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10-2-91 JS(2)

UCLA-PPG-1200

THE TITAN REVERSED-FIELD-PINCH FUSION REACTOR STUDY

**Final Report
1990**

Volume III: TITAN-I Fusion Power Core

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and Nuclear Engineering and
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ACKNOWLEDGEMENTS

The TITAN design team wishes to acknowledge the consultations and advice given by the TITAN Physics Advisory Committee on reversed-field-pinch physics issues and by the TITAN Engineering Review Committee on engineering design and analysis. Useful discussions with Frank Clinard of Los Alamos National Laboratory on properties of ceramic insulators, with John Davis of McDonnell Douglas on refractory metals, with John Haines of McDonnell Douglas on vacuum pumping issues, with Steve Piet of Idaho National Engineering Laboratory on safety aspects of the design, with Mark Tillack of University of California, Los Angeles on liquid-metal MHD issues, and with scientists of Fusion Engineering Design Center at Oak Ridge National Laboratory on design maintenance and integration are also acknowledged. The authors also wish to express their appreciation to Ms. Joan George for her effort in preparing this report.

The TITAN research program is supported by the U. S. Department of Energy, Office of Fusion Energy, at University of California, Los Angeles under grant DE-FG03-86ER52126, at General Atomics under contract DE-AC03-84ER53158, at Rensselaer Polytechnic Institute under grant DE-FG02-85ER52118, and at Los Alamos National Laboratory which is operated by the University of California for the U. S. DOE under contract W-7405-ENG-36.

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ABSTRACT

The TITAN Reversed-Field-Pinch (RFP) fusion-reactor study is a multi-institutional research effort to determine the technical feasibility and key developmental issues for an RFP fusion reactor operating at high power density, and to determine the potential economic (cost of electricity), operational (maintenance and availability), safety and environmental features of high mass-power-density fusion systems. Mass power density (MPD) is defined as the ratio of net electric output to the mass of the fusion power core (FPC). The fusion power core includes the plasma chamber, first wall, blanket, shield, magnets, and related structure.

Two different detailed designs, TITAN-I and TITAN-II, have been produced to demonstrate the possibility of multiple engineering-design approaches to high-MPD reactors. TITAN-I is a self-cooled lithium design with a vanadium-alloy structure. TITAN-II is a self-cooled aqueous loop-in-pool design with 9C ferritic steel as the structural material. Both designs would use RFP plasmas operating with essentially the same parameters. Both conceptual reactors are based on the DT fuel cycle, have a net electric output of about 1000 MWe, are compact, and have a high MPD of 800 kWe per tonne of FPC. The inherent physical characteristics of the RFP confinement concept make possible compact fusion reactors with such a high mass power density. The TITAN designs would meet the U. S. criteria for the near-surface disposal of radioactive waste (Class C, 10CFR61) and would achieve a high Level of Safety Assurance with respect to FPC damage by decay afterheat and radioactivity release caused by accidents. Very importantly, a "single-piece" FPC maintenance procedure has been worked out and appears feasible for both designs.

Parametric system studies have been used to find cost-optimized designs, to determine the parametric design window associated with each approach, and to assess the sensitivity of the designs to a wide range of physics and engineering requirements and assumptions. The design window for such compact RFP reactors would include machines with neutron wall loadings in the range of 10 – 20 MW/m² with a shallow minimum-COE at about 16 MW/m². Even though operation at the lower end of the this range of wall loading (10 – 12 MW/m²) is possible, and may be preferable, the TITAN study adopted the design point at the upper end (18 MW/m²) in order to quantify and assess the technical feasibility and physics limits for such high-MPD reactors. From this work, key physics and engineering issues central to achieving reactors with the features of TITAN-I and TITAN-II have emerged.

9. OVERVIEW OF TITAN-I DESIGN

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9. OVERVIEW OF TITAN-I DESIGN

9.1. INTRODUCTION

The TITAN research program is a multi-institutional [1] 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 economics (cost of electricity, COE), safety, environmental, and operational viewpoints.

In recent reactor studies, the compact reactor option [2-5] has been identified as one approach toward a more affordable and competitive fusion reactor. The main feature of a compact reactor is a fusion power core (FPC) with a mass power density in excess of 100 to 200 kWe/tonne. Mass power density (MPD) is defined [2] as the ratio of the net electric power to the mass of the FPC, which includes the plasma chamber, first wall, blanket, shield, magnets, and related structure. The increase in MPD 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. A compact reactor, therefore, strives toward a system with an FPC comparable in mass and volume to the heat sources of alternative fission power plants, with MPDs ranging from 500 to 1000 kWe/tonne and competitive cost of energy.

Other potential benefits for compact systems can be envisaged 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 will be less sensitive to changes in the unit cost of FPC components or the plasma performance. Moreover, since a high-MPD FPC is smaller and cheaper, a rapid development program at lower cost should be possible, changes in the FPC design will not introduce large cost penalties, and the economics of learning curves can be readily exploited throughout the plant life.

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 an RFP is the poloidal field, which is generated by the large toroidal current flowing in the plasma. This feature results in a low field at the external magnet coils, a high plasma beta, and a 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 permit the use of normal coils while joule losses remain a small fraction of the plant output. This option allows a thinner blanket and shield. In addition, the high current density in the plasma allows ohmic heating to ignition, eliminating the need for auxiliary heating equipment. Also, the RFP concept promises the possibility of efficient current-drive systems based on low-frequency oscillations of poloidal and toroidal fluxes and the theory of RFP relaxed states. The RFP confinement concept allows arbitrary aspect ratios, and the circular cross section of plasma eliminates the need for plasma shaping coils. Lastly, the higher plasma densities particularly at the edge, together with operation with a highly radiative RFP plasma, significantly reduce the divertor heat flux and erosion problems.

These inherent characteristics of the RFP [6] allow it to meet, and actually far exceed, the economic threshold MPD value of 100 kWe/tonne. As a result, the TITAN study also seeks to find potentially significant benefits and to illuminate main drawbacks of operating well above the MPD threshold of 100 kWe/tonne. The program, therefore, has chosen a minimum cost, high neutron wall loading of 18 MW/m² as the reference case in order to quantify the issue of engineering practicality of operating at high MPDs. The TITAN study has also put strong emphasis on safety and environmental features in order to determine if high-power-density reactors can be designed with a high level of safety assurance and with low-activation material to qualify for Class-C waste disposal.

An important potential benefit of operating at a very high MPD is that the small physical size and mass of a compact reactor permits the design to be made of only a few pieces and a single-piece maintenance approach will be feasible [7,8]. Single-piece maintenance refers to a procedure in which all of components that must be changed during the scheduled maintenance are replaced as a single unit, although the actual maintenance procedure may involve the movement, storage, and reinstallation of some other reactor components. In TITAN designs, the entire reactor torus is replaced as a single unit during the annual scheduled maintenance. The single-piece maintenance procedure is expected to result in the shortest period of downtime during the scheduled maintenance period because: (1) the number of connects and disconnects needed to replace components will be minimized; and (2) the installation time is much shorter because the replaced components are pretested and aligned as a single unit before commitment to service. Furthermore, recovery from unscheduled events will be more standard and rapid because complete components will be replaced and the reactor brought back on line. The repair work will then be performed outside the reactor vault.

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 and to explore a variety of approaches to major subsystems. The Design Phase focused on the conceptual engineering design of basic ideas developed during the Scoping Phase with direct input from the parametric systems analysis and with strong emphasis on safety, environmental, and operational (maintenance) issues.

Scoping Phase activities of the TITAN program were reported separately [1]. Four candidate TITAN FPCs were identified during the Scoping Phase:

1. A self-cooled, lithium-loop design with a vanadium-alloy structure;
2. An aqueous, self-cooled "loop-in-pool" design in which the entire FPC is submerged in a pool of water to achieve a high level of passive safety;
3. A self-cooled FLiBe pool design using a vanadium-alloy structure; and
4. A helium-cooled ceramic design with a solid breeder and silicon carbide structure.

Two of the above FPC designs were selected for detail evaluation during the Design Phase because of inadequate resources to pursue all four designs. The choice of which two concepts to pursue was difficult; 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-cooled design promises excellent safety features and uses 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 a high degree of passive safety. The lithium-loop (TITAN-I) and the aqueous "loop-in-pool" (TITAN-II) concepts were chosen for detailed conceptual design and evaluation in the Design Phase. The choice was based 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 future design studies.

The operating space of a compact RFP reactor has been examined using a comprehensive parametric systems model which includes the evolving state of knowledge of the physics of RFP confinement and embodies the TITAN-I and TITAN-II engineering approaches (Section 3). Two key figures of merit, the cost of electricity (COE) and mass power density (MPD), are monitored by the parametric systems model and are displayed

in Figure 9.1-1 as functions of the neutron wall loading. Figure 9.1-1 shows that the COE is relatively insensitive to wall loadings in the range of 10 to 20 MW/m², with a shallow minimum at about 19 MW/m². The MPD is found to increase monotonically with the wall load. For designs with a neutron wall load larger than about 10 MW/m², the FPC is physically small enough such that single-piece FPC maintenance is feasible. These considerations point to a design window for compact RFP reactors with neutron wall loading in the range of 10 to 20 MW/m². The TITAN-class RFP reactors in this design window have an MPD in excess of 500 kWe/tonne, and an FPC engineering power density in the range of 5 to 15 MWt/m²; these values represent improvements by factors of 10 to 30 compared with earlier fusion reactor designs. The FPC cost is a smaller portion of the total plant cost (typically about 12%) compared with 25% to 30% for earlier RFP designs [4,5]. Therefore, the unit direct cost (UDC) is less sensitive to related physics and technology uncertainties.

Figure 9.1-1. The COE and MPD as functions of neutron wall loading for the TITAN-class RFP reactors. TITAN-I (filled circle) and TITAN-II (filled squares) reference design points are also shown.

Table 9.1-I.

OPERATING PARAMETERS OF TITAN FUSION POWER CORES

	TITAN-I	TITAN-II
Major radius (m)	3.9	3.9
Minor plasma radius (m)	0.60	0.60
First wall radius (m)	0.66	0.66
Plasma current (MA)	17.8	17.8
Toroidal field on plasma surface (T)	0.36	0.36
Poloidal beta	0.23	0.23
Neutron wall load (MW/m ²)	18	18
Radiation heat flux on first wall (MW/m ²)	4.6	4.6
Primary coolant	Liquid lithium	Aqueous solution
Structural material	V-3Ti-1Si	Ferritic steel 9-C
Breeder material	Liquid lithium	LiNO ₃
Neutron multiplier	none	Be
Coolant inlet temperature (°C)	320	298
First-wall-coolant exit temperature (°C)	440	330
Blanket-coolant exit temperature (°C)	700	330
Coolant pumping power (MW)	48	49
Fusion power (MW)	2301	2290
Total thermal power (MW)	2935	3027
Net electric power (MW)	970	900
Gross efficiency	44%	35%
Net efficiency	33%	30%
Mass power density, MPD (kWe/tonne)	757	806
Cost of electricity, COE (mill/kWh)	39.7	38.0

Near-minimum-COE TITAN-I and TITAN-II design points, incorporating distinct blanket thermal-hydraulic options, materials choices, and neutronics performances have been identified in Figure 9.1-1. The major parameters of the TITAN reactors are summarized in Table 9.1-1. In order to permit a comparison, the TITAN reference design points have similar plasma parameters and wall loadings allowing for certain plasma engineering analyses to be common between the two designs.

The TITAN RFP plasma operates at steady state using oscillating-field current drive (OFCD) to maintain the 18 MA of plasma current. This scheme [9,10] utilizes the strong coupling, through the plasma relaxation process which maintains the RFP profiles [11], between the toroidal and poloidal fields and fluxes in the RFP. Detailed plasma/circuit simulations have been performed which include the effects of eddy currents induced in the FPC (Section 7). The calculated efficiency of the TITAN OFCD system is 0.3 A/W delivered to the power supply (0.8 A/W delivered to the plasma).

