, thermal strains leading to thermal stresses are calculated using the coolant bulk temperature as

Fig. 8.3.-10. Temperature differences across first wall, water-wall interface temperature, and maximum wall temperature as a function of the first wall heat flux or the radiation fraction, f_{RAD} (CONV: convective heat transfer; SFB: Subcooled flow boiling heat transfer).

Plasma

Fig. 8.3.-11. First wall structural analysis model for the aqueous blanket.

Mechanical Boundary Conditions for Pressure Stresses

Mechanical Boundary Conditions for Thermal Stresses

Fig. 8.3.-12. Boundary conditions for the stress calculation for the aqueous blanket design.

the reference temperature. Also, in calculating thermal stresses, the displacement boundary condition at the junction specifies no motion in the x direction rather than the displacement found calculating the pressure stresses. Heat-transfer boundary conditions are shown in Fig. 8.3.-13.

It is assumed for the buckling analysis that the first-wall lobes do not add significant resistance against buckling, thereby requiring that the annuli be stable under the pressure load. Two possible modes of buckling failure are a) collapse by circumferential creasing or b) an axisymmetric buckling mode when displacements perpendicular to the plane of the annulus become unstable. The critical loads for these modes are given by Timoshenko [49]. The axisymmetric buckling mode model, however, is overly conservative for the present design, since this model ignores the inward forces acting on the inner edge.

Pressure and thermal stresses in the inner first-wall poloidal ring structure are obtained by varying the thickness of the poloidal ring. When the displacement is artificially constrained at the junction of the stiffening ring, pressure stresses are marginally acceptable based on the 100 MPa allowable design limit for the Cu alloy. More realistic modeling accounting for the flexibility of the plates, however, will yield higher pressure stresses. The equivalent pressure stresses can be reduced to acceptable levels by simply increasing the poloidal ring radial dimension (Fig. 8.3.-14) and/or by connecting the first-wall structural poloidal rings to the back structure and prestressing, as is shown in Fig. 8.3.-15. The first-wall thermal stress was found to be acceptable even with high surface heat flux of 5.24 MW/m^2 ($f_{\text{RAD}} = 1.0$).

8.3.6. Tritium

Most blanket concepts seek to minimize the leakage of tritium into the water coolant because of the difficulty of isotopically separating small amounts of tritium. Tritium recovery from water, however, is a well-established technology, although the TITAN aqueous blanket design would demand an increased capacity from present systems. This situation is in contrast to proposed tritium-recovery systems for other blanket concepts, where the extraction technology is at best experimental, the demonstrated scale is orders of magnitude below that required for a fusion device, and the costing and attractiveness is correspondingly uncertain.

For the TITAN aqueous blanket design, tritium is extracted from water by first transferring the tritium into a hydrogen-gas flow, and then isotopically

Heat Transfer Boundary Conditions

Fig. 8.3.-13. Boundary conditions for the heat transfer calculation for the aqueous blanket design.

Dimensions: $a = 0.65$ m
 $b = 0.72$ m
 $w = 0.0015$ m
 $t_1 = 0.002$ m
 $t_2 = 0.0065$ m
 $r = 0.01$ m

Load $p = 15.8$ MPa

$p_{pre} = 0$ MPa

c - Dimension from the plasma axis to the back of the first wall

Fig. 8.3.-14. Sensitivity of the structural response to changes in the dimension of the first wall and blanket segment.

Dimensions:

$$a = 0.65 \text{ m}$$

$$\text{Load } p = 15.8 \text{ MPa}$$

$$b = 0.72 \text{ m}$$

$$c = 1.0 \text{ m}$$

$$w = 0.0015 \text{ m}$$

$$t_1 = 0.002 \text{ m}$$

$$t_2 = 0.0065 \text{ m}$$

$$r = 0.01 \text{ m}$$

Fig. 8.3.-15. Effect of prestressing on the stress levels which develop in the inner first wall poloidal ring structure.

separating the tritium. The preferred process depends on the tritium level, the water feed rate, and whether the feed stream is light or heavy water. For reactor systems, the options include pre-enrichment by Water Distillation (WD), followed by Vapor Phase Catalytic Exchange (VPCE), Liquid-Phase Catalytic Exchange (LPCE), Direct Electrolysis (DE), or a new combination called Tritiated Water Upgrading Process (TWUP) [50]. The isotope separation would best be accomplished by Cryogenic Distillation (CD). Some energy for electrolysis could be recovered by recombining the detritiated hydrogen with oxygen in a Fuel Cell (FC), or by selling the hydrogen as chemical feedstock.

For the 1000 MWe(net) TITAN reactor, the tritium production rate is about 120 kg/yr for an 80% availability. For average tritium levels in the water coolant of 10-20 Ci/l, the water feed rate to the tritium-extraction facility would be 8000-17,000 l/hr (500-1000 kmol/hr). For comparison, maintaining 0.1 Ci/l in a water-cooled solid breeder or liquid-metal blanket with 0.2-2 g/d permeation of tritium into the coolant [6,51] would require a water detritiation system with 44-440 kmol/hr water feed rate (the higher value assumes tritium permeation barriers are not successful). The plasma exhaust processing will use a CD system with a 0.1 kmol/hr throughput of DT.

Table 8.3.-V summarizes existing large-scale water isotope separation facilities. Both the VPCE and CD processes will be demonstrated on a large scale at the Darlington Tritium Removal Facility. The LPCE (and CD) process will be demonstrated on a smaller scale at the Chalk River Tritium Extraction Plant. Direct Electrolysis is a commercial technology at large feed rates without tritium and at modest feed rates with relevant tritium concentrations. Chalk River uses an electrolytic cascade process to enrich diluted reactor heavy water. These systems are not based on full electrolytic cells, in that they do not separate the oxygen and hydrogen, but they do provide experience with handling multiple cells and tritiated electrolyte. Water distillation is a mature technology at large feed rates. The Pickering and Bruce CANDU reactors have several WD-based heavy-water upgraders on-site. These systems process heavy water with tritium concentrations typical of those of the moderator or primary coolant. The Bruce plant produces heavy water from light water using WD. Although it does not contain tritium, the carrier/exchange gas is H_2S , which requires leak-tight design because of its chemical toxicity. Several fuel-cell demonstration units of 1-5 MWe capacity are in operation.

Several process combinations have been considered for TITAN, assuming either heavy or light-water coolant. A detailed description of these options is

TABLE 8.3.-V
PARAMETERS OF VARIOUS LARGE WATER PROCESSING SYSTEMS

<u>Site</u>	<u>Feed fluid</u>	<u>Product fluid</u>	<u>Pro- Process</u>	<u>Feed rate (kmol/hr)</u>	<u>Startup date</u>
Darlington (Canada)	D ₂ O	T ₂	VPCE/CD	18	1987
Grenoble (France)	D ₂ O	T ₂	VPCE/CD	1	1972
Chalk River (Canada)	D ₂ O	T ₂	LPCE/CD	1	1987
Mound Lab (US)	H ₂ O	T ₂	CECE/CD	0.06	1986
Chalk River (Canada)	D ₂ O	D ₂	DE	0.13	1985
Chalk River (Canada)	D ₂ O/H ₂ O	D ₂ O	Electrolytic cascade	3-30	1950
Aswan Dam (Egypt)	H ₂ O	H ₂	DE	1700	1984
Norsk Hydro (Norway)	H ₂ O	H ₂	DE	180	1950
Pickering (Canada)	D ₂ O/H ₂ O	D ₂ O	WD	1-6	1972
Bruce (Canada)	H ₂ O	D ₂ O	H ₂ S Exchange	110,000	1975
Bruce (Canada)	H ₂ O	D ₂ O	WD	20	1975
Los Alamos (US)	DT	T ₂	CD	0.02	1985
Tokyo (Japan)	H ₂	H ₂ O	FC	150	1984

not included here. However, the direct installed cost for the tritium-extraction hardware for the aqueous blanket design has been estimated at between 80 and 150 M\$ US (1986), depending on the particular cost and the assumed cost scaling. The corresponding energy requirement would include low-grade steam and electricity. The electricity consumption is generally larger and also more important, since low-grade steam can be readily available in a power reactor. Electric-power consumption is largest in electrolysis and cryogenic-distillation systems. A DE front end with no pre-enrichment for TITAN would require about 100 MWe. This energy could be partially recovered in fuel cells, or the hydrogen could be sold. Other alternatives that use VPCE, LPCE or WD front-end processes, alone or to enrich the feed stream before electrolysis, lead to overall system electricity requirements on the order of 30-80 MWe for TITAN. Both the capital and operating costs can be reduced by operating at higher tritium concentrations, scaling roughly as the square root of the tritium concentration. These cost estimates do not include advantages from integrating

the water-coolant system with others, particularly the waste-water treatment, plasma-fuel processing, and cryogenic refrigeration systems.