The impurity-control and particle-exhaust system consists of three high-recycling, toroidal-field divertors (Sections 5, 11, and 17). The TITAN designs take advantage of the beta-limited confinement observed in RFP experiments [12,13] to operate with a highly radiative core plasma, deliberately doped with a trace amount of high- Z Xe impurities (Section 5). The highly radiative plasma distributes the surface heat load uniformly on the first wall (4.6 MW/m^2). Simultaneously, the heat load on the divertor target plates is reduced to less than about 9 MW/m^2 . The ratio of impurity density to electron density in the plasma is about 10^{-4} , Z_{eff} is about 1.7, and 70% of the core plasma energy is radiated (an additional 25% of the plasma energy is radiated in the edge plasma).

The "open" magnetic geometry of the divertors (Section 4.4), together with the intensive radiative cooling, leads to a high-recycling divertor with high density and low temperature near the divertor target ($n_e \simeq 10^{21} \text{ m}^{-3}$, $T_e \simeq 5 \text{ eV}$) relative to the upstream separatrix density and temperature ($n_e \simeq 2 \times 10^{20} \text{ m}^{-3}$, $T_e \simeq 200 \text{ eV}$). The radial temperature profile is calculated to decay sharply to 2 eV near the first wall (Section 5). Negligible neutral-particle leakage from the divertor chamber to the core plasma and adequate particle exhaust are predicted. The first-wall and divertor-plate erosion rate is negligibly small because of the low plasma temperature and high density at that location.

9.2. CONFIGURATION

Detailed subsystem designs for TITAN-I FPC are given in Sections 10 through 14. The parameters of the TITAN-I reference design point, based on detailed subsystem design, are included in Appendix A and follows the DOE/OFE standard reporting format. Appendix A also includes detailed cost tables and parametric systems code predictions of subsystem parameters for comparison with DOE/OFE tables. The elevation view of the FPC is shown in Figures 9.2-1. Figures 9.2-2 and 9.2-3 show the general arrangement of the TITAN-I reactor.

One of the unique features of the TITAN-I design is that the entire FPC operates inside a vacuum tank, made possible because of the small physical size of the reactor (Figure 9.2-1). The vacuum-tank concept moves the vacuum boundary well away from the harsh radiation and thermal environment allowing for a more robust and reliable design. During maintenance of the FPC, the weld at the lid of the vacuum tank must be cut and then re-welded after the maintenance is completed. Although a design with individual vacuum ducts leading to each of the three divertor chambers was considered and is possible, remote cutting and welding of that complex geometry is expected to be much more difficult.

The TITAN-I vacuum tank also provides an additional safety barrier in the event of an off-normal incident. The entire primary-coolant system is enclosed within the containment building, which is filled with argon as a cover gas in order to reduce the probability of a lithium fire in case of major rupture of coolant pipes. Drain tanks are provided below the FPC (Figure 9.2-1) to recover and contain any lithium spilled in the vacuum tank or the reactor building. The drain-tank system connected to the vacuum tank is evacuated during normal operation. The entire primary-coolant system is located above the FPC in order to eliminate the possibility of a complete loss-of-coolant accident (Figures 9.2-1 to 9.2-3).

The TITAN plasma is ohmically heated to ignition by using a set of normal-conducting ohmic-heating (OH) coils and a bipolar flux swing. The TITAN start-up requires minimum on-site energy storage, with the start-up power directly obtained from the power grid (maximum start-up power is 500 MW). An important safety design guideline for TITAN-I allows no water inside the containment building and vacuum vessel, in order to reduce the probability of lithium-water reactions (Section 13). As a result, the OH coils are cooled by helium gas. A pair of relatively low-field superconducting equilibrium-field (EF) coils produce the necessary vertical field and a pair of small, copper EF trim coils provide the exact equilibrium during the start-up and OFCD cycles. The poloidal-field-

Figure 9.2-1. Elevation view of the TITAN-I reactor building through the reactor centerline showing the reactor vault, the maintenance crane, and the vacuum tank.

coil arrangement allows access to the complete reactor torus by removing only the upper OH-coil set.

Another unique feature of the TITAN-I design is that the divertor and the toroidal-field (TF) coils are based on the integrated-blanket-coil (IBC) concept [14]. The IBC concept utilizes the poloidally flowing lithium coolant of the blanket as the electrical conductor for the divertor and TF coils. Although lithium is about 20 times more resistive than copper, the low toroidal-field requirement of RFPs, combined with the large cross-sectional area available to the IBC, results in acceptable power requirements for TF and divertor coils. The joule heating in the TITAN-I divertor and TF coils are, respectively, 120 and 24 MW. The IBC concept reduces the need to shield the coils significantly, improves neutronics efficiency and energy recovery, reduces the number of components

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Figure 9.2-2. The TITAN-I fusion power core. The coolant supplies to the first wall are in white, to the IBC are in blue, to the shield are in red, and to the divertor-IBC are in yellow.

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Figure 9.2-3. Cut-away view of the TITAN-I fusion power core. The coolant supplies to the first wall are in white, to the IBC are in blue, to the shield are in red, and to the divertor-IBC are in yellow.

in the FPC, reduces the toroidal-field ripple, and allows direct access to the blanket and shield assemblies, thereby easing the maintenance procedure.

Poloidal cross sections of the TITAN-I FPC through a divertor module and through a blanket section are shown, respectively, in Figures 9.2-4 and 9.2-5. The geometry, size, and configuration of the first wall, blanket, shield and the associated coolant channels are established primarily by thermal-hydraulic, structural, and neutronics considerations. The dominant magnetic field at the plasma edge (or first wall) in the RFP is poloidal. Since, the TITAN-I FPC is cooled by lithium, the coolant channels in the first wall and blanket are aligned with the poloidal field to minimize the induced MHD pressure drops.

The TITAN reactors operate with a highly radiative core plasma in order to distribute the plasma heat load uniformly on the first wall. Simultaneously, the heat load on the divertor target plates is reduced to manageable levels. As a result, the first wall intercepts the radiation heat flux of about 4.6 MW/m^2 . The TITAN-I first wall is made of a bank of circular tubes. Tubular coolant channels with circular cross sections are suitable for the first wall, since a circular tube has the best heat-transfer capability (highest Nusselt number) and highest strength. In addition, tubes are easy to manufacture with small tolerances in size and wall thickness. To adjust for the shorter toroidal length on the inboard as compared to the outboard, the first-wall tubes slightly overlap on the inboard side of the torus, as is shown in Figure 9.2-6. Consequently, two sets of coolant tubes are used; one set has a slightly larger poloidal diameter than the other. The inside diameter and wall thickness of the first-wall coolant tubes are, respectively, 8 and 1.25 mm. The inside diameter of the first-wall tubes reflects a compromise between the total number of coolant tubes and the heat-transfer coefficient; reducing the diameter increases the number of tubes, which may result in a lower reliability but increases the heat-transfer coefficient. The tube wall thickness of 1.25 mm includes a 0.25-mm allowance for erosion (the first-wall erosion is estimated to be negligible).

The blanket and shield coolant channels are designed with the consideration of heat transfer, blanket energy multiplication, tritium breeding, and shielding requirements. The overall thickness of the blanket and shield is 75 cm. The 28-cm-wide IBC zone is located 1 cm behind the first wall and consists of 6 rows of tubular coolant channels with an inside diameter of 4.75 cm and a wall thickness of 2.5 mm. The primary reason for using tubular coolant channels for the IBC zone, which results in more void, is to reduce the number of load-bearing welded joints (associated with using square ducts) near the plasma. The IBC coolant channels have varying cross sections (Figure 9.2-6) in order to minimize the void fraction of this zone. As a result, the IBC zone consists of 18% structure, 72% lithium, and 10% void by volume.

Figure 9.2-4. Poloidal cross section of the TITAN-I FPC through one of the divertor modules.

Figure 9.2-5. Poloidal cross section of the TITAN-I first-wall and blanket coolant channels.

Figure 9.2-6. Horizontal, midplane cross section of the TITAN-I FPC through blanket and divertor regions.

The 45-cm-thick hot shield is located 1 cm behind the IBC and has two zones. The first zone is 30 cm thick and consists of 5 rows of square coolant channels with outer dimensions of 6 cm and a wall thickness of 5 mm. The inside corners are rounded and have a radius-to-wall-thickness ratio of unity. The structure volume fraction is 30%, the coolant volume fraction is 70%, and there is no void. The second zone of the hot shield is 15 cm thick and consists of 4 rows of rectangular channels with thick walls to increase the structure volume fraction in this zone. The structure volume fraction is 90% and the coolant volume fraction is 10%. The channels have outer dimensions of 11.25 cm by 3.75 cm and a wall thickness of 16.25 mm.

The coolant flow in both first wall and blanket are single pass and in the poloidal direction. Double-pass poloidal flow, however, is used in the hot shield. Lithium flows in through the first three square channels of the hot shield, makes a 180° turn at the inboard side, and exits through the last two square channels and the rectangular channels of the hot shield. This double-pass flow pattern allows the hot shield to be constructed of two separate units. During the annual FPC maintenance, the top half of the shield will be removed so that the torus assembly (including the first-wall, IBC, and divertor sections) can be replaced. The estimated lifetime of the shield component is four full power years and, therefore, this portion can be reinstalled after the completion of the annual maintenance.

The lifetime of the TITAN-I reactor torus (including the first wall, blanket, and divertor modules) is estimated to be in the range of 15 to 18 MWy/m² with the more conservative value of 15 MWy/m² requiring the change-out of the reactor torus on a yearly basis for operation at 18 MW/m² of neutron wall loading at 76% availability. The lifetime of the hot shield is estimated to be five years. To reduce the rad-waste, therefore, the TITAN-I hot shield is made of two pieces; the upper hot shield is removed during the maintenance procedures and then reused following replacement of the reactor torus.

9.3. MATERIALS

The advantage of vanadium-base alloys over others for fusion-reactor structural materials has been pointed out in previous publications [15,16]. In particular, when compared with ferritic-steel alloys, vanadium-base alloys exhibit better physical, mechanical, and nuclear properties. For example, compared to HT-9, vanadium-base alloys have a higher melting temperature, a lower thermal expansion coefficient, and a lower density. Furthermore, compared to ferritic alloys at 1 MW/m² of neutron wall load, vanadium-base

alloys have about one-half the nuclear-heating rate ($\sim 25 \text{ W/cm}^3$), about a third of the helium-generation rate ($\sim 57 \text{ He-appm/y}$), about half the hydrogen-production rate ($\sim 240 \text{ H-appm/y}$), and lower long-term afterheat [15].

The high melting temperature of vanadium alloys ($T_m = 1890^\circ\text{C}$) has significant bearing on safety related issues. The higher ultimate tensile strength ($\sigma_u \sim 600 \text{ MPa}$ at 600°C), the lower expansion coefficient, and the slightly higher thermal conductivity of vanadium-base alloys are reflected in higher thermal stress factors when compared to HT-9 [15]. The thermal stress factor is a measure of heat load capability. The high T_m coupled with a high thermal stress factor, promises high operating-temperature and high neutron-wall-loading capabilities. High T_m combined with a low helium-production rate is also desirable for fusion reactor materials, since below $\sim 0.5T_m$ (in K), strength and ductility are retained and fracture remains transgranular [17] ($0.5T_m$: vanadium $\simeq 1082 \text{ K}$, HT-9 $\simeq 846 \text{ K}$). Because helium embrittlement is directly related to the helium production rate, a low helium generation rate in vanadium-base alloys is a very favorable characteristic.

From the three candidate vanadium-base alloys, V-3Ti-1Si is chosen as the primary structural material for TITAN-I, primarily because of its irradiation behavior. It outperforms V-15Cr-5Ti and VANSTAR considering helium embrittlement, irradiation hardening, and swelling after exposure to a damage dose of 40 dpa by fast neutrons. However, V-3Ti-1Si has the lowest thermal-creep resistance of the three alloys (Section 10.2.1).