Control of the tritiated water coolant would be important, but only one part of the overall occupational and public safety aspects of the fusion device. Considerable nuclear utility operating experience with such tritium levels is available [52]. For example, the Pickering Nuclear Generating Station uses eight 550 MWe CANDU reactors. The heavy-water moderator (70°C and 0.1 MPa) contains 20-30 Ci/l tritium, while the heavy-water primary coolant (300°C and 10 MPa) contains 1.5-2 Ci/l. The total tritium inventory in the station is about 4 kg, and is located primarily in the moderator. This inventory is larger than in TITAN aqueous blanket. However, the total tritium-release rate from this multi-unit power station has averaged only 50 Ci/d in air and 50 Ci/d in water over the past several years. These tritium release rates are comparable to those envisaged as being allowable for a fusion facility.

In CANDU reactors, it is observed that a small number of components contribute the bulk of the leakage, and that leakage is related more to the component mechanical environment (e.g., closeness to pump vibrations, constantly cycling valves) than to temperature and pressure conditions. The dominant loss is through seals, as opposed to cracks or by permeation.

Many directions are possible for reducing water leakage from present CANDU levels. The improvements are being incorporated into the new reactors, and could certainly be used in a fusion reactor. These include [52,53] the following:

- ♦ Improve component testing procedures and leak-tightness requirements
- ♦ Improve installation during construction and maintenance
- ♦ Minimize piping joints and components, use welds where possible
- ♦ Make fixing of leaks a high maintenance priority
- ♦ Use live-loading and bellows seals on valves and flanges
- ♦ Improve room sealing
- ♦ Fill/empty drums using piping, avoiding air exposure.

The TITAN aqueous blanket allows convenient external control of the blanket tritium. For example, during the initial reactor commissioning phase, the blanket could use pure water to allow testing of plasma and heat-transport systems without tritium being present in the coolant circuits. Under these conditions, the salt could be gradually added until full operating conditions

were reached. During operation, the tritium-production ratio and recovery would be unaffected by partial power operation. During maintenance, the tritiated water could be removed from the blanket to a dump tank and replaced by cleaner (and possibly unpressurized) water, thereby minimizing tritium release during blanket replacement. Tritium levels in the reactor blanket could be externally controlled at any time by reducing the amount of lithium salt to minimize production.

8.3.7. Safety

The key distinction between the TITAN aqueous blanket design and conventional fusion reactor design is the cold pool of water at atmospheric-pressure selected for the submersion of the primary coolant loop, which includes the torus and primary heat exchangers. This cold pool is designed to handle the loss of coolant accident (LOCA) by diluting the energy content of the hot loop to low enough temperature ($< 100^{\circ}\text{C}$) such that significant radioactivity release by vaporization is impossible. The Cu-alloy first wall with its vertical flow configuration enhances natural convection in case of the loss-of-flow-accident (LOFA), as is shown in Fig. 8.3.-3.

Table 8.-VI summarizes various accident scenarios for the aqueous blanket design and the means by which inherent safety can be achieved, with detailed calculations and evaluations being needed. An interesting possibility for handling first wall and blanket, as indicated in Table 8.3.-VI, is by designing a thermally leaky primary loop; i.e., by making use of the temperature difference between the hot loop (300°C .) and the cold pool (30°C). The blanket afterheat power can be transferred to the cold pool by completely natural and passive means of conduction in the cold pool. To control the desired amount ($< 5\%$) of steady state thermal power to be released, a stagnant layer of water at < 5 mm thick can be used as thermal insulation around the hot loop. Loss of high-temperature heat from the primary loop to the cold pool is wasteful, but there may be passive-safety advantages. More detailed analysis will have to be done to determine the minimum heat-loss rate during normal operation that will allow complete passive safety under accident conditions. It is estimated that at most, this will be a few percent of the full operating thermal power.

More detailed evaluations and calculations are needed to support the safety scenarios suggested in Table 8.3.-VI, but it seems that the aqueous blanket design has the potential to achieve level 2 of safety assurance.

TABLE 8.3.-VI
SAFETY SCENARIOS FOR THE AQUEOUS LOOP-IN-POOL BLANKET DESIGN

- ♦ Normal Operation
 - Active cooling.
- ♦ Loss of Primary Coolant Flow:
 - Natural circulation to primary HX (passive).
- ♦ Loss of Secondary Flow:
 - Natural circulation in secondary loop may be possible,
 - Conduction of primary loop through insulation to cold pool,
 - Natural circulation through cold pool safety loop.
- ♦ Loss of Primary and Secondary Flow:
 - Natural circulation in primary loop,
 - Natural circulation in secondary loop may be possible,
 - Conduction from primary loop through insulation to cold pool.
 - Natural circulation through cold pool safety loop.
- ♦ Loss of Secondary Coolant:
 - Conduction from primary loop through insulation to cold pool,
 - Natural circulation through cold pool safety loop.
- ♦ Loss of Primary Coolant Pressure:
 - Primary loop vents to cold pool,
 - Cold pool will reach 50 °C after mixing,
 - Cold pool can reach 100 °C after 5.8 weeks following shutdown (adiabatic),
 - Natural circulation through cold pool safety loop.
- ♦ Catastrophic Destruction of the Fusion Power Core:
 - Primary loop vents to cold pool,
 - Cold pool will reach 50 °C after mixing with hot loop,
 - Cold pool can reach 100 °C after 5.8 weeks following shutdown (adiabatic),
 - Earth as ultimate heat sink,
 - Potential release of only a fraction of hot loop tritium inventory (1.3 kg) due to transient events and diffusion.

8.3.8. Conclusions

The following features can be identified for the TITAN aqueous loop-in-pool blanket design:

1. Good combination of structural material and coolant to handle high surface heat flux of 5 MW/m^2 at steady state, because of the high thermal conductivity of Cu-alloy and the high heat flux capability of water-cooled SFB heat transfer. This feature is useful in the design of the divertor and other high-heat-flux components (i.e., divertor plate cooling).

2. Potential to achieve the Level 2 of safety assurance. The cold-water pool limits the potential release of radioactivity.
3. The technologies related to the use of Cu-alloy, PCA materials, aqueous breeder, pressurized-water power conversion, and tritium extraction, need less extrapolation from existing technologies than for most blanket designs, which can significantly impact the development cost of a commercial fusion reactor.

The power conversion performance of the aqueous blanket design measured by the product of blanket energy multiplication, M , and thermal power conversion efficiency is $1.45 \times 0.35 = 0.51$. This is quite good, despite the low power conversion system efficiency due to the good blanket energy multiplication.

Uncertainties that need to be addressed are: the irradiation properties of Cu-alloy at high fluence, the material compatibility problems of aqueous lithium coolant with blanket materials, the cost of tritium extraction, and the further development of SFB heat transfer for the TITAN first-wall geometry.

8.4. FLIBE POOL DESIGN (IPFR)

8.4.1. Concept Description

The basic configuration of a fusion reactor blanket and shield has not been changed in the past three decades. Layers of structural material, including the first and/or second wall, blanket, reflector and shield are located between the plasma and magnets. Each subsystem has its own unique function and, therefore, unique design and possibly different structural material and coolant. For most cases, each subsystem is cooled by high pressurized coolant in long, small-sized, and thin-walled tubes. Very high reliability is required by each subsystem.

During the past few years, a number of innovative ideas have been developed to remove or combine reactor systems to simplify the reactor design and to improve the attractiveness of fusion. The integral pool fusion reactor (IPFR) has been developed as part of the TPSS program [54] with a similar spirit. The basic principle is to use FLiBe (Li_2BeF_4) to serve the multiple functions of cooling, breeding and shielding. If such a system can be proved feasible, significant improvements in the simplicity of the reactor systems would result.

The safety, reliability and maintainability of the nuclear island could also be improved.

The IPFR system concept is a pool-type configuration. The fusion power core, including only the first wall and the magnets, is submerged under a molten FLiBe pool as shown in Fig. 8.4.-1. The FLiBe will fill the space between the first wall and the magnets and will provide the necessary magnet protection. As such, the FLiBe serves the multiple functions of breeding, cooling and shielding eliminating the need for a separate blanket and shield. Therefore, the only structural layer remaining between the plasma chamber and the magnets is the first wall.

The FLiBe-to-FLiBe intermediate heat exchanger (IHX) is also located in the pool. The IHX is needed for safety and tritium containment. Since the working fluid is FLiBe on both sides of the IHX, the IHX can continue to operate with small leaks. One or more pumps are also submerged in the pool to generate a FLiBe flow upward around the first wall and downward through the IHX for the purpose of heat transport. By such design, the need for a primary loop is also eliminated.

The attractive features of the IPFR concept are as follows:

1. By eliminating a separate blanket, shield and the primary loop, the cost of the system can be reduced.
2. After draining the FLiBe, the first wall is exposed. The replacement of the first wall is easy. The amount of radioactive waste to be disposed is reduced.
3. Coolant connection in the reactor are not required. The IHX is from FLiBe-to-FLiBe and, therefore, can operate with leaks. The reliability of the blanket should be high.
4. A pool-type reactor has the potential to achieve Level 2 of safety assurance.

On the other hand, first wall cooling, corrosion, and tritium breeding, extraction, and containment are some of the critical issues for this concept.

Fig. 8.4.-1. The FLiBe pool concept.

8.4.2. Materials

FLiBe is chosen as the tritium breeding material because of its low induced activation, low electrical conductivity, high-temperature stability and inertness toward air and water. However, some critical issues exist for using FLiBe. It has been generally accepted that FLiBe will not breed without additional neutron multiplier. However, in recent calculations [1], Cheng concluded that by careful material selection, FLiBe can yield a breeding ratio of 1.2. Other critical issues associated with FLiBe are corrosion and tritium containment. It seems that either the corrosion or the tritium containment problem can be handled, but it may be difficult to solve both simultaneously. A possible solution for tritium and corrosion has been developed and presented in Sec. 8.4.4, however.

A high-nickel alloy is the structural material for Molten Salt Breeder Reactor (MSBR) [55]; it is also a logical choice for the structural material for fusion application. Because of radiation-damage, however, a high-nickel alloy cannot be used in the fusion environment (i.e., close to the plasma chamber). The high-nickel alloy can still be used outside the neutron environment, (i.e., magnet casings, IHX, etc).

The preferred first wall material requires high operating temperature, long life, low neutron absorption cross-section, and low activation. A vanadium-based alloy fulfills most of these requirements and is chosen for the first wall.

Another important issue for the FLiBe pool is the question of Be resources. The U.S. and World reserves of beryllium have been recently estimated [56] and are listed in Table 8.4.-I. For the TITAN configuration, the total beryllium inventory is 230 tonnes, which should be compared to the beryllium burnup rate of 18 kg/year for a 1000 MWe (net) fusion reactor [1]. The FLiBe in the reactor, therefore, does not need to be reprocessed; one only has to make up for the burnup rate. Comparing the Be burnup rate with the Be resources of Table 8.4.-I shows that sufficient beryllium exists in the U.S. reserve for hundreds of 1000 MWe (net) reactors for an extended period of time.

Klein has pointed out that one of the problems in estimating the beryllium reserve is caused by the small demand [57]. The known reserve is disproportionally small compared to the abundance of the material. Erickson [58] reports that the potential recoverable resource for most elements in the earth's crust should approach

TABLE 8.4.-I
Be RESOURCESTITAN FLIBE POOL:

Total volume of FLiBe (m ³)	1,260.
Total mass of FLiBe (tonnes)	2,520.
FLiBe cost (M\$)	95.
Be required (tonnes)	230.

BE RESOURCES:

	U.S. Resource (tonnes)	World Resource (tonnes)
U.S. Bureau of Mines (USBM)	73,000. (320 ^a)	1,185,000. (5200 ^a)
U.S. Geological Survey (USGS)	282,000. (1200 ^a)	678,000. (3000 ^a)

^a Number of reactors that can be built with the total resource.

$$R \text{ (tonnes)} = 2.43 \times 10^6 A, \quad (8.4.-1)$$

where A is the abundance of material in the earth's crust in wppm, and R is the potential resource in tonnes. Table 8.4.-II lists the reserves and potential resources for different materials. For materials in greatest demand, the ratio is close to 1 but for beryllium the ratio is about 100. It is reasonable, therefore, to speculate that at least 10 times as much beryllium as the reserve number may be available. If this is the case, sufficient beryllium will be available for a fusion economy.

8.4.3. Thermal Hydraulics

The first-wall cooling is an inherent problem associated with the pool configuration. It is difficult to force the coolant to the first wall in such an open geometry. A finite-element analysis of heat transfer in a pool configuration with an unrestricted first wall is in progress, with some encouraging results reported [59].

A baffled first wall can also be used to enhance the first-wall cooling, as shown in Fig. 8.4.-2. The concept involves a first-wall channel with a 10 mm gap. The first-wall channel rests on a supporting structure with a flow restriction device to limit the coolant flow in the back of the blanket. The

TABLE 8.4.-II

Abundance-Reserves-Resources Relationship in U.S. and Earth Crust⁹

Element	United States			World		
	Reserve ^a (MTx10 ⁶)	Recoverable Resource Potential ^c (MTx10 ⁶)	Ratio of Potential to Reserve	Reserve ^b	Recoverable Resource Potential ^c	Ratio of Potential to Reserve
Lead	31.8	31.8	1.0	0.54	550.0	1000
Copper	77.8	122.0	1.6	200.0	2120.0	10
Zinc	31.6	198.0	6.3	81.0	3400.0	42
Silver	0.05	0.16	3.2	0.16	2.75	18
Gold	0.002	0.0086	4.1	0.011	0.15	14
Molybdenum	2.83	2.7	1.0	2.0	46.6	23
Beryllium	0.073	3.7	50.0	0.016	64.0	4000
Beryllium	0.025 ^d	3.7	148.0	0.38 ^d	64.0	170

^aU.S. Bureau of Mines (1970).^bU.S. Bureau of Mines (1970), does not include U.S. reserve.^cRecoverable resource potential = $2.45 A \times 10^6$ (abundance A expressed in g/mt).^dU.S. Bureau of Mines (1982).³

pressure balance, in this case favors coolant flow to the first wall. Table 8.4.-III lists the thermal-hydraulic parameters for the first wall region. The maximum heat flux capability of this design is about 1.3 MW/m^2 .

The design goal for TITAN is a neutron wall load of $\sim 20 \text{ MW/m}^2$. It is not clear that a FLiBe pool will be able to handle the surface heat load associated with such a high neutron wall load. It should be pointed out that the thermal conductance of a 2 mm thick wall structure is $1.3 \times 10^4 \text{ W/m}^2\text{K}$, which is comparable to the heat transfer coefficient of FLiBe. Any improvement of the heat transfer coefficient of the pool coolant, whether it is FLiBe, water or other fluids, is not effective as long as the first wall is a vanadium alloy because the heat transfer is controlled by thermal resistance of the structure.