The effects of gaseous transmutations (*i.e.*, hydrogen and helium) on the mechanical properties of vanadium-base alloys were considered. Based on extrapolation of the limited available data to TITAN-I operating conditions, irradiation hardening and helium and hydrogen embrittlement of V-3Ti-1Si set an upper lifetime limit of approximately 18 MWy/m^2 for the TITAN-I first wall. Irradiation-induced swelling of the V-3Ti-1Si alloy was also investigated and it was concluded that swelling would be negligible for the lifetime of the TITAN-I first wall (Section 10.2.1).

The modified-minimum-commitment method (MMCM) [18] was used to extrapolate the creep-rupture data and to establish the creep behavior during normal and off-normal operating conditions (Figure 9.3-1). From the limited creep data, it appears that V-3Ti-1Si will be able to operate satisfactorily at elevated temperatures (700°C). To include the effects of the irradiation hardening, helium-embrittlement data were used to estimate the maximum allowable design stress based on a 2/3 creep-rupture-stress criterion (Section 10.2.1). Further creep-rupture experiments are needed to develop more precise creep-rupture models for V-3Ti-1Si.

Figure 9.3-1. Creep-rupture stress curves for V-3Ti-1Si at various temperatures estimated by MMCM. Solid lines represent the creep behavior of unirradiated and dashed lines that of irradiated V-3Ti-1Si alloy. Also shown is the expected stress range during the normal operation of the TITAN-I design.

Compatibility of the vanadium-base alloys with lithium coolant was investigated. Recent test results were used to establish the *anticipated degree of lithium attack on the V-3Ti-1Si alloy*. Various lithium-attack processes were examined with particular attention given to the interaction between vanadium and nonmetallic impurities such as oxygen, nitrogen, carbon, and hydrogen. The limited available data does indicate the possibility of a self-limiting corrosion rate on V-3Ti-1Si because of the formation of complex vanadium-titanium-nitride surface layers (Section 10.2.2). The effects of a bimetallic loop containing liquid lithium were also investigated. Low-carbon, titanium-stabilized ferritic steel exhibits good resistance against lithium corrosion (Section 10.2.2).

In the TITAN-I design, the liquid-lithium coolant flows at a high velocity of 21 m/s in the first-wall channels. The effects of velocity on corrosion rate are complex and depend on the characteristics of the metal and the environment. Velocity affects corrosion by two distinct mechanisms: agitation of reaction constituents can increase reaction rates; and increasing momentum of fluid particles can lead to an increase in wear (*i.e.*, erosion). Increased reaction rates are generally found in *aqueous solutions*, where the concentration of cations and anions play a large role in corrosion rates. In general, liquid metals do not interact chemically with solid surfaces, therefore, the effects of velocity on corrosion rates of vanadium alloys in a liquid-lithium environment fall mostly into the second category.

Wear by erosion can be caused by intense pressure or shock waves traveling in the fluid. A literature search regarding erosion by liquid lithium showed that this issue has not been investigated in any detail, specifically for vanadium alloys. Most of the research regarding erosion deals with water-steel systems, and particularly distinguishes between particle-free or particle-containing water or slurry. *The effects of high-velocity lithium on erosion* was estimated using water-steel data. From a very limited set of data on erosion of refractory metals by high-velocity liquid lithium and from the water-steel experience, it seems that lithium velocity of 20 to 25 m/s should not introduce unacceptable erosion rates (Section 10.2.2).

In TITAN-I reactor, electrically insulating materials are not used in direct contact with coolant; therefore coolant compatibility is not a major issue in selecting an insulating material. The selection criteria is based primarily on satisfying minimum irradiation-induced swelling, retention of strength, and minimum radiation-induced conductivity. Organic insulating materials generally do not meet high temperature requirements and suffer from rapid degradation of electrical resistivity when exposed to ionizing radiation. Ceramic insulating materials, on the other hand, possess high melting or decomposition temperatures ($> 2000^{\circ}\text{C}$).

Spinel (MgAl_2O_4) has been chosen as the primary electrical insulating material for

Figure 9.3-2. Swelling of spinel as a function of dpa (or exposure time under 18 MW/m^2 of neutron wall loading) and temperature.

the TITAN-I design, based on excellent resistance to radiation-induced swelling and retention (or increase) of strength (Section 10.2.3). A phenomenological swelling equation was developed as a function of temperature and damage dose. Figure 9.3-2 shows the estimated swelling of spinel at the first wall or divertor of TITAN-I as a function of temperature and exposure time at 18 MW/m^2 of neutron wall load. This swelling curve shows that operating spinel below 150°C or above 300°C ensures low swelling rates ($< 5\%$). High operating temperatures may ensure a low swelling rate but could bring about dielectric breakdown of the insulator. Low-temperature operation ($< 150^\circ\text{C}$) is, therefore, suggested (Section 10.2.3).

9.4. NEUTRONICS

The neutronics design of the blanket and shield for the TITAN reactors is unique because of the high neutron wall loading (18 MW/m^2). The other unique aspect of the TITAN reactors is the use of normal-conducting coils in the toroidal-field, divertor, and

ohmic-heating (OH) magnets. The neutron-fluence limit of the TITAN-I OH magnets is set by the spinel-insulator lifetime and is 3 to 4 orders of magnitude larger than that of a superconductor magnet (1×10^{23} n/m²) [19]. The use of normal-conducting coils with ceramic insulators implies a 0.6 to 0.8 m reduction in the shielding space, and helps maintain the compactness of the FPC design.

Tritium breeding, blanket energy multiplication, afterheat, radiation damage to the structural materials and the OH magnets, annual replacement mass (and cost) of blanket and shield, and the waste-disposal ratings are some of the important parameters that were considered for the neutronics optimization of the TITAN-I design. Neutronics calculations were performed to investigate each of the above parameters based on a 1-D blanket and shield model in a cylindrical geometry, with the center of the poloidal cross section of the plasma located on the centerline of the cylinder. The neutron and gamma-ray transport code, ANISN [20], is used with the cross-section library ENDF/B-V-based MATXS5 processed with the NJOY system at Los Alamos National Laboratory [21].

Scoping calculations were performed for several combinations of blanket and shield thickness with varying amount of structure and different levels of ⁶Li enrichment in the lithium coolant (Section 10.3). It was found that:

1. Most of blankets considered achieved an adequate tritium-breeding ratio (TBR > 1.2 from 1-D full-coverage calculations). Enrichment level of ⁶Li can be used to control the TBR.
2. Manganese stainless-steel shield can increase the blanket energy-multiplication ratio but would impose a potential safety problem because of higher levels of decay afterheat.
3. The energy-multiplication ratio for blankets with a vanadium shield ranges from 1.1 to 1.2. Afterheat levels, however, are considerably lower than those with a manganese shield.
4. A highly enriched ⁶Li coolant (75%) reduces the afterheat level by a factor of ~ 8 for about one hour after shutdown.
5. The atomic displacement in the shield and magnets decreases dramatically as the ⁶Li enrichment increases which reduces the annual replacement mass.

The neutronics scoping studies resulted in the reference blanket and shield design of the TITAN-I illustrated in Figure 9.4-1. The neutronics performance of the reference

RADIUS (m)		LIFETIME
0.0	PLASMA	
0.60	VOID	
0.66	FIRST WALL	1 FPY
0.67	VOID	
0.68	IBC 72% Li, 18% V, 10% VOID	
0.96	VOID	
0.97	HOT SHIELD (1st ZONE) 70% Li, 30% V	
1.27	HOT SHIELD (2nd ZONE) 10% Li, 90% V	4 FPY
1.42	VOID	
1.43	OH MAGNET 70% COPPER, 10% HELIUM 10% STRUCTURE, 10% SPINEL	30 FPY
1.85		

Figure 9.4-1. Schematic of the blanket and shield for the TITAN-I reference design.

Table 9.4-I.
 NUCLEAR PERFORMANCE
 OF THE TITAN-I REFERENCE DESIGN^(a)

• ^6Li enrichment	30%
• Tritium-breeding ratio:	
^6Li (n, α)	1.084
^7Li (n,n', α)	0.247
TOTAL	1.33
• Blanket energy multiplication, M	1.14
• Nuclear heating (MeV per DT neutron):	

<u>Component</u>	<u>Neutron</u>	<u>Gamma Ray</u>	<u>Sum</u>
First wall	0.341	0.183	0.524
Blanket	7.382	2.603	9.985
Shield (1st zone)	3.148	1.595	4.743
Shield (2nd zone)	0.235	0.560	0.795
TOTAL	11.106	4.941	16.047
OH coils	0.038	0.438	0.476

(a) From 1-D ANISN calculations.

design with a vanadium-alloy shield is given in Table 9.4-1. The tritium-breeding ratio is 1.33 and the total nuclear heating is 16.05 MeV per DT neutron resulting in a blanket energy multiplication of 1.14. The maximum fast-neutron fluence at the OH magnet after 30 full-power years (FPY) of operation at 18 MW/m² of neutron wall loading is found to be 7×10^{26} n/m², and is substantially lower than the estimated lifetime limit of 2×10^{27} n/m² for the spinel insulator.

The ⁶Li enrichment of 30% was selected for the reference design because of the improved afterheat and magnet protection performance and the acceptable enrichment cost. Future optimization of the TITAN-I design may be possible by considering different low-activation reflector materials to reduce cost and by performing very detailed trade-off studies between ⁶Li enrichment, cost, annual-replacement-mass, and waste-disposal issues.

The final design parameters were verified by a set of 3-D neutronics calculations with the Monte Carlo code, MCNP [22], taking into account the toroidal geometry and the divertor modules. Non-IBC blanket tubes are incorporated in the space around the divertor and target plates. The hot-shield is extended behind the divertor coils except that a 90° opening in the poloidal direction is provided on the outboard divertor region for pumping (Figure 9.2-6). The tritium-breeding ratio for the 3-D model of the reference design is 1.18 and the maximum fast-neutron fluence at the OH magnet in the inboard region is 1.6×10^{27} n/m², which is well below the assumed lifetime limit for the spinel insulator.

9.5. THERMAL AND STRUCTURAL DESIGN

The TITAN-I FPC is cooled by liquid lithium. One of the issues for liquid-metal coolants in fusion reactors is the MHD-induced pressure drop. In an RFP fusion reactor such as TITAN, the toroidal magnetic field at the first wall is small; thus the MHD pressure drop can be kept low by alignment of the coolant channels primarily in the poloidal direction.

The TITAN designs operate with a highly radiative core plasma, in order to distribute the surface heat load uniformly on the first wall (4.6 MW/m²) and to keep the heat load on the divertor target plates at a manageable level. Cooling of the high-heat-flux components, such as the first wall and divertor target plates, represents one of the critical engineering aspects of compact fusion reactors. The use of a highly radiative core plasma

and the resulting distribution of the heat fluxes over the first wall is central to the solution to this problem. The main thermal-hydraulic design features of the TITAN-I FPC are:

1. First-wall sputtering is almost negligible as a result of the operation with a high-recycling divertor.
2. Small-diameter, thin-walled, circular coolant tubes are used for the first wall.
3. First-wall and blanket coolant circuits are separated.
4. Coolant channels are aligned with the dominant, poloidal magnetic field.
5. Turbulent-flow heat transfer is used to remove the high heat flux on the first wall.

For a given size for the first-wall coolant tubes, the maximum wall-temperature constraint would result in a maximum limit on the surface heat flux. Turbulent coolant flow, which is accompanied by a higher Nusselt number (Nu), allows a higher surface heat flux compared with laminar coolant flow. The magnetic field, generally, tends to suppress turbulence in the flow of an electrically conducting fluid, and the onset of turbulence would occur at higher Reynolds numbers compared with non-MHD pipe flow.

A few studies on the turbulent heat transfer in liquid metals in the presence of a magnetic field [23-25] are available. Kovner *et al.* [23] performed experiments on the effect of a longitudinal magnetic field on turbulent heat transfer in liquid-gallium flow in a tube. The following empirical correlation for Nusselt number was then proposed:

$$Nu = 6.5 + \frac{0.005Pe}{1 + 1890(Ha_{\parallel}/Re)^{1.7}}, \quad (9.5-1)$$

where Re is the Reynolds number, $Ha_{\parallel}$ is the parallel Hartmann number, $Pe = Re Pr$ is the Peclet number, and Pr is the Prandtl number. Even though Equation 9.5-1 is based on experimental data up to $Ha_{\parallel} = 550$, it is expected to hold beyond this range. Other experimental data and numerical studies show similar dependence [24,25]. Figure 9.5-1 shows the variation of the Nusselt number with Peclet number for $B = 0$, $Ha_{\parallel} = 1000$, and $Ha_{\parallel} = 3040$ (the expected $Ha_{\parallel}$ in the TITAN-I first wall). The range of the experimental data is also shown in this figure.

The nonuniform circumferential heat flux on the first-wall coolant tubes will reduce the turbulent Nusselt number at the point of higher heat flux, as is shown for the case of laminar flow (Section 10.4.3). For the TITAN-I design, the Nu given by Equation 9.5-1 is reduced by a factor of two to account for this nonuniform circumferential heat flux until

Figure 9.5-1. Variation of the Nusselt number, Nu , with the Peclet number, Pe for turbulent flow as predicted by Equation 9.5-1. The range of experimental data as well as the operating point of the TITAN-I first wall are also shown. The Nusselt number from this graph should be halved to account for nonuniform heat flux on the TITAN-I first wall.

further data becomes available. The MHD pressure drops were calculated by various correlations appropriate for TITAN-I design (Section 10.4).

In order to complete the thermal-hydraulic design, pressure and thermal stresses in the FPC coolant channels were estimated by 1-D equations (along r , radial direction in the tube) for a thick-walled tube. Two-dimensional thermomechanical analyses of the TITAN-I FPC were also performed using the finite-element code, ANSYS [26], which verified the 1-D analysis (Section 10.4.6).

A thermal-hydraulic design window for compact, liquid-lithium-cooled RFP reactors was found based on certain design constraints such as the maximum allowable temperature of the structure (750°C), the maximum allowable pressure and thermal stresses in the structure (respectively, 108 and 300 MPa), and the maximum allowed pumping power (5% of plant output). The maximum allowable temperature of the structure corresponds to a maximum value for the average coolant exit temperature for a given heat flux. The maximum allowable stress and the maximum allowed pumping power would result in minimum values for the average exit temperature of the coolant. The design window for the coolant exit temperature is then located between these limits. Other parameters impacting the design window are the neutron wall loading, the coolant channel size, and the coolant inlet temperature.

Figure 9.5-2 illustrates the thermal-hydraulic design window for the TITAN-I first wall and shows that a design with a radiation heat flux on the first wall of 4.9 MW/m^2 is possible (corresponding to 20 MW/m^2 of neutron wall load at 95% total radiation fraction). The sudden change in the slope of the top curve in Figure 9.5-2 is caused by the change from laminar to turbulent flow. The flow in blanket and shield is always laminar. The total pumping-power limit of 5% of electric output is more restrictive than the pressure stress of 108 MPa. The thermal-stress limit is not reached up to the maximum heat flux on the first wall.

The main results of the thermal-hydraulic analysis of the TITAN-I first wall are given in Table 9.5-I. The coolant flow velocity in the TITAN-I first-wall tube is 21 m/s and the maximum pressure drop is 10 MPa. The coolant velocities in the 1st and 6th (last) rows of the IBC coolant channels are, respectively, 0.5 and 0.2 m/s. The pressure drops in the 1st and 6th rows are 3.0 and 0.5 MPa, respectively. The maximum pressure drop in the divertor coolant circuit is 12 MPa. In order to simplify the design, the first-wall and divertor coolants are supplied from the same circuit with a delivery pressure of 12 MPa. One single orifice is used to reduce the lithium pressure to 10 MPa for the first-wall circuit. Additional orifices are used, wherever necessary, in order to reduce the coolant pressure from 12 MPa to the required inlet pressure of the individual rows of divertor coolant

Table 9.5-I.

THERMAL-HYDRAULIC DESIGN OF TITAN-I FIRST WALL

Pipe outer diameter, b	10.5	mm
Pipe inner diameter, a	8.0	mm
Wall thickness, t	1.25	mm
Erosion allowance	0.25	mm
Structure volume fraction	0.400	
Coolant volume fraction	0.375	
Void volume fraction	0.225	
Coolant inlet temperature, T_{in}	320	°C
Coolant exit temperature, $T_{ex,FW}$	440	°C
Maximum wall temperature, $T_{w,Max}$	747	°C
Maximum primary stress	50	MPa
Maximum secondary stress	288	MPa
Coolant flow velocity, U	21.6	m/s
Pressure drop, Δp	10	MPa
Total pumping power ^(a)	37.7	MW
Reynold's number, Re	1.90×10^5	
Magnetic Reynold's number, Re_m	0.48	
Parallel Hartmann number, $H_{ }$	3.04×10^3	
Perpendicular Hartmann number, $H_{\perp}$	2.01×10^2	
Parallel magnetic interaction parameter, $N_{ }$	48.6	
Perpendicular magnetic interaction parameter, $N_{\perp}$	0.21	
Nusselt number, Nu	10.35	
Prandtl number, Pr	4.08×10^{-2}	
Peclet number, Pe	7.76×10^3	

(a) A pump efficiency of 90% is assumed.

Figure 9.5-2. The thermal-hydraulic design window for the TITAN-I FPC.

tubes. The supply pressure of the blanket coolant pump is 3 MPa. Orifices are used to reduce the pressure to the required values at the inlet of each row of IBC channels.

9.6. MAGNET ENGINEERING

Three types of magnets are used in the TITAN-I design (Figure 9.2-4). The ohmic-heating (OH) and the equilibrium-field (EF) trim coils are normal-conducting with copper alloy as the conductor, spinel as the insulator, and gaseous helium as the coolant. The main EF coils are made of NbTi conductor and steel structural material. These poloidal-field (PF) coils are designed to last the life of the plant. Because of their simple geometry, the robust support structure, and the relatively low field produced by these coils, little or no extrapolation of current technology should be required.

The divertor and the toroidal-field (TF) coils of the TITAN-I design are based on the integrated-blanket-coil (IBC) concept [14]. The IBC as applied to TITAN-I also acts as the toroidal-field driver coil for the oscillating-field current-drive system, OFCD (Section 7). The toroidal-field (TF) coils of TITAN-I oscillate at 25 Hz with currents ranging between 30% to 170% of the mean steady-state value of 7.0 MA-turns (including

the currents in the divertor trim coils). The PF coils also oscillate at 25 Hz. It is also necessary to oscillate the divertor coils to maintain the plasma separatrix at the proper location.

The IBC design encounters several critical engineering issues: (1) steady-state and oscillating power-supply requirements for low-voltage, high-current coils; (2) time-varying forces caused by the OFCD cycles; (3) integration of the primary heat-transport system with the electrical systems; (4) sufficient insulation to stand off induced voltages; and (5) suitable time constants for various components to permit the coil currents to oscillate at 25 Hz. Heat removal is not an issue for IBC because the joule heating is produced directly in the primary coolant.

Design of the power supplies is one of the critical issues for IBC. The low number of electrical turns available (12 for the TITAN-I design) results in low-voltage, high-current coils (3.85 V, 520 kA per coil). Power supplies rated for such conditions would be expensive and would exhibit high internal-power losses if based on technology that is currently available. Connecting all 12 IBCs of TITAN-I in series would raise the voltage of the power supply to a more manageable value. However, the IBC approach requires that the electrical and hydraulic systems be physically connected, and that the intermediate heat exchangers (IHxs) and coolant pumps should be grounded (*i.e.* no electric current flowing through the IHxs and pumps).

Figure 9.6-1 illustrates the electrical and hydraulic layout of the TITAN-I IBC system. The TITAN-I FPC comprises three sectors which are connected to each other through the divertor modules. To increase the power-supply voltage, the four IBCs in each sector are electrically connected in series in the TITAN-I design and allow a better match of current and voltage for the power supply (15.4 V, 541 kA). This circuit, however, requires two IHxs per sector for the IBC cooling circuit. Figure 9.6-1 shows that, because of the series connection of the IBCs and grounding of the pumps and heat exchangers, a leakage current will flow through the cold and hot legs. The leakage current is small in magnitude but causes unequal coil currents, necessitating a small balancing power supply to accompany each main power supply, as is indicated in Figure 9.6-1. The load on each balancing power supply (7.7 V and 27 kA) is much smaller than that of the main power supply (15.4 V and 541 kA) and leaks through the cold legs to ground.

The impurity-control system of the TITAN-I design consists of three toroidal-field divertors. Each divertor consists of one nulling coil and two flanking coils to produce the local effects necessary for field nulling. Because of the loss of coverage of TF IBCs in the divertor region, a pair of trim coils is added to each divertor in order to localize the toroidal-field ripple. The divertor IBCs operate at relatively higher current densities

Figure 9.6-1. Schematic of the electrical and hydraulic layout of TITAN-I TF IBCs.

than the TF coils, thereby requiring much greater voltages. Furthermore, the current in each nulling coil is exactly equal to that of the two flanking coils. The divertor IBCs are connected in order to take advantage of the symmetric currents and larger voltages. Because equalizing power supplies are not needed for the divertor IBCs, only two power supplies per divertor module are required. The joule losses in the divertor IBCs (three divertors) are 117 MW with additional 3.5-MW losses in the hot legs.

Because of the large impedance of the toroidal-field circuit during the OFCD cycles (about $0.1 \text{ m}\Omega$ for each TF coil), the oscillating voltage on each TF coil ($\sim 50 \text{ V}$) is much larger than the steady-state value ($\sim 3.8 \text{ V}$). Detailed analyses of the OFCD power supplies and the leakage currents were not performed because the results are sensitive to the impedances of the hot and cold legs which in turn depend on the piping arrangement. Instead, the leakage currents were calculated based on simple estimates of the internal inductances of coolant pipings. The joule losses in the TF coils during the OFCD cycle are estimated to be 25.6 MW for the steady-state portion (24 MW in the coil and 1.6 MW in the hot and cold legs) and 16 MW for the oscillating voltages (15 MW in the coils and 1 MW in the hot and cold legs).

It is necessary to oscillate the divertor coils during the OFCD cycle to maintain the plasma separatrix at the proper location. Since the magnitude of oscillation of the toroidal flux was found to be small, the strength of the toroidal field on the plasma surface would be directly proportional to the reversal parameter and the magnitude of the current oscillations in the divertor coils would be about $2/3$ of the steady-state value. The voltage oscillations applied to the divertor coils are roughly equal to the steady-state values, in contrast to the TF coils, because the divertor coils operate at much higher current densities and have higher resistances. The oscillating losses in the divertor IBCs are estimated at 26 MW in the coils and 0.8 MW in the hot and cold legs.

Interaction of the current in the IBC with the reactor magnetic fields produces forces on the TF and divertor coils. These forces are much lower than the corresponding forces in tokamaks since the coil currents are much lower in RFPs. The magnitudes of the forces on the TITAN-I IBCs vary over time as the currents oscillate during the OFCD cycle. Hence, structural supports are designed for the peak loads (Section 10.5.3). Dynamic structural analysis also shows that failure will not occur as a result of these cyclic forces.

9.7. POWER CYCLE

One of the advantages of using liquid lithium as the coolant for TITAN-I is the ability to remove the thermal energy from the reactor at a high thermal potential so that a high power-cycle efficiency can be realized. An important feature of the thermal-hydraulic design of the TITAN-I first wall and blanket is the separation of the coolant circuits for these components in order to handle the high surface-heat flux on the first wall. As a result, the first-wall coolant has a much lower exit temperature (440°C) than the blanket and shield coolant (700°C). The divertor coolant also has a different exit temperature (540°C). The inlet temperatures to all three circuits are kept the same primarily for simplicity. Since the thermal power in the divertor circuit is very small (1% of total thermal power), the first-wall and divertor coolants are mixed at exit, leading to two separate streams of the primary coolant which remove, respectively, 765 and 2170 MWt of power with exit temperatures of 442 and 700°C .