8.4.4. Material Compatibility and Tritium Containment

The problems associated with corrosion and tritium containment may be resolved by formation of a molybdenum coating on the surfaces of the structural components. The formation of the coating has been demonstrated at ORNL by dissolving MoF_6 in FLiBe [60]. Table 8.4.-IV lists the free energy of formation of different fluoride compounds. It can be seen that MoF_6 is the most unstable of all the listed materials. Therefore, V, Ni and tritium will react with MoF_6 to form VF_4 , NiF_2 and TF, respectively. In reaction with V or Ni, the Mo is precipitated out and forms a continuous coating to prevent further reaction. If the MoF_6 concentration is carefully monitored, then this coating is self

TABLE 8.4.-III
PARAMETERS OF THE BAFFLED FIRST WALL

Surface heat flux (MW/m^2)	1.3
Volumetric heat generation (MW/m^3)	150.
First wall channel width (mm)	10.
First wall channel length (m)	2.
Coolant temperature ($^{\circ}\text{C}$)	550./600.
Coolant velocity (m/s)	2.23
Heat transfer coefficient ($\text{W/m}^2\text{K}$)	1.37×10^4
Coolant pressure drop (MPa)	0.03
Maximum interface temperature ($^{\circ}\text{C}$)	690.
First wall thickness (mm)	2.

Fig. 8.4.-2. Baffled first wall cooling scheme for FLiBe pool concept.

TABLE 8.4.-IV
FREE ENERGIES OF FORMATION OF FLUORIDES

	ΔG^f at 1000 K (kcal/g-atom of fluorine)
MoF ₆ ^a	-50.2
WF ₆ ^a	-56.8
NiF ₂ ^b	-55.3
VF ₅ ^a	-58.0
VF ₄ ^c	-66.0
HF ^a	-66.2
FeF ₂ ^b	-66.5
NbF ₅ ^a	-72.5
CrF ₂ ^b	-75.2
TaF ₅ ^a	-82.5
TiF ₄ ^a	-85.4
LiF ^d	-125.2
BeF ₂ ^d	-106.9

-
- a) Gaseous.
b) Dissolved (aqueous).
c) Crystalline.
d) Liquid.

healing. The formation of TF will greatly reduce the mobility of tritium and reduce its permeation rate.

The pressure ratio of TF to that of T₂ can be calculated by the free energy of formation and is summarized in Table 8.4.-V. If 1 ppm of MoF₆ is dissolved in FLiBe, then the partial pressure of T₂ is 10⁻⁸ Pa, and tritium permeation is no longer important. If the MoF₆ concentration is higher, the situation is more favorable. The total tritium inventory in the reference design is 6 g, which can be relaxed by a factor of 10.

TABLE 8.4.-V
TRITIUM SUMMARY

x_{MoF_6} (ppm)	1.
P_{TF} (Pa)	13.
P_{T_2} (Pa)	1.7×10^{-8}
Tritium inventory (g)	5.
Tritium leakage rate (Ci/d)	4.
Purge gas flow rate (l/s)	$\sim 10^3$

8.4.5. Safety

The combination of the material selection and the configuration of the IPFR provides this reactor concept with a large safety margin. The control of the tritium is a key safety concern with the use of FLiBe. As has been discussed earlier, the addition of MoF_6 is expected to reduce the scope of this problem considerably.

For most fusion reactor systems, thermal transients may lead to the release of considerable amounts of radioactivity from the reactor. The use of the pool configuration together with structural and breeding materials with low decay heat minimizes the potential problems associated with thermal transients. The establishment of natural convection in the blanket region, made possible by the open configuration, makes the problem of thermal transients even less threatening.

FLiBe has no exothermic reactions with water or air and is unlikely to have any significant reaction with concrete. This lack of reactivity is a distinct advantage over either lithium or $\text{Li}_{17}\text{Pb}_{83}$ -cooled blankets. Furthermore, FLiBe has low electrical conductivity, and MHD problems are eliminated. Therefore, FLiBe has the advantages associated with these liquid metal blankets (e.g., combined coolant and breeder functions) without the potential problems of the chemical reaction or MHD effects.

The degree of safety involves the level of protection which a reactor concept can provide to the public. Since the IPFR concept has no chemical or thermal transient problems, a high level of safety can be assured. In fact, there may not be any plausible pathway for radioactivity mobilization which

could be sufficient to cause any acute fatalities. Therefore, the FLiBe pool design has the potential to achieve Level 2 of safety assurance.

8.4.6. Conclusions

A unique concept of improving the attractiveness of fusion, the FLiBe pool design, was described in this section. The concept has the potential to significantly improve the safety, simplicity, reliability and maintainability of a typical fusion reactor. However, this concept is still in the development stage, feasibility issues remain, and applicability to high wall loading ($>10 \text{ MW/m}^2$) and high surface heat flux (up to 4 MW/m^2) reactors is limited by heat transfer considerations.

8.5. HELIUM-COOLED CERAMIC DESIGN (FISC)

8.5.1. Concept Description

The Fusion Inherently Safe Ceramic design (FISC) is based on the Low Activation Fusion Reactor concept pioneered by G. Hopkins [61]. The low activation concept attempts to use materials in the regions of the reactor that are exposed to a significant fluence of neutrons, that exhibit only a low level of short-lived activation if exposed to fusion neutrons. If this can be done, then significant safety, maintenance and waste management advantages may be achieved [62]. If the total hazard inventory and afterheat level can be kept low enough that no significant dose would occur at the site boundary, regardless of what happened to the physical integrity of the reactor, then many or even all of the N-stamp requirements imposed on fission reactors would be unnecessary. Avoiding these requirements could result in an approximately 30% savings in the capital cost of the plant [63].

The materials choices that will allow very low activation to be achieved are very limited. Only low-Z elements such as H, He, B, Be, C, Li, Si, O may be used in the high flux zones. This restricts the materials choices. Previous studies [61] have concluded that the most promising possibility is SiC as the structural material, solid lithium compounds for breeding tritium and helium as the coolant. The FISC design started with this concept.

The FISC concept places the fusion power core and high-pressure-helium primary heat transport loop inside a prestressed concrete reactor vessel (PCRV) filled with pressurized helium as shown in Fig. 8.5.-1. This configuration places the first wall/vacuum chamber torus under a compressive load.

Fig. 8.5.-1. Helium-cooled ceramic blanket (FISC) Design.

Furthermore, the entire primary loop is under the same compressive, load which balances the tensile load created by the high pressure, high temperature helium coolant. The result is a ceramic design with only compressive primary stresses (Fig. 8.5.-2).

The TITAN FISC design characteristics are given in Table 8.5.-I. To take advantage of the high temperature capabilities of the helium-cooled ceramic design, a Closed-Cycle Gas Turbine (CCGT) power conversion system is used. This concept has been developed for use with the advanced high temperature gas-cooled fission reactor (HTGR) and is integrally located inside the PCR. It is ideally suited for coupling to the FISC reactor [64].

For the 20 MW/m² TITAN strawman design, the vacuum chamber is of sufficiently high aspect ratio to allow fairly uniform compressive stresses to be developed. The torus, however, is divided by the four divertors, requiring that an extra load path be provided to support the major axis compression. As proposed, this configuration consists of two rings at a tangent to the top and bottom surface, as is shown in Fig. 8.5.-3, on to which the four toroidal segments can butt; an abutment face is provided on the chamber top and bottom tangent points. The butting force is 0.876 MN/m (5000 lb/in), which will require grinding of the mating surfaces. Temperature control or slitting of the ring will avoid thermal stress problems. It is proposed that the torus be composed of 0.5 m long poloidal rings (Fig. 8.5.-2). The first wall cooling passages must be incorporated in the "green" stage of the ceramic manufacturing, using a non-refractory filler which melts out in subsequent firing of the composite. Joints between these rings will require reliable SiC braze joints but brazing of SiC to a metal has been demonstrated.

The pressure-induced compressive stress on the ring is 145 MPa. The tensile stress in the axial direction that is induced by the divertor chamber is similar in magnitude in the smallest sections. Because of its shape, the divertor shell carries its load as a beam, one support being at the outside diameter and the other at the torus intersection. The supports will tend to provide some bending support, but this situation is undesirable because of the vacuum sealing requirement. The beam stiffness is such that the joints will not rotate significantly. To achieve this, the depth of the divertor beam (wall) must be at least 80 mm thick, as is shown in Fig. 8.5.-3.

Fig. 8.5.-2. FISC ceramic first wall torus which is compressively loaded.