The TITAN-I reference design uses two separate power cycles: one for the first-wall and divertor stream and the other for the IBC and shield stream (Section 10.6.2). Each of these two power cycles has a separate IHX, steam generators, and turbine-generator set. The TITAN-I FPC consists of three toroidal sectors. One IHX and one steam generator are required per sector for the first-wall and divertor coolant stream. The steam produced in these three steam generators is mixed and fed to a single turbine-generator set. For the IBC and shield stream, two IHXs are used per sector, based on electrical engineering requirements of the IBCs. The secondary coolants of each pair of these heat exchangers are mixed and fed to one steam generator (per sector). As in the first-wall and divertor cycle, the steam from all three steam generators is mixed and fed to a single turbine-generator set.

The power cycle analysis is performed by the computer code PRESTO [27]. The pinch-point temperature difference in the steam generators of each of these power cycles is kept above 20°C . For both the first-wall and divertor cycle and the IBC and shield cycle, the temperature loss in the IHXs is set at 20°C . The first-wall and divertor power cycle is a superheat Rankine cycle with four stages of feed-water heating. Reheat of the steam after expansion through the high-pressure turbine is not used. The total thermal power in the first-wall and divertor power cycle is 765 MWt and the gross thermal efficiency is 37.0%. The IBC and shield power cycle is a superheat Rankine cycle with two reheat stages and seven stages of feed-water heating. The superheater and the reheaters are arranged in series. The total thermal power in this cycle is 2170 MWt, and the gross thermal efficiency of this cycle is 46.5%.

The main results and parameters of the first-wall and divertor cycle and the IBC and shield cycle are given in Table 9.7-1. The overall gross thermal efficiency for the TITAN-I design, by combining the efficiencies of the two cycles, is 44%.

9.8. DIVERTOR ENGINEERING

The design of the impurity-control system poses some of the most severe problems of any component of a DT fusion reactor and for a compact or high-power-density device these problems can be particularly challenging. The final TITAN-I divertor design represents the result of extensive iterations between edge-plasma analysis, magnetic design, thermal-hydraulic and structural analysis, and neutronics.

A summary of the results of the edge-plasma modeling is given in Table 9.8-1 and is described in detail in Section 5.4. The plasma power flow is controlled by the injection of a trace amount of a high- Z material (xenon) into the plasma which causes strong radiation from the core, scrape-off layer, and divertor plasmas. About 95% of the steady-state heating power (alpha particle and ohmic heating by the current-drive system) is thereby radiated to the first wall and divertor plate, although only about 70% is radiated from the core plasma (*i.e.*, inside the separatrix). This intense radiation reduces the power deposited on the divertor target by the plasma to an acceptably low level. Preliminary experimental results [12,13] suggest that beta-limited RFP plasmas can withstand a high fraction of power radiated without seriously affecting the global confinement (Section 5.3). The radiative cooling also reduces the electron temperature at the first wall and divertor target (also assisted by recycling) which, in turn, reduces the sputtering and erosion problems.

To satisfy the requirement for a high- Z material for the plasma-facing surface of the divertor target, a tungsten-rhenium alloy (W-26Re) is used. The high rhenium content provides the high ductility and high strength necessary for the severe loading conditions. A bank of lithium-cooled vanadium-alloy coolant tubes removes the heat deposited on the target. These tubes are separated from the tungsten-alloy armor by a thin, electrically insulating layer of spinel, to avoid an excessive MHD pressure drop. Fabrication of the divertor target is based on brazing the tungsten-alloy plate (produced by powder-metallurgy techniques) to the bank of coolant tubes, with the spinel layer deposited by the chemical-vapor-deposition (CVD) process. As a second technique, a unique manufacturing process using CVD (instead of brazing) is proposed to enhance bond strength of the tungsten-spinel-vanadium interfaces.

Table 9.7-1.

PARAMETERS OF THE TITAN-I POWER CYCLE

First-Wall and Divertor Power Cycle:		
Total thermal power in the primary coolant	765	MWt
Primary-coolant inlet temperatures	320	°C
Primary-coolant exit temperatures	442	°C
Secondary-coolant inlet temperatures	300	°C
Secondary-coolant exit temperatures	422	°C
Throttle steam temperature	396	°C
Throttle steam pressure	10.7	MPa
Steam flow rate	326	kg/s
Condenser back pressure	6.76×10^3	Pa
Stages of feed-water heating	4	
Feed-water inlet temperature	169	°C
Gross thermal efficiency	37.0%	
IBC and Shield Power Cycle:		
Total thermal power in the primary coolant	2170	MWt
Primary-coolant inlet temperatures	320	°C
Primary-coolant exit temperatures	700	°C
Secondary-coolant inlet temperatures	300	°C
Secondary-coolant exit temperatures	680	°C
Steam temperature after 1st reheat	565.6	°C
Steam temperature after 2nd reheat	550.0	°C
Throttle steam pressure	21.4	MPa
Steam flow rate	703	kg/s
Condenser back pressure	6.76×10^3	Pa
Stages of feed-water heating	7	
Feed-water inlet temperature	258	°C
Gross thermal efficiency	46.5%	
Overall gross thermal efficiency	44.0%	

Table 9.8-I.
SUMMARY OF TITAN-I EDGE-PLASMA CONDITIONS

Number of divertors	3
Scrape-off layer thickness	6 cm
Peak edge density	$1.7 \times 10^{20} \text{ m}^{-3}$
Peak edge ion temperature	380 eV
Peak edge electron temperature	220 eV
Plasma temperature at first wall	1.7 eV
Peak divertor density	$6 \times 10^{21} \text{ m}^{-3}$
Peak divertor plasma temperature	4.5 eV
Divertor recycling coefficient	0.995
Throughput of DT	$6.7 \times 10^{21} \text{ s}^{-1}$
Throughput of He	$8.2 \times 10^{20} \text{ s}^{-1}$
Vacuum tank pressure	20 mtorr

The TITAN-I impurity-control system is based on the use of toroidal-field divertors to minimize the perturbation to the global magnetic configuration (toroidal-field is the minority field in RFPs) and to minimize the coil currents and stresses. The TITAN divertor uses an "open" configuration, in which the divertor target is located close to the null point and faces the plasma, rather than in a separate chamber. This positioning takes advantage of the increased separation between the magnetic field lines (flux expansion) in this region, which tends to reduce the heat loading on the divertor plate because the plasma flowing to the target is "tied" to the field lines. The high plasma density in front of the divertor target ensures that the neutral particles emitted from the surface have a short mean free path; a negligible fraction of these neutral particles enter the core plasma (Section 5.5).

The final magnetic design includes three divertor modules, located 120° apart in toroidal direction (Figure 9.8-1). The magnetic field lines are diverted onto the divertor

plate using a nulling coil and two flanking coils which localize the nulling effect. For the TITAN-I design, the divertor IBC assembly displaces a part of the TF IBC tube bank. Therefore, a pair of trim coils is also required to control the toroidal-field ripple. Also shown on the outboard view of Figure 9.8-1 is the pumping aperture which leads to the vacuum tank surrounding the torus. This aperture is present for only the outboard 90° in the poloidal angle; elsewhere there is shielding material to protect the OH coils.

The low value of the toroidal field in the RFP allows high coolant velocities to be achieved without prohibitive MHD pressure drops, thus permitting operation in the turbulent flow regime with the associated high heat-transfer coefficients. Despite the intense radiation arising from the impurities injected into the plasma, careful shaping of the divertor target, as shown in Figure 9.8-1, is also required to maintain the heat flux at acceptable levels at all points on the plate. Figure 9.8-2 shows the distribution of the various components of the surface heat flux along the divertor target for the inboard and outboard locations. The heat flux on the inboard target ($\sim 9.5 \text{ MW/m}^2$) is significantly higher than that on the outboard ($\sim 6 \text{ MW/m}^2$), because of the toroidal effects.

The temperature distribution of the target plate coolant and structure is shown in Figure 9.8-3. The same coolant inlet temperature of 320°C as for the first wall is used, allowing both coolant loops to be fed from the same circuit. The maximum temperature of the vanadium-alloy tubes does not exceed 750°C . The maximum temperature of the tungsten-rhenium armor is about 930°C , at which level the alloy retains high strength and the thermal stresses are within allowable levels.

A total pressure drop of 12 MPa was used for the divertor-coolant circuit. The maximum allowable coolant velocity was set at 25 m/s for this analysis, based on considerations of physical erosion. Figure 9.8-3 also shows the components of the pressure drop in the divertor-coolant tubes. Flow orificing is used extensively to tailor the coolant velocity distribution. In low-field regions, the large pressure head of 12 MPa would otherwise cause the velocity to exceed the 25 m/s limit. Near the outside of the plate, orificing allows the coolant outlet temperature to be adjusted so as to maintain an approximately constant level across the plate.

A detailed finite-element analysis of the steady-state temperatures and stresses in the divertor was made using the finite-element code, ANSYS [26], which has verified the design of the target plates (Section 11.5). A 2-D finite-element structural analysis also indicated that stress concentrations will occur at the edge of the interface between the different materials of the target. This aspect requires further analysis and experimental investigation to assure the viability of the design.

Figure 9.8-2. Heat flux distribution on outboard (A) and inboard (B) sections of divertor target. Coolant tubes are numbered from the apex or symmetry point of the target between the nulling coil and the core plasma.

Figure 9.8-3. Coolant and structural temperatures at the coolant outlet (A) and components of the coolant pressure drop (B) for coolant tubes in the divertor target. Coolant tubes are numbered from the apex or symmetry point of the target between the nulling coil and the core plasma.

The vacuum system is based on the use of a large vacuum tank encompassing the entire torus, and connected to the divertor region by a duct located at each of the three divertor locations. It is proposed to employ lubricant-free magnetic-suspension-bearing turbo-molecular pumps for the high-vacuum pumps to avoid the possibility of tritium contamination of oil lubricants.

9.9. TRITIUM SYSTEMS

The major units in the tritium system of a fusion reactor are: (1) the plasma-processing system, (2) the breeder tritium-recovery system, (3) the atmospheric-tritium system, and (4) the secondary containment systems. The complete tritium system has to be designed under the constraints of tritium inventory, system cost, and tritium leakage rate. Significant relaxation of any one of the constraints will have a major impact on the overall design of the complete system.

In the TITAN design, the separation of the D and T of the plasma exhaust is not required. Therefore, only about 1% of the plasma exhaust will be required to pass through the cryogenic distillation system to separate protium generated by the DD reaction. The capacity and cost of the plasma-exhaust processing is thus much reduced. Since the cost of the plasma-exhaust-processing system is so low, a redundant unit is affordable. A double plasma-exhaust-processing system can significantly improve the reliability of the system and the reactor tritium storage can be reduced.

A molten-salt recovery process [28] is selected for tritium recovery from the lithium blanket, in which the liquid lithium and a molten salt are in contact, and LiH is preferentially distributed to the salt phase. The salt is then electrolyzed to yield hydrogen which is removed by sweeping the porous stainless-steel hydrogen electrode with a circulating stream of inert gas. The tritium is subsequently recovered from the inert gas with a getter. The molten-salt recovery process has been demonstrated on a laboratory scale to recover tritium from lithium down to 1 wppm. Therefore, the tritium inventory in the blanket would be moderate. The parameters of TITAN-I blanket tritium-recovery system is shown in Table 9.9-I.

Most reactor designs selected sodium as the intermediate coolant [16]. For the TITAN-I design, lithium is also used as the intermediate coolant to avoid using two separate technologies (sodium and lithium). Since, tritium solubility is much higher in lithium than in sodium, the TITAN-I design has a moderate amount of tritium inventory in the secondary loop. A unique advantage of using lithium as the primary coolant and

Table 9.9-I.
ANALYSIS OF MOLTEN-SALT EXTRACTION SCHEME FOR
A LIQUID-LITHIUM BLANKET SYSTEM

Breeding rate	420 g/d ^(a)
Recovery rate	520 g/d ^(a)
Lithium exit temperature	556 °C ^(b)
Extraction system temperature	556 °C ^(b)
Estimated blanket inventories	
Lithium	2.12×10^8 g
Tritium	212 g (1 wppm)
Tritium recovery efficiency, ϵ	90%
Capacity per extractor unit	23 m ³ /h
Electrical power per unit	3.7 kW

Tritium Concentration (wppm)	Effective Distribution Coefficient ($D_v\eta$)	Lithium Processed per Hour (kg/h)	Number of Units	Required Electrical Power (kW)
1	4	22,000	7	26
1	1	88,000	28	104 ^(c)

(a) Based on a tritium-breeding ratio of 1.2 and 100 g/d of PDP.

(b) Parameters of the Scoping Phase design,
the blanket and first-wall coolant were mixed in the outlet.

(c) Reference case.

Table 9.9-II.
TRITIUM INVENTORIES IN TITAN-I REACTOR

Unit	Tritium Inventory (g)
Storage	1,100
Primary-coolant loop	212
Secondary-coolant loop	300
Molten-salt extraction	10
Fuel processing	20
First wall:	
typical case	0.72
excessive PDP	4.53
Integrated blanket coil	2.20
Hot shield, zone 1	0.14
Hot shield, zone 2	0.25
Divertor shield	0.08
Divertor	< 0.01
Out-of-blanket piping	≪ 0.01
Total TITAN-I inventory	1,650

as the breeder is associated with the high tritium solubility in the lithium. The tritium partial pressure is very low. For a tritium concentration of 1 wppm, the tritium partial pressure is only 10^{-7} Pa. With such low tritium partial pressure, tritium containment is usually not a severe problem. This reduces the required capacity of the room-air-detritiation system and the secondary containment systems. The tritium inventories in TITAN-I components are shown in Table 9.9-II. The TITAN-I tritium inventory (1650 g) and leakage rate (7 Ci/d) are very reasonable.

A potential problem facing TITAN-I is the plasma-driven permeation (PDP) of low-energy tritons through the permeable vanadium-alloy first wall. The extent of PDP depends on the ability of the small fraction of high-energy plasma ions to adequately clean the first-wall surface, which is uncertain. The problem of PDP is not unique to

compact RFP designs. Any fusion reactor design with a combination of a low edge temperature and a vanadium-alloy first wall must consider this problem. Experiments are needed to determine the extent of PDP and the sputtering rate of the first-wall structure at low edge-plasma temperatures. In the TITAN-I design, a tungsten-rhenium alloy (W-26Re) is chosen for the divertor plates. Because tungsten is very resistant to permeation, PDP through the divertor plate is not a concern.

9.10. SAFETY DESIGN

Strong emphasis has been given to safety engineering in the TITAN study. Instead of an add-on safety design and analysis task, the safety activity was incorporated into the process of design selection and integration from the beginning of the study. The safety-design objectives of the TITAN-I design are: (1) to satisfy all safety-design criteria as specified by the U. S. Nuclear Regulatory Commission on accidental releases, occupational doses, and routine effluents; and (2) to aim for the best possible level of passive safety assurance.

The elevation view of the TITAN-I reactor is shown in Figure 9.2-1. The key safety features of the lithium self-cooled TITAN-I design are:

- The selection of a low-afterheat structural material, V-3Ti-1Si;
- The selection of a relatively high ^6Li enrichment (30%) to aid in further reducing afterheat and radioactive wastes;
- The use of three enclosures separating the lithium and air: the blanket tubes, vacuum vessel, and the containment building which is filled with argon cover gas;
- Locating all coolant piping connections at the top of the torus to prevent a complete loss of coolant in the FPC in case of a pipe break;
- The use of lithium-drain tanks to reduce the vulnerable lithium inventory should a pipe break occur;
- The use of steel liner to cover the containment-building floor to minimize the probability of lithium-concrete reaction;
- Excluding water from the containment building and vacuum vessel to prevent the possibility of lithium-water reaction.

Figure 9.10-1. The thermal response of the TITAN-I FPC to a complete LOFA as a function of time after the initiation of the accident.

Two of the major accidents postulated for the fusion power core are the loss-of-flow accident (LOFA) and the loss-of-coolant accident (LOCA). Thermal responses of the TITAN-I FPC to these accidents are modeled using a finite-element heat-conduction code, TACO2D [29]. Figure 9.10-1 shows the resulting temperatures during a LOFA. At 12.8 hours after the initiation of the accident, the first wall reaches its peak temperature of 990°C which is well below the recrystallization temperature of the V-3Ti-1Si alloy. The first-wall peak temperature is also well below ~1300°C, the on-set of volatilization of radioactive products (CaO, SrO) in the vanadium alloy (more experimental data is needed to clarify the on-set temperature and the extent of the release of these radio-isotopes). The heat capacity of the static lithium accounts for the moderate temperature excursion. No natural convection of the coolant is assumed even though the emergency plasma shutdown procedure is accompanied by the discharge of all magnets and no MHD retarding force is expected on the coolant. If natural convection develops, the temperature excursions would be considerably smaller than those predicted by Figure 9.10-1.

Thermal creep-rupture behavior of the TITAN-I first wall during accidents is estimated using the modified-minimum-commitment method (MMCM) [18]. For the materials and loadings expected in the TITAN-I first wall during a LOFA, the thermal stresses

have a negligible influence on the rupture time relative to the pressure stresses. The predicted rupture times for several primary stresses at 1000°C are given in Table 9.10-I. Since the coolant pressure is lost during off-normal conditions, the expected primary stress in the TITAN-I design during a LOFA is below 2 MPa and is caused by the hydrostatic pressure load inside the coolant piping. Table 9.10-I shows that creep-rupture would not occur even if the structure is kept at elevated temperatures (1000°C) for a prolonged period of time – a LOFA would not lead to a LOCA. High-temperature creep-rupture data above 850°C are necessary to gain more confidence in the creep-rupture behavior at these higher temperatures.

Higher afterheat is expected in the tungsten plate of the divertor. During a LOFA, the peak temperature in the divertor vanadium cooling tube is 1117°C, close to recrystallization temperature of the V-3Ti-1Si. This may result in shortening the lifetime of the divertor modules, but failure that would lead to a LOCA is unlikely.

In the event of major primary-pipe breaks and failure of the containment building and vacuum vessel, air could enter the vacuum chamber and start a lithium fire. The TITAN-I reactor is configured so as (1) to ensure that a lithium fire would be a low probability event, and (2) to minimize the consequences of lithium fire if it occurs. In order to reduce the probability of lithium fires, three barriers (primary-coolant pipes, the vacuum tank, and the containment building) exist between the primary-coolant lithium and air. The containment building is also filled with argon cover gas. In order to reduce

Table 9.10-I.

CREEP-RUPTURE TIME FOR TITAN-I FIRST WALL

Primary Stress, σ_p (MPa)	Rupture Time, t_r (h)
10	3200
20	360
30	101
40	41
50	20

the consequences of a lithium spill, two sets of lithium-drain tanks are provided to drain the maximum amount of lithium in less than 30 seconds.

For the perceived worst-accident condition of a lithium fire with breach of all barriers and no argon cover gas, the maximum combustion-zone temperature is found to be less than 1000 °C. The tritium release in this case would be about 60 Ci which is quite acceptable under this worst-accident scenario. Of critical concern in the lithium-fire scenario is the formation and release of vanadium oxide V_2O_5 . Further measurement of vanadium-oxide formation and its vapor pressure with temperature, and the calculation of potential releases to the public based on the TITAN-I configuration and accident scenarios should be performed.

The total tritium inventories in the lithium primary and secondary loops are 344 and 300 g, respectively. These are acceptable inventories when passive drain tanks are used to control the amount of possible tritium releases. The tritium inventory in the blanket structure is less than 10 g, which is also acceptable. The tritium-leakage rate from the primary loop was estimated to be 7 Ci/d which is within the 10 Ci/d design goal.

Plasma-accident scenarios need to be further evaluated as the physics behavior of RFPs becomes better understood. Preliminary results indicate that passive safety features can be incorporated into the design, such that the accidental release of plasma and magnetic energies can be distributed without leading to major releases of radioactivity. Research activities in this area need to be continued, especially for high-power-density devices.

Based on the analyses summarized above, TITAN-I does not need to rely on any active safety systems to protect the public. A LOFA will result in no radioactive release and will not lead to a more serious LOCA. A complete LOCA from credible events is not possible. Only the assurance of coolant-piping and vacuum-vessel integrity is necessary to protect the public. The TITAN-I design, therefore, meets the definition of level 3 of safety assurance, "small-scale passive safety assurance" [30,31]. Pending information on the vanadium-oxide formation and release from the TITAN-I vacuum chamber under the lithium-fire accident scenario, the qualification of TITAN-I as a level-2 of safety assurance design, "large-scale passive safety assurance," may also be possible.

9.11. WASTE DISPOSAL

The neutron fluxes calculated for the reference TITAN-I reactor were used as input to the activation calculation code, REAC [32]. These results were analyzed to obtain the

allowable concentrations of alloying and impurity elements in TITAN-I FPC components. Waste-disposal analysis has shown that the compact, high-power density TITAN-I reactor can be designed to meet the criteria for Class-C waste disposal [33]. The key features in achieving Class-C waste in the TITAN-I reactor are attributed to: (1) materials selection and (2) control of impurity elements.

The materials selected for the TITAN-I FPC are the vanadium alloy, V-3Ti-1Si, and lithium. The main alloying elements of V-3Ti-1Si do not produce long-lived radionuclides with activity levels exceeding the limits for Class-C disposal (no limit on the concentration of vanadium and titanium and 23% allowable concentration of silicon which is much larger

Table 9.11-I.

MAXIMUM CONCENTRATION LEVELS OF IMPURITIES IN TITAN-I REACTOR COMPONENTS TO QUALIFY AS CLASS-C WASTE

Element	Major Nuclide (Activity Limit) ^(a)	Components			Nominal Level
		FW & Blanket (1 FPY) ^(b)	Hot Shield (5 FPY) ^(b)	OH Magnets (30 FPY) ^(b)	
Nb (appm)	⁹⁴ Nb (0.2 Ci/m ³)	5.	1.4	0.5	0.1
Mo (appm)	⁹⁹ Tc (0.2 Ci/m ³) ⁹⁴ Nb (0.2 Ci/m ³)	65.	100.	90.	1.0
Ag (appm)	^{108m} Ag (3 Ci/m ³)	1.3	1.5	0.7	1.0
Tb (appm)	¹⁵⁸ Tb (4 Ci/m ³)	0.4	0.6	7.0	5.0
Ir (appm)	^{192m} Ir (2 Ci/m ³)	0.1	0.1	0.02	5.0
W	^{186m} Re (9 Ci/m ³)	5%	9%	100%	0.89%

(a) From Reference [32].

(b) Based on operation at 18 MW/m² of neutron wall loading.

than 1% content of Si in V-3Ti-1Si). The allowable concentrations of various impurities in the vanadium structural material of the TITAN-I reactor are listed in Table 9.11-I. Some of these impurity elements, mainly niobium and possibly silver, terbium, and iridium, need to be controlled in the vanadium alloy below appm levels.

Table 9.11-II summarizes the TITAN materials and related quantities for Class-C disposal. The total weight in the FPC of the TITAN-I reactor is about 1363 tonnes, of which about 73% is from the magnet systems (OH and EF coils, and EF shield) that last the plant lifetime. The reactor torus (first wall, blanket, and the divertor module) is replaced annually and constitutes only 4% of the total weight of the FPC. The balance of the weight is from the shield which has a five-year lifetime. The average annual-replacement mass of the FPC is about 150 tonnes.

The TITAN-I divertor plates are fabricated with a tungsten armor because of its low sputtering properties. The waste-disposal rating of the divertor plates is estimated to be a factor of 10 higher than for Class-C disposal after one year of operation. The annual disposal mass of this non-Class-C waste is 0.35 tonnes, about 0.23% of the average annual discharge mass.

The conclusions derived from the TITAN-I reactor study are general, and provide strong indications that Class-C waste disposal can be achieved for other high-power-density approaches to fusion. These conclusions also depend on the acceptance of recent evaluations of specific activity limits carried out under 10CFR61 methodologies [34].