TABLE 8.5.-I
CHARACTERISTICS OF TITAN FISC DESIGN

Major radius (m)	3.9
Minor first wall radius (m)	0.65
Neutron wall loading (MW/m^2)	20.0
Surface heat loading (MW/m^2)	1.5
Thermal power (MW)	2,351.
Net electric power (MW)	1,000.
Tritium breeder	Solid breeder
Neutron multiplier	BeO
Tritium breeding ratio	1.17
Blanket energy multiplication	1.20
Coolant	Helium at 5 MPa
Inlet temperature ($^{\circ}\text{C}$)	540.
Outlet temperature ($^{\circ}\text{C}$)	800.
First wall material	SiC
Structural material	SiC
First wall thickness (mm)	1.9 to 2.5
First wall temperature, peak ($^{\circ}\text{C}$)	1,198.
Power conversion system thermal efficiency	40%

8.5.2. Material

Desirable characteristics of ceramics used as structural material for fusion applications are: (1) high strength at high temperatures; (2) high elastic modulus; (3) electrical properties range from insulator to semiconductor and can be tailored by dopants; (4) fabrication of odd shapes can be handled; (5) low density. Table 8.5.-II summarizes some of the physical properties for monolithic SiC. On the other hand, some of the undesirable characteristics are: (1) brittle fracture; (2) unpredictable failure point (the fracture tensile strength has a wide statistical distribution); (3) failure is usually catastrophic with rapid crack propagation through the stressed region. By using a ceramic/ceramic composite, both the unpredictability and catastrophic nature of failure can be avoided. The lack of strong bonding between the fiber and the

Fig. 8.5.-3. Ceramic divertor design.

TABLE 8.5.-II
PHYSICAL PROPERTIES OF SiC AT 1000°C [65]

Melting point (i.e., decomposition, °C)	2600
Specific heat (J/kg°K)	1286
Density (Mg/m ³)	3.21
Coefficient of thermal expansion (10 ⁻⁶ /K)	4.9
Thermal conductivity (W/mK)	50
Young's Modulus (GPa)	360
Flexural strength (MPa)	500-1000

matrix forces both fiber and matrix to behave somewhat independently as far as crack propagation is concerned.

8.5.2.1. Manufacturing

The manufacturing process for SiC/SiC composite components is well developed using the chemical vapor infiltration (CVI) process. First, a preform of the component (tube, sphere, dome, torus etc.) is woven from SiC fibers. The preform is then suspended in a CVI-furnace. Chlorinate silanes (silicon source) and hydrocarbon gas (methane, ethane, propane; carbon source) are vented into the CVI-chamber. Silicon and carbon infiltrate the fiber preform and combine to create the SiC matrix imbedding the fibers. Reaction rates, bonding strength between fiber and matrix, and matrix density are controlled with furnace temperature and by using a cover gas (argon) to regulate flow rates. Matrix densities of 95% theoretical SiC density have been achieved using CVI.

The composite nature allows a high degree of predictability, which enhances reliability in structural designs. Three-point bend tests have shown that the composite fracture characteristic results from the independent fracture of fiber and matrix. The matrix cracks propagate up to the fiber and either stop or run along the fiber direction. This structural independence leads to "fiber-pullout" from the matrix upon fracture.

8.5.2.2. Composite Material Description

The NIPPON Carbon Corp. of Japan has been successful in producing long SiC fibers (NICALON). These fibers have ~12 µm diameter and can be woven

successfully into two-dimensional patterns. Shapes of performs manufactured are circular and hexagonal cross sectional (thin walled) tubes, domes, and nozzles.

Tests on the physical property of the SiC/SiC-composite material has shown that the limiting strengths were tensile not compressive (compressive strength is about 3 to 4 times that of tensile). Upon fracture, the ends of fibers extended into the matrix, which implies that some fiber-matrix frictional forces exist, which can be tailored by CVI control. Table 8.5.-III summarizes some of the physical properties measured during CVI process development [66]. Apparently, in these measurements, the ultimate bend strength of the composite is a factor of 5 to 6 lower than that of individual fibers. This difference may be due to the fact that the CVI-process is still in the development phases. Thus, improvements in CVI-processes and optimum fiber strength utilization, therefore, promise even better physical properties.

8.5.2.3. Irradiation Effects

Chemical Vapor Infiltration of SiC consists of high-density β -SiC, which tends to be stable under irradiation because of the cubic crystal structure. However, the irradiation behavior of SiC/SiC-composites is not known.

TABLE 8.5.-III
SiC/SiC COMPOSITE PHYSICAL PROPERTY DATA [66]

	Unidirectionally Wound, 1-D	Two-dimensional Braid, 2-D
<u>SiC/SiC Composite (~ 50% Fiber by volume):</u>		
Ultimate bend strength, MPa (ksi)	620 (90)	350 (50)
Elastic modulus, GPa (Mpsi)	240 (35)	240 (35)
Strain to failure	~ 1%	1-2.5%
Linear elastic limit	0.1-0.4%	0.1-0.4%
Density, Mg/m ³	2.5	2.5
<u>Nicalon SiC fibers (ceramic grade):</u>		
Tensile strength, MPa (ksi)	3290 (470)	
Elastic modulus, GPa (Mpsi)	245 (35)	
Density, Mg/m ³	2.6	

Silicon-Carbide shows a good retention of strength under irradiation. The dimensional stability under irradiation is optimum around 1000°C with a linear expansion of 0.1% or less at fluences exceeding 10^{26} n/m² [64]. The most drastic effect of fast neutron irradiation is the reduction of thermal conductivity. Several authors have measured an 80% reduction of thermal conductivity after irradiation to 2×10^{26} n/m² at 540 to 740°C [64]. Recent experiments at room temperature indicate an increase in thermal conductivity of SiC from 50 W/mK to 270 W/mK with the addition of 2 at.% BeO to the SiC. Higher temperature results for this material are also encouraging. Irradiation effects on this mixed ceramic at high fluence will be evaluated in the future [66].

Silicon Carbide is considered as a structural material for FISC because of its thermal stress resistance, strength, creep resistance, irradiation stability, commercial availability, shaping capability and tritium retention. Low activation makes this material very attractive from a maintenance and waste management point of view; and low afterheat promise the potential for a high level of safety assurance [67].

8.5.3. Thermal Hydraulics

The thermal hydraulics analysis of the FISC design has used standard heat transfer and pressure drop correlations to evaluate a suitable helium flow path and peak temperature levels throughout the first wall and blanket area. The power rating and geometry are based on a 19 MW/m^2 neutron wall load design. With a blanket energy multiplication of 1.2, the total first wall and blanket power is 2803 MWt. Helium at inlet pressure of 5 MPa and temperature of 813 K and outlet temperature of 1073 K is circulated through the torus assembly. These temperature levels were selected to achieve an efficient power conversion [68] with a He flow rate of 8123 kg/s. Four parallel modular power conversion systems, one for each torus quadrant, share this mass flow. For this calculation, LiAlO_2 was chosen as the solid breeding material, because of a high temperature capability (1000°C). In retrospect, LiAlO_2 has the drawback of relatively high afterheat and induced radioactivity. Solid breeders such as Li_2O , Li_2SiO_3 , Li_4SiO_4 and Li_2TiO_3 should be evaluated.

The cross-section of each torus quadrant is divided up into four poloidal route sectors. Figure 8.5.-4 shows the configuration and helium flow paths for one of these sectors. The coolant flows in the poloidal direction in the first wall channels as well as in the gaps between the multiplier and breeder plates. The helium flow enters radially and splits into two streams (routes A and B in

Fig. 8.5.-4). The multiplier route, A, cools the 100 mm deep breeder zone located nearest to the plasma (solid breeder zone I) on the way back to the manifold. Route B cools the first wall and returns in a parallel flow through solid breeder zone II (300 mm deep) and solid breeder zone III (400 mm deep). The high local surface heat fluxes in the rectangular coolant channels, which are integrated in the first wall, require high velocity and, therefore, cause the dominant pressure drop in first wall channels. However, combining this pathway with the low-power solid breeder zones II and III allows the pressure differential in the primary system to be held below 4% of the system pressure of 5 MPa. The pressure drops are calculated for average conditions of the respective zone (Fig. 8.5.-5 and Table 8.5.-IV). These values do not include additional losses associated with bends and turns, as well as 3-D effects related to the poloidal flow.