9.12. MAINTENANCE

The TITAN reactors are compact, high-power-density designs. The small physical size of these reactors permits each design to be made of only a few pieces, allowing a single-piece maintenance approach [7,8]. Single-piece maintenance refers to a procedure in which all of components that must be changed during the scheduled maintenance are replaced as a single unit, although the actual maintenance procedure may involve the movement, storage, and reinstallation of some other reactor components. In TITAN designs, the entire reactor torus is replaced as a single unit during scheduled maintenance. Furthermore, because of the small physical size and mass of the TITAN-I FPC, the maintenance procedures can be carried out through vertical lifts, allowing a much smaller reactor vault.

Table 9.11-II.

**SUMMARY OF TITAN-I REACTOR MATERIALS AND RELATED
WASTE QUANTITIES FOR CLASS-C WASTE DISPOSAL^(a)**

Component	Material	Lifetime (FPY)	Volume (m ³)	Weight (tonnes)	Annual
					Replacement Mass (tonnes/FPY)
First wall	V-3Ti-1Si	1	0.4	2.5	2.5
Blanket (IBC)	V-3Ti-1Si	1	6.4	39.2	39.2
Shield (zone 1)	V-3Ti-1Si	5	15.5	95.6	19.1
Shield (zone 2)	V-3Ti-1Si	5	28.0	172.0	34.4
OH coils	Modified steel	30	3.8	34.0	1.1
	Copper		26.6	239.0	8.0
	Spinel		3.8	15.2	0.5
	TOTAL		34.2	289.2	9.6
EF coils	Modified steel	30	43.0	315.0	10.5
EF shield	Modified steel	30	43.9	347.0	11.6
	B ₄ C		18.8	47.0	1.6
	TOTAL		62.7	394.0	13.2
Divertor shield					
zone 1	V-3Ti-1Si	1	2.3	14.2	14.2
zone 2	V-3Ti-1Si	5	6.7	41.2	8.2
TOTAL CLASS-C WASTE			199.	1363.	151.

(a) Based on operation at 18 MW/m² of neutron wall loading.

Potential advantages of single-piece maintenance procedures are identified:

1. Shortest period of downtime resulting from scheduled and unscheduled FPC repairs;
2. Improved reliability resulting from integrated FPC pretesting in an on-site, non-nuclear test facility where coolant leaks, coil alignment, thermal-expansion effects, etc., would be corrected by using rapid and inexpensive hands-on repair procedures prior to committing the FPC to nuclear service;
3. No adverse effects resulting from the interaction of new materials operating in parallel with radiation-exposed materials;
4. Ability to modify continually the FPC as may be indicated or desired by reactor performance and technological developments; and
5. Recovery from unscheduled events would be more standard and rapid. The entire reactor torus is replaced and the reactor is brought back on line with the repair work being performed, afterwards, outside the reactor vault.

The lifetime of the TITAN-I reactor torus (first wall, blanket, and divertor modules) is estimated to be in the range of 15 to 18 MWy/m², and the more conservative value of 15 MWy/m² will require the change-out of the reactor torus on a yearly basis for operation at 18 MW/m² of neutron wall loading at 76% availability. The lifetime of the hot shield is estimated to be 5 years and, therefore, to reduce the rad-waste, the TITAN-I hot shield is made of two pieces with the upper hot shield removed during the maintenance procedures and reused in the next replacement of the reactor torus.

Seventeen principal tasks must be accomplished for the annual, scheduled maintenance of the TITAN-I FPC. These steps are listed in Table 9.12-1. The tasks which would require a longer time to complete in a modular design are also identified in Table 9.12-1 (assuming the same configuration for the modular design as that of TITAN-I). Vertical lifts have been chosen for the component movements during maintenance. Lift limits for conventional bridge cranes is around 500 tonnes, with special-order crane capacities in excess of 1000 tonnes. The most massive components lifted during TITAN-I maintenance are the upper OH-coil set (OH coils 2 through 5) and the upper hot shield each weighing about 150 tonnes, which are easily manageable by the conventional cranes. The four major component lifts are illustrated in Figure 9.12-1.

An important feature of the TITAN design is the pretest facility. This facility allows the plant personnel to test fully the new torus assemblies in a non-nuclear environment

Table 9.12-I.

**PRINCIPAL TASKS
DURING THE TITAN-I MAINTENANCE PROCEDURE**

-
1. Orderly shutdown of the plasma and discharge of the magnets;
 2. Continue cooling the FPC at a reduced level until the decay heat is sufficiently low to allow cooling by natural convection in the argon atmosphere;
 3. During the cool-down period:
 - a. Continue vacuum pumping until sufficient tritium is removed from the FPC;
 - b. Break vacuum (valve-off vacuum pumps and cut weld at vacuum tank lid),^(a)
 - c. Remove vacuum tank lid to the lay-down area;
 - d. Disconnect electrical and coolant supplies from the upper OH-coil set;
 4. Drain lithium from the FPC;
 5. Lift OH-coil set and store in the lay-down area;
 6. Disconnect lithium-coolant supplies;^(a)
 7. Lift upper shield and store in the lay-down area;
 8. Lift the reactor torus and move to the hot cell;^(a)
 9. Inspect FPC area;
 10. Install the new, pretested torus assembly;^(a)
 11. Connect lithium supplies;^(a)
 12. Replace upper shield and connect shield-coolant supplies;
 13. Replace the upper OH-coil set and connect electrical and coolant supplies;
 14. Hot test the FPC;^(b)
 15. Replace vacuum-tank lid and seal the vacuum tank;^(a)
 16. Pump-down the system;^(c)
 17. Initiate plasma operations.
-

(a) The time required to complete these tasks is likely to be longer for a modular system than for a single-piece system, assuming similar configuration.

(b) The new torus assembly is pretested and aligned before commitment to service. Only minimal hot testing would be required.

(c) The TITAN-I reactor building is filled with argon gas and the replacement torus is also stored in argon atmosphere. Therefore, the pump-down time would be short.

(1) VACUUM TANK LID

(2) UPPER OH COILS

(3) UPPER HOT SHIELD

(4) TORUS ASSEMBLY

Figure 9.12-1. Four major crane lifts required for the TITAN-I maintenance.

prior to committing it to full-power operation in the reactor vault. Any faults discovered during pretesting can be quickly repaired using inexpensive hands-on maintenance. Furthermore, additional testing can be used as a shake-down period to reduce the infant mortality rate of the new assemblies. A comprehensive pretest program could greatly increase the reliability of the FPC, hence increasing the plant overall availability. The benefits of pretesting (higher reliability, higher availability) must be balanced with the additional cost associated with the pretest facility. The more representative the pretests are of the actual operation, the more duplication of the primary-loop components is required. A detailed list of pretests for the TITAN-I design is included in Table 9.12-II.

9.13. SUMMARY AND KEY TECHNICAL ISSUES

The TITAN reversed-field-pinch (RFP) fusion reactor study [1] is a multi-institutional research effort to determine the technical feasibility and key developmental issues for an RFP fusion reactor operating at high power density and to determine the potential economic (cost of electricity, COE), operational (maintenance and availability), safety, and environmental features of high mass-power-density (MPD) fusion systems.

Two different detailed designs, TITAN-I and TITAN-II, have been produced to demonstrate the possibility of multiple engineering design approaches to high-MPD reactors. Both designs would use RFP plasmas operating with essentially the same parameters. The major features of the designs are listed in Table 9.1-I. Both conceptual reactors are based on the DT fuel cycle, have a net electric output of about 1000 MWe, are compact and have a high MPD of about 800 kWe/tonne of FPC. The mass power density and the FPC power density of several fusion-reactor designs and a fission pressurized-water reactor (PWR) are shown in Figure 9.13-1 and compared with those of the TITAN reactors. The TITAN study further shows that with proper choice of materials and FPC configuration, compact reactors can be made passively safe and that the potential attractive safety and environmental features of fusion need not be sacrificed in compact reactors. The TITAN designs would meet the U. S. criteria for the near-surface disposal of radioactive waste (Class-C, 10CFR61) [33] and achieve a high level of safety assurance [30,31] with respect to FPC damage by decay afterheat and radioactivity release caused by accidents. Very importantly, a "single-piece" FPC maintenance procedure, unique to high MPD reactors, has been worked out and appears feasible for both designs.

Parametric system studies have been used to find cost-optimized designs, to determine the parametric design window associated with each approach, and to assess the sensitivity

Table 9.12-II.

MAIN PREOPERATIONAL TESTING OF THE TITAN-I FPC

Test	Sub-Module ^(a)	Module ^(a)	Full Torus ^(b)	
			No Plasma	Plasma ^(c)
Mechanical				
• Tube-bank vibration (first wall, blanket)	X	X	X	
• Tube-bank expansion (first wall, blanket)	X	X	X	
• Inter-module and full-torus deflection			X	
• Plasma chamber (shell)/coil displacement			X	
Thermal Hydraulic				
• Flow rates, pressure drops, leaks, ... :			X	
* First wall, divertor, blanket, shield	X			
* Coils			X	
* Manifolds, headers			X	
• "Hot" FPC test, (pressure drops, vibrations, ...)			X	
* Electrically heated coolant			X	
* Plasma-driven heat fluxes				X
• Remote coupling, disconnects	X	X	X	
Electrical				
• Magnet test (forces, deflection, voltages, ...)			X	X
• Vacuum-field mapping (TF ripple, vertical field, ...)			X	
• Plasma transients				
* RFP formation			X	X
* Fast-ramp phase			X	X
* Slow-ramp phase				X
• Current-drive (steady-state) phase				X
• Active feedback control			X	X
• Eddy currents (start-up, OFCD)				
* First wall and shell			X	X
* Blanket and shield			X	X
* Coil casing, structure, pumps, ...			X	X
• Termination control/response				X
Vacuum, Fueling, and Impurity-Control Systems				
• Base vacuum			X	
• Full gas-load test			X	
• Pellet injection				X
Neutronics				
• Breeding efficiency				X
• Energy-recovery efficiency				X
• Shielding effectiveness, streaming				X

(a) Performed at factory site.

(b) Performed at plant site during operational year.

(c) Performed in the reactor vault during the scheduled maintenance.

Figure 9.13-1. The MPD and the FPC power density of several fusion reactor designs, including TITAN, and a fission PWR.

of the designs to a wide range of physics and engineering requirements and assumptions. The design window for such compact RFP reactors would include machines with neutron wall loadings in the range of 10 to 20 MW/m² with a shallow minimum for COE at about 19 MW/m². The high MPD values possible for the RFP appear to be a unique attribute of this confinement concept [6]. Reactors in this "design window" are physically small and a potential benefit of this "compactness" is improved economics. Also, the cost of the FPC for TITAN reactors is a small fraction of the overall estimated plant cost (< 10%, similar to a fission PWR), making the economics of the reactor less sensitive to changes in the plasma performance or unit costs for FPC components. Moreover, since the FPC is smaller and cheaper, a development program should cost less. Even though operation at the lower end of the this design window of wall loading (10 to 12 MW/m²) is possible, and may be preferable, the TITAN study adopted the design point at the upper end (18 MW/m²) in order to quantify and assess the technical feasibility and physics limits for such high-MPD reactors.

The TITAN-I design is a lithium, self-cooled design with a vanadium alloy (V-3Ti-1Si) structural material. Magneto-hydrodynamics (MHD) effects had precluded the use of liquid-metal coolants for high-heat-flux components in previous designs (mainly of tokamaks), but the magnetic field topology of the RFP is favorable for liquid-metal cooling. In the TITAN-I design, the first wall and blanket consist of single pass, poloidal-flow loops aligned with the dominant poloidal magnetic field. Other major features are: separation of the first-wall and blanket coolant circuits to allow a lower coolant-exit temperature from the first wall; and use of MHD turbulent-flow heat transfer at the first wall, made possible by the low magnetic interaction parameter. The TITAN-I thermal-hydraulic design (Table 9.5-I) can accommodate up to 5 MW/m² of heat flux on the first wall with a reasonable MHD pressure drop, a high thermal-cycle efficiency, and a modest pumping power of about 45 MWe. A molten-salt tritium-extraction technique is used.