The one-dimensional heat transfer calculations are made at critical locations, (i.e., evaluation of peak temperatures in the first wall and the multiplier/breeder plates).³ Of primary concern is the first wall, which is exposed to a 1.5 MW/m^2 surface heat flux (corresponding to a radiation fraction of $f_{\text{RAD}} = 0.3$). An additional 0.4 MW/m^2 is added to simulate the volumetric heating in the coolant channels near the plasma. With high coolant velocity,

TABLE 8.5.-IV
THERMAL HYDRAULICS PARAMETERS OF THE FISC DESIGN

	First Wall	Multiplier	Solid Breeder		
			Zone I	Zone II	Zone III
Q (MW)	230	1028	760	331	116
Power fraction	9.3%	41.7%	30.8%	13.4%	4.7%
T _{in} (K)	813	813	946	946	946
T _{out} (K)	946	946	1073	1073	1073
m (kg/s)	333	1490	1148	500	175
$\bar{V}_{\text{He}}$ (m/s)	215.	83.2	100.	14.5	9.5
ΔP_{He} (MPa)	0.1416	0.0310	0.1086	0.0011	0.0012
k (W/m-K)	16.0	15.0	1.0	1.0	1.0
T _{limit} (K)	1475	1760	1275	1275	1275
T _{max} (K)	1466	1053	1271	1276	1274

Fig. 8 5.-4. Quarter section of FISC torus with coolant routing.

Fig. 8.5.-5. Schematic representation of helium flow path with mass flow rates, pressure drops and temperatures.

however, the heat transfer conditions can satisfy the 1475 K temperature limit for SiC-composites [68].

The second problem area is the breeder zone closest to the plasma, where the peak volumetric heat production is about two orders of magnitude higher than average value. Furthermore, the thermal conductivity of sintered LiAlO_2 plates is poor (1 W/m-K), and the maximum temperature is limited to 1000°C. To solve this problem, a design with thin plates (2 mm) and narrow gaps between the plates was chosen in order to maintain high coolant velocities and, therefore, good gas-side heat transfer. The corresponding high pressure drop is not of concern because the system pressure drop is dominated by the first wall.

Table 8.5.-IV shows that the temperatures of various components are close to or below their respective temperature limit. The pressure drops in the different cooling zones are also acceptable and match well. However, routing of the flow routing should be further optimized. The dominance of the first wall pressure drop, on the other hand, indicates that the system cannot sustain much increase in surface heat flux; the upper bound for the proposed design appears to be at a heat flux of 2 MW/m^2 .

8.5.4. Neutronics

The neutronics calculations were performed with the Monte Carlo transport code, MCNP, [3] with statistical errors for all integral quantities limited to less than 1%. The Los Alamos Recommended Monte Carlo Cross Section set was used for all calculations. This set of cross sections was processed primarily from the basic ENDF/B-V data files [2].

Figure 8.5.-6 shows the one-dimensional model used in the neutronics calculation. This model is a full-coverage cylindrical blanket model and consists of a 40 mm thick SiC first wall, a 0.2 m thick BeO neutron multiplying zone, a 0.8 m thick Li_2O tritium breeding zone, and copper magnets. Silicon carbide is used as the 5% by volume structure in both BeO and Li_2O zones. The Helium coolant takes up 50% by volume in the first wall zone and 45% by volume in both the BeO and Li_2O zones. The density factor for both BeO and Li_2O compounds is assumed to be 80% in this design. Natural lithium is employed in the Li_2O compound. The performance of this reactor system is summarized in Table 8.5.-V and Fig. 8.5.-7.

Table 8.5.-V summarizes the neutronics performance of the blanket system described above. The total tritium breeding ratio is 1.17, 8.4% of which is attributable to the ${}^7\text{Li}(n,n'\alpha)\text{T}$ reaction in lithium, 0.5% to the $\text{Be}(n,\text{T})$

TABLE 8.5.-V
NEUTRONICS PERFORMANCE OF THE HELIUM-COOLED FISC DESIGN

<u>Tritium breeding (tritons/DT neutron)</u>			
	${}^6\text{Li}(n, \alpha)\text{T}$	1.065	
	${}^7\text{Li}(n, n'\alpha)\text{T}$	0.098	
	$\text{Be}(n, \text{T})$	0.006	
	Total TBR	1.17	
<u>Nuclear Heating (MeV/DT neutron)</u>			
<u>Zone</u>	<u>Neutron Heating</u>	<u>Gamma-ray Heating</u>	<u>Total</u>
SiC (FIRST WALL)	1.22	0.56	1.78
BeO	4.49	1.56	6.05
Li_2O	7.32	1.17	8.50
Magnet	0.005	0.18	0.18
TOTAL	13.04	3.47	16.51

THICKNESS	40 MM	0.2 M	0.8 M	0.4 M
PLASMA	VACUUM (VOID)	5 % SiC 50 % BeO (80 % DENSE) + 45 % HELIUM (VOID)	5 % SiC 50 % Li_2O (80 %) 45 % HELIUM (VOID)	TF / OH MAGNETS 10 % H_2O 10 % INSULATOR 80 % COPPER
	50 % SiC + 50 % HELIUM (VOID)			

Fig. 8.5.-6. Schematic of the one-dimensional neutronics model for the helium-cooled ceramic TITAN reactor design.

reaction in beryllium, and the remainder is from the ${}^6\text{Li}(n,\alpha)\text{T}$ reaction in lithium. Also shown in Table 8.5.-V is the total nuclear heating rate in the system which is 16.5 MeV, of which about 82% of that is from neutron nuclear heating. The total recoverable heat in the blanket system will amount to about 16.9 MeV if the decay heat, estimated to be about 3.5% of the direct blanket heat, is taken into account. A blanket energy multiplication of about 1.2 results. The decay heat in this system results primarily from very short half-life radionuclides and would decay away quickly after shutdown.

It should be noted that Li_2O is used as the solid breeder in this neutronics calculation. To use the higher temperature capability of the blanket design LiAlO_2 , was later selected for the heat transfer calculation. However, with the presence of Be as the neutron multiplier, the neutronics performance of the previous design can be achieved by simply adjusting the thicknesses of the different zones in the new design.

Figure 8.5.-7 presents the volumetric nuclear heating rate as a function of distance from the first wall and is normalized to 10 MW/m^2 neutron wall loading. The nuclear heating is about 60 MW/m^3 at the SiC first wall, and decreases to 31 MW/m^3 in the BeO compound at 0.1 m from the first wall. The volumetric heating peaks in the Li_2O compound at 136 MW/m^3 immediately behind the BeO zone due to significant neutron absorption in ${}^6\text{Li}$ in the soft neutron spectrum moderated by BeO. However, the heating rate in the Li_2O zone decreases rapidly as the location moves away from the BeO zone. It is about 2 MW/m^3 at 0.4 m from the BeO zone, as shown in Fig. 8.5.-7. The maximum nuclear heating rate in the magnet is about 0.4 MW/m^3 in the location immediately behind the Li_2O zone.

Figure 8.5.-8 shows the neutron flux distribution in the BeO zone, Li_2O zone, and the copper-alloy TF magnets. It appears that the maximum fast flux in the TF coils will be no more than $2 \times 10^{20} \text{ n/cm}^2\text{-yr}$ at 10 MW/m^2 neutron wall loading. Based on the fluence limit of $4 \times 10^{22} \text{ n/cm}^2$ for spinel [16] insulator, then the lifetime of the TF coils will be about 200 years at 10 MW/m^2 wall loading, or 100 years at 20 MW/m^2 . It is noted that the thickness of the Li_2O zone in this design is not optimized. The lifetime estimate indicates that an optimization study is possible to reduce the Li_2O zone thickness and still enjoy a plant life of the TF and OH coils of about 40 years operation.

8.5.5. Structural Analysis

In this section the pressure and thermal stresses of the SiC pressure vessel are calculated using the finite element code, ANSYS [25]. The most

Fig. 8.5.-7. Volumetric nuclear heating rate in the helium-cooled ceramic blanket design (FISC).

Fig. 8.5.-8. Neutron flux ($E \geq 0.1$ MeV) distribution in the helium-cooled ceramic design. The fluxes are given at $10 MW/m^2$ neutron wall loading.

difficult aspects of the analysis are the material and structural modeling, which are complicated by the anisotropic, layered nature of the composite and by the toroidal shape of the vessel. These complications are dealt with by various means described below, providing adequate results for this initial scoping phase. Because high heat fluxes require thin walls and high pressures require relatively thick walls, the first wall stresses are crucial to the choice of the plasma-side wall thickness. To assess the viability of this concept, a preliminary design is first analyzed and subsequent modifications are incorporated as needed.

8.5.5.1. Modeling

Because of the difficulties associated with analysis of anisotropic layered structures, the results presented here are for a homogeneous, isotropic material with higher strength in compression than in tension. Because ceramic laminates incorporate many layers with fibers in different directions, this model should yield good results for the global behavior of the structure, but the possibility of delamination cannot be addressed.

The first wall is modeled as a poloidally axisymmetric ring using 2-D axisymmetric finite elements. This is coupled with analytical results for the toroidal pressure stresses to approximate the full toroidal shell. The model consists of 132 nodes and 96 quadrilateral elements, using the loadings and boundary conditions shown in Fig. 8.5.-9. It should be noted that subsequent design evolution from thermal-hydraulics analysis (Sec. 8.5.3) indicated a first wall channel dimension of 8 mm (Fig. 8.5.-2). Therefore, the results indicated here which are based on the longer span of 12 mm channels should be conservative. For the thermal analysis, a uniform heat flux is imposed on the plasma-facing surface and convective heat transfer to the coolant is included in the channel and on the shield-side surface. Uniform volumetric heating is also assumed, although it has little effect on either the temperatures or the stresses. Due to symmetry, the surfaces formed by making a poloidal "cut" are assumed to be adiabatic. For the stress analysis, the channel surfaces are pressure-loaded and toroidal symmetry is again used to establish the displacement conditions at the sides.

The material properties and strengths used in the analysis are given in Table 8.5.-VI. The high allowable strengths are typical of high-strength fiber-matrix ceramics, providing one of their major advantages. The thermal conductivity after irradiation, though, is quite low and a fairly conservative

FIRST WALL DESIGN (DIMENSIONS IN mm)

Fig. 8.5.-9. First wall structural analysis model for the helium-cooled ceramic design.

TABLE 8.5.-VI
PROPERTIES OF SiC

Young's modulus (GPa)	317
Coefficient of thermal expansion (K^{-1})	4.9×10^{-6}
Poisson's ratio	0.18
Thermal conductivity (W/m-K)	16
Design Allowable Stresses:	
σ_{comp} (MPa)	-700
σ_{tens} (MPa)	350

value of 16 W/mK has been adopted [68]. A higher conductivity would decrease the peak temperatures and stresses significantly.

8.5.5.2. Results

In all the discussion and contour plots that follow, the stresses presented are in the "z" or "hoop" direction because these are the most severe in all cases. The "severity" is determined by the likelihood to cause failure and its determination is complicated by the difference between the compressive and tensile strengths of the SiC. The failure criterion used here is a distorted Tresca criterion, which has been shown to work quite well for isotropic ceramics [69]. Equivalent stresses are not presented because they are meaningless for materials (such as SiC) that behave differently in tension and compression.

For the base design, which features a 2.5 mm thick front section, the pressure stresses (due to a pressure of 50 atm) are quite low. As shown in Fig. 8.5.-10, the peak compressive stress is about -145 MPa and the minimum is -132 MPa; there are no tensile stresses. Using shell theory [70] one can easily show that the radial stresses are negligible and the toroidal stresses are given by pa/t , where a is the first wall radius, p is the coolant pressure and t is the wall thickness. In this case, the toroidal stress is about -70 MPa and is again compressive everywhere. These pressure stress levels are within the design limits given in Table 8.5.-VI.

The thermal stresses are calculated following a heat transfer analysis which includes a 2 MW/m^2 surface heat load and a uniform volumetric heating of 122 MW/m^3 . The bulk fluid temperature used for the analysis is 673°C and the

PRESSURE STRESS CONTOURS (MPa)

B=-144

C=-142

D=-140

E=-138

MN=-145

F=-136

G=-134

H=-132

MX=-131

Fig. 8.5.-10. Pressure stresses in the FISC design.

heat transfer coefficients for the small channel and the multiplier side of the first wall are 9300 and 8180 W/m²-K, respectively. The peak temperature of 1289°C, which occurred at the plasma-side surface, is slightly above the limit. The peak compressive stresses are also too high, as shown in Fig. 8.5.-11, which shows the combined pressure and thermal hoop stresses. Unfortunately, the toroidal thermal stresses are unknown, so a complete 3-D failure analysis cannot be done. It is clear, though, that the stresses must be reduced.

In order to reduce the peak temperature and peak stresses, the front (2.5 mm thick) section between the coolant channels and the plasma-side surface is reduced to 1.9 mm. Then, the peak temperature is 1196°C and the peak compressive and tensile stresses are -723 MPa and 159 MPa, respectively. These values are close to the allowable and compressive toroidal stresses may reduce the severity of the stress state, so this design appears feasible, pending the detailed 3-D analysis and multi-axial failure assessment.

8.5.6. Tritium

In this design, the bred tritium is to be released from the solid breeder to the main helium stream and removed in the tritium extraction circuit. After diffusing out from the solid breeder, the elemental tritium is first oxidized in a CuO bed before being extracted in a molecular sieve as shown in Fig. 8.5.-12. As indicated, to reduce thermal power losses, counter-current thermal regenerators will be needed in the helium extraction circuit. By assuming a side stream extraction of 1% of the primary coolant flow, the tritium inventory in the hot helium loop was estimated at 1.6 g, which is quite acceptable. The tritium extraction characteristics are also summarized in Fig. 8.5.-12.

8.5.7. Safety

This design has the best combination of low activation blanket materials to achieve inherent safety, namely SiC, helium, BeO and solid breeder. The induced activities in the blanket are about 5 to 6 orders of magnitude lower than for corresponding metallic designs reducing the waste disposal problem considerably. The induced activities would be dominated by impurities. Even though hands-on maintenance could not be possible in the plasma chamber, however, one week after shutdown, hands-on maintenance behind the blanket should be possible. Afterheat generation from the first wall blanket would be negligible shortly after shutdown. Coupled with the high temperature capability of the selected materials, no safety problem related to the afterheat is expected.

PRESSURE PLUS THERMAL STRESS CONTOURS (MPa)

D=-800

G=-200

E=-600

H=0

F=-400

MX=177

MN=-868

Fig. 8.5.-11. Pressure plus thermal stress in the FISC design.

Fig. 8.5.-12. Schematic of tritium extraction for the TITAN FISC concept.

Based on the design of extracting the bred tritium from the main coolant stream, at 1% of the main coolant flow rate, the coolant tritium inventory was estimated to be 1.6 g which, if all released, would result in a site boundary dose of less than 0.2 rem.

The key safety-related item that needs to be addressed is routine tritium release and tritium releasable inventory from solid blanket materials. It is also important to identify the feasible low activation materials that can be used for other near torus components, (e.g., magnet coils and plasma heating devices) in order to ascertain the maintenance and waste disposal advantages of the FISC design.

8.5.9. Conclusions

The FISC design for TITAN is at an early stage of development. However, it appears to offer certain advantages:

1. This design has the best combination of materials to achieve inherent safety, since there is negligible induced afterheat from the blanket

material shortly after shutdown, and the induced radioactivities would be about 6 orders of magnitude less than for metallic structured designs. This would reduce the problem of reactor waste management considerably. Coupled with the high temperature capability of the design, no safety problem related to afterheat is expected.

2. With the compressive load design, FISC can take a neutron wall loading of 20 MW/m^2 at a first wall surface heat loading of 1.5 to 2 MW/m^2 .
3. With the closed cycle gas turbine power conversion system (CCGT), a thermal efficiency of 40% or higher can be expected.

The key development need for the FISC design is in ceramic-fiber-matrix materials. This includes areas in material development, fabrication, joining technology, material impurity controls and irradiation effects. More detailed designs are also needed to better quantify the projected safety advantages of the FISC design, and to establish the potential in high wall loading reactors. The advantages of the concept are more compelling for lower power density, lower heat flux reactors.

8.6. DIRECTIONS FOR THE DESIGN PHASE

During the scoping phase of the TITAN study a large number of design concepts and options were considered. Of particular importance are the four blanket concepts described in this section. It was decided to narrow to two the number of FPC designs to be pursued during the design phase. The decision was necessary because of inadequate resources to pursue all four designs. The decision on which of the two concepts to pursue was difficult to make. All four concepts have attractive features. The lithium-loop design promises excellent thermal performance and is one of the main concepts being developed by the blanket technology program. The water design promises excellent safety features and use of more developed technologies. The helium-cooled ceramic design offers true inherent safety and excellent thermal performance. The molten-salt pool design is the only low-pressure blanket and promises passive safety. The lithium loop concept and the aqueous loop-in-pool concept were chosen for detailed conceptual design and evaluation in the design phase of the TITAN program. The choice was made primarily on the capability to operate at high

neutron wall load and high surface heat flux. The choice not to pursue the helium-ceramic and molten-salt designs should in no way denigrate these concepts. Both concepts offer high performance and attractive features when used at lower wall loads; these concepts should be pursued in other design studies.

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9. OVERALL SUMMARY AND PROGRAM DIRECTIONS

R. W. Conn, and F. Najmabadi

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9. OVERALL SUMMARY AND PROGRAM DIRECTIONS

9.1. OVERALL SUMMARY

During the Scoping Phase, the TITAN team has succeeded in its interim objectives: to define the parameter space for a high mass power density (MPD) RFP reactor; to explore a variety of approaches to the design of major subsystems; to narrow to two major design approaches consistent with high MPD; and to reach an intermediate stage which includes preliminary engineering design and integration. The program retains a balance in its approach to investigating high MPD systems. On the one hand, parametric investigations of both subsystems and overall system performance are performed. On the other hand, more detailed analysis and engineering design and integration are performed, appropriate to determining the technical feasibility of the high MPD approach to RFP fusion reactors. Furthermore, a strong coupling and feedback between the engineering design effort and the parametric systems activity have been established. Because of this balance, we have come to refer to the TITAN effort as a "parapoint" study.

Detailed technical conclusions are given in individual sections. The physics issues for compact RFP reactors are discussed separately in Sec. 4.8. Major technical results at this interim stage can be summarized as follows:

1. Parametric system studies continue to suggest that a shallow minimum in cost of electricity (COE) versus neutron wall loading exists, extending from about 10 MW/m^2 to 20 MW/m^2 with the minimum COE at 18 MW/m^2 . Reversed-field pinch reactors with this range of neutron wall loading have MPD values well in excess of 100 kWe/tonne . The TITAN reference design at a neutron wall loading of 18 MW/m^2 has a MPD of 640 kWe/tonne and a COE of 35 mills/kWeh (constant 1986 dollars).
2. Reversed-field pinch systems with high MPD at $15\text{--}20 \text{ MW/m}^2$ neutron wall loading are physically compact systems. The cost of the fusion power core (FPC) is a small fraction of plant cost (about 7% of the direct cost), which means that small units can be used to minimize the cost of a development program as well as allowing shortfall in FPC performance goals without seriously impacting COE.

3. Single-piece maintenance of the entire reactor torus (first wall, blanket, divertor sections, with or without the shield) is feasible for high MPD systems. At 18 MW/m^2 neutron wall loading, the entire reactor torus of TITAN, drained of coolant, can be vertically lifted with a crane and replaced with a complete and pre-tested unit with a minimum amount of down time and start-up time. The full impact of single-piece maintenance and the ability to pre-test the entire reactor torus as a unit on reliability and availability is not yet determined. The shallow minimum in COE is largely a result of the assumption that the availability is not a strong function of the maintenance concept, at least at the level of single-piece versus modular approach to design and testing.
4. Reversed-field pinch experiments appear to operate well even when the dominant core plasma loss mechanism is radiation rather than conductive energy transport. This is particularly advantageous for high wall loading systems, as it distributes the plasma energy loss uniformly on the walls. For TITAN, this approach has been adopted, along with four toroidal field divertors as the particle removal system.
5. The dominance of poloidal field and weak toroidal field external to the plasma makes the RFP particularly well suited to liquid-metal cooling. One design approach being pursued uses liquid lithium as coolant and breeder and vanadium alloy (V-3Ti-1Si) as the structural material. The first wall, blanket, shield, and divertor cooling are all accomplished using lithium in this design; no other coolant is needed. This simplifies system integration and design. At 18 MW/m^2 neutron wall loading, the fluid pressure in the first wall tubes is estimated to be about 100 atm (about 10 MPa): this level is reasonable, since the stresses and pumping power requirements associated with this high pressure are modest. The coolant pressure in the blanket is much lower at about two MPa.
6. The integrated blanket-coil concept (IBC) is significantly better suited to the RFP than to the tokamak concept because of the lower value of the magnetic field that the coil must produce. The IBC is especially advantageous, perhaps uniquely so, for use as the main divertor field coil in an RFP. It can also be used to generate the toroidal field. In TITAN, the IBC has been adopted for both divertor and TF coils. When applied to

the divertor, the IBC truly improves the RFP as a reactor, whereas the advantages over a copper TF coil system are less clear. Since the copper TF coil approach appears certain to work, the TITAN team chose to vigorously pursue the TF-coil IBC approach. It is not, however, required for achieving high wall loading.

7. The aqueous "loop-in-pool" blanket has emerged as an alternative design approach for high-MPD RFP systems. This design incorporates a water-cooled copper first wall and steel structural material for blanket and shield. The cooling is achieved with a loop design. The fusion power core (FPC) as a whole is submerged, however, in a low pressure water pool to achieve level 2 passive safety. Tritium breeding is achieved using a lithium salt dissolved in the water while controlling the pH of the solution to minimize corrosion. Work on this design is less advanced within the study.

9.2. PROGRAM DIRECTIONS

During the scoping phase of the TITAN study a large number of design concepts and options were considered. Of particular importance are the four blanket concepts, reported in Sec. 8. It was decided to narrow to two the number of FPC designs to be pursued during the design phase. The decision was a necessary because of inadequate resources to pursue all four designs. The decision on which of the two concepts to pursue was difficult to make. All four concepts have attractive features. The lithium-loop design promises excellent thermal performance and is one of the main concepts being developed by the blanket technology program. The water design promises excellent safety features and use of more developed technologies. The helium-cooled ceramic design offers true inherent safety and excellent thermal performance. The molten-salt pool design is the only low-pressure blanket and promises passive safety. The lithium loop concept and the aqueous "loop-in-pool" concept were chosen for detailed conceptual design and evaluation in the design phase of the TITAN program. The choice was made primarily on the capability to operate at high neutron wall load and high surface heat flux. The choice not to pursue the helium-ceramic and molten-salt designs should in no way denigrate these concepts. Both concepts offer high performance and attractive features when used at lower wall loads; these concepts should be pursued in other design studies.

In the design phase, the TITAN program will emphasize engineering design and complete technical evaluation of the high-MPD approaches based first on the Li/V loop system and then on the aqueous "loop-in-pool" concept. About half of the duration of the design phase will be devoted to complete the Li/V design, with essentially allocating the full resources of the program during this period. Major efforts will be made to provide the technical material needed to establish engineering feasibility and the design integration. In addition, safety and environmental tasks will receive special attention; work on the plasma modeling, first-wall design, and divertor system will continue. The area of high-heat-flux components is the most difficult physics-engineering interface and will receive major attention.

Once the Li/V design has been brought to completion, the TITAN team will concentrate on establishing the feasibility and examining key issues of the aqueous blanket design. All of the major subsystem design and analysis will be addressed along with the assessment of safety and environmental impact. The technical feasibility and key issues for high-MPD RFP reactors will be established; having more than one design approach strengthens this case.

Finally, parametric studies will continue in concert and iteration with the engineering design so that a better understanding of the changes in system design in going to lower wall loadings (e.g., about 10 to 12 MW/m²), and in using high-MPD RFP systems in a development program will emerge.