A unique feature of the TITAN-I design is the use of the integrated-blanket-coil (IBC) concept [14]. With the IBC concept, the lithium coolant in the blanket circuit flowing in the poloidal direction is also used as the electrical conductor of the toroidal-field and divertor coils. The IBC concept eliminates the need for shielding the coils and allows direct access to the blanket and shield assemblies, thereby easing the maintenance procedure.

The general arrangement of the TITAN-I reactor is illustrated in Figures 9.2-1 to 9.2-6. The operational (maintenance and availability), safety, and environmental issues have been taken into account throughout the design. For example, the entire FPC is contained in a vacuum tank to facilitate the remote making and breaking of vacuum

welds. All maintenance procedures would be performed by vertical lift of the components (heaviest component weighs about 250 tonnes), reducing the size of the expensive containment building. The compactness of the TITAN designs would reduce the FPC to a few small and relatively low-mass components, making toroidal segmentation unnecessary. A "single-piece" FPC maintenance procedure, in which the first wall and blanket are removed and replaced as a single unit is, therefore, possible. This unique approach permits the complete FPC to be made of a few factory-fabricated pieces, assembled on site into a single torus, and tested to full operational conditions before installation in the reactor vault. The low cost of the FPC means a complete, "ready-for-operation" unit can be kept on-site for replacement in case of unscheduled events. All of these features are expected to improve the plant availability.

All of the FPC primary-coolant ring-headers are located above the torus for ease of access during maintenance. This arrangement also ensures that the coolant will remain in the torus in the event of a break in the primary piping. The most severe safety event will be a loss-of-flow accident (LOFA). The FPC and the primary coolant loop are located in an inert-gas-filled (argon) confinement building which, together with the blanket containers and the vacuum vessel, form three barriers to prevent air influx, thereby reducing the hazards of lithium fires and providing protection for the public from radioactive materials. Lithium-drain tanks are provided for both the reactor vault and the vacuum tank to reduce passively the vulnerable blanket-lithium inventory.

A low-activation, low-after-heat vanadium alloy is used as the structural material throughout the FPC in order to minimize the peak temperature during a LOFA and to permit near-surface disposal of waste. The maximum temperature during a first-wall LOCA and system LOFA (the most severe accident postulated for TITAN-I) is 990°C. Lithium-fire accident scenarios and site-boundary dose calculations were performed to understand the potential release of radioactivity under major accident and routine release conditions. The safety analysis indicates that the liquid-metal-cooled TITAN-I design can be classified as passively safe, without reliance on any active safety systems. A high level of safety assurance [30,31] for the compact TITAN-I design, therefore, is expected.

The results from the TITAN study support the technical feasibility, economic incentive, and operational attractiveness of compact, high mass-power-density RFP reactors. The road towards compact RFP reactors, however, contains major challenges and uncertainties, and many critical issues remain to be resolved. The TITAN study has identified the key physics and engineering issues which are central to achieving reactors with the features of TITAN-I and TITAN-II.

The experimental and theoretical bases for RFPs have grown rapidly during the last few years [6], but a large degree of extrapolation to TITAN-class reactors is still required. The degree of extrapolation is one to two orders of magnitude in plasma current and temperature and two to three orders of magnitude in energy confinement time. However, the TITAN plasma density, poloidal beta, and plasma current density all are close to present-day experimental achievements. The next generation of RFP experiments [13,35] with hotter plasmas will extend the data base toward reactor-relevant regimes of operation. The TITAN study has brought out and illuminated a number of key physics issues, some of which require greater attention from the RFP physics community. These issues are discussed in detail in Section 8.

The physics of confinement scaling, plasma transport, and the role of the conducting shell are already major efforts in RFP research. However, the TITAN study points to three other major issues. First, operating high-power-density fusion reactors with intensely radiating plasmas is crucial. Confirming that the global energy confinement time remains relatively unaffected while core-plasma radiation increases (a possible unique feature of RFP) is extremely important. Second, the TITAN study has adopted the use of three "open-geometry" toroidal divertors as the impurity control and particle exhaust system. Even with an intensely radiative plasma, using an array of poloidal pump-limiters as the impurity-control system would suffer from the serious erosion of the limiter blades (and possibly the first wall). The physics of toroidal-field divertors in RFPs must be examined, and the impact of the magnetic separatrix on RFP confinement must be studied. If toroidal divertors are consistent with confinement and stability in RFPs, then high-recycling divertors and the predicted high-density, low-temperature scrape-off layer must be also confirmed. Third, early work in the TITAN study convinced the team that high mass-power-density, compact RFP reactors must operate at steady state. Current drive by magnetic-helicity injection utilizing the natural relaxation process in RFP plasma is predicted to be efficient [9,10] but experiments on oscillating-field current drive (OFCD) are inconclusive. Testing OFCD in higher temperature plasmas must await the next generation of RFP experiments, namely ZTH [13] and RFX [35].

The key engineering issues for the TITAN-I FPC have been discussed. In the area of materials, more data on irradiation behavior of V-3Ti-1Si, especially irradiation-induced swelling, are needed to confirm the materials prediction and to estimate accurately the lifetime of the TITAN-I first wall. Further creep-rupture experiments are also needed to develop more precise creep-rupture models for V-3Ti-1Si. Compatibility of vanadium-base alloy with lithium coolant and the effects of a bimetallic loop also require more experimental data. Ceramic insulators offer the potential of minimum irradiation-induced

conductivity, high melting and decomposition temperature, retention of strength, and minimum irradiation-induced swelling. Further experimental data on irradiation behavior of these insulators are needed.

The low value of the toroidal field in the RFP allows high coolant velocities to be achieved without prohibitive MHD pressure drops, thus permitting operation in the turbulent-flow regime, with the associated high heat-transfer coefficients. Further experimental data on turbulent-flow heat-transfer capability of liquid metals, especially in the TITAN-relevant operational regime of low magnetic field and high velocities, are crucial to verify the TITAN-I thermal-hydraulic design. The combined effect, if any, of the parallel and perpendicular magnetic fields on flow transition and turbulent-flow heat transfer should also be investigated. The MHD pressure drop equations for bend, contraction, and a varying magnetic field need to be substantiated by further large-scale experiments and numerical and theoretical analyses. The effect of nonuniform heat flux on the heat-transfer capability (or Nusselt number) and volumetric nuclear heating in the coolant on the film temperature drop should be further studied.

The TITAN-I poloidal-field-coil system requires little or no extrapolation of current technology. But, the TITAN-I TF and divertor IBC design encounters several critical engineering issues. The most critical issue is the design of low-voltage, high-current power supplies for these coils. The requirement of oscillating voltages and currents for the OFCD compounds the IBC power-supply issues. The copper-coil option for both TF and divertor coils, similar to the TITAN-II design, is also possible.

The design of the impurity-control system poses some of the most severe problems of any component of a DT fusion reactor, and for a compact or high-power-density design these problems can be particularly challenging. Physics operation of high-recycling toroidal-field divertors in RFPs should be experimentally demonstrated and the impact of OFCD on the divertor performance studied. Cooling of the TITAN-I divertor plate requires experimental data on turbulent-flow heat transfer in liquid-metal systems, as outlined above. Fabrication of the tungsten divertor plate remains to be demonstrated and the degree of precision needed for target shaping and control of the position of the plasma separatrix are particularly difficult tasks.

The TITAN-I molten-salt tritium-recovery process needs large-scale demonstration. Any fusion reactor with vanadium first walls may encounter the problem of plasma-driven permeation (PDP) of tritium. The extent of PDP should be experimentally investigated.

The TITAN-I design uses many safety-design features to achieve a high level of safety assurance. Further detailed analysis of the response of the TITAN-I FPC to loss-of-

flow and loss-of-coolant accidents, including lithium fires, are needed to confirm the findings. Data are needed on elevated temperatures of vanadium alloys such as the recrystallization temperature, the onset temperatures and the extent of volatilization of radioactive products in vanadium, and the formation and release of vanadium oxide, V_2O_5 . In addition, in order to qualify for Class-C waste disposal, some of the impurity elements (mainly niobium and possibly silver, terbium, and iridium) need to be controlled in the vanadium alloy to below ppm levels.

In summary, the results from the TITAN study support the technical feasibility, economic incentive, and operational attractiveness of compact, high-mass-power-density RFP reactors. It must be emphasized, nevertheless, that in high-power-density designs such as TITAN, the in-vessel components (e.g., first wall and divertor plates) are subject to high surface heat fluxes and that their design remains the most difficult engineering challenge. Also, the RFP plasma itself must operate in the manner outlined: with toroidal-field divertors, with a highly radiative core plasma, and at steady state. Future research will determine if, in fact, the physics and technology requirements of TITAN-like RFP reactors are achievable.

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10. TITAN-I FUSION-POWER-CORE ENGINEERING

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10. TITAN-I FUSION-POWER-CORE ENGINEERING

10.1. INTRODUCTION

The TITAN-I reactor is a compact, high-neutron-wall-loading (18 MW/m^2) design. The TITAN-I fusion power core (FPC) is cooled by liquid lithium and uses a vanadium alloy (V-3Ti-1Si) as the structural material. An overview of the TITAN-I design is given in Section 9. Figures 10.1-1 and 10.1-2 show the elevation view and the poloidal cross section of the TITAN-I FPC. Global parameters of the design are summarized in Table 10.1-I. A detailed list of the TITAN-I operating parameters can be found in Appendix A.

In this section, the detailed engineering design and analysis of the TITAN-I FPC is presented. The FPC components considered here are the reactor torus (including the first wall, blanket, and divertor), hot shield, and coil sets. The areas of materials (Section 10.2), neutronics (Section 10.3), thermal and structural design (Section 10.4), magnet engineering (Section 10.5), and power-cycle analysis (Section 10.6) are discussed. Other engineering aspects of the TITAN-I design, such as divertor and vacuum engineering, tritium systems, safety and waste disposal, and maintenance are presented in Sections 12 through 14.

The emphasis of the TITAN study has been on the investigation and demonstration of the feasibility of the compact, high-power-density reactors and also identification of the critical issues for these devices. Therefore, some of the engineering issues that were not considered crucial to the design were for the most part not covered in detail in this study.

10.2. MATERIAL SELECTION

The attractiveness of commercial fusion power devices depends to a large extent on material performance. Components of a fusion reactor are exposed to a unique set of stress, thermal, radiation, electromagnetic, and chemical loads and should function properly for the duration of the design lifetime. Material options are even more limited

Figure 10.1-1. Elevation view of the TITAN-I fusion power core.

Figure 10.1-2. Poloidal cross section of the TITAN-I fusion power core.

for a compact, high-power-density reactor such as TITAN because of high heat and radiation fluxes. Safety and waste-management criteria further restrict material options; the goal of the TITAN reactor study has been to satisfy Class-C waste disposal criteria and achieve a Level-2 of safety assurance (Section 13).

The following sections highlight various material selection issues for FPC components. Section 10.2.1 focuses on structural material. The TITAN-I design uses liquid lithium

Table 10.1-I.
TITAN-I OPERATING PARAMETERS

Major toroidal radius, R_T	3.90 m
Minor plasma radius, r_p	0.60 m
First-wall radius, r_{FW}	0.66 m
Primary coolant	Lithium
Structural material	V-3Ti-1Si
Coolant inlet temperature, T_{in}	320 °C
First-wall-coolant exit temperature, $T_{ex,FW}$	440 °C
Blanket-coolant exit temperature, $T_{ex,B}$	700 °C
Pumping power, P_{pump}	48.1 MWe
Neutron wall load, I_w	18.1 MW/m ²
Radiation heat flux on first wall, q''_o	4.6 MW/m ²
Fusion power, P_f	2301 MW
Total thermal power, P_{th}	2935 MW
Net electric power, P_{net}	970 MWe
Gross efficiency, η_{gross}	44%
Net efficiency, η_{net}	33%
Mass power density, MPD	757 kWe/tonne

as the primary coolant. A discussion of the compatibility of liquid lithium with the structural material is given in Section 10.2.2. In Section 10.2.3 options for insulator materials are presented. Results are summarized in Section 10.2.4.

10.2.1. Structural Material

The advantage of vanadium-base alloys over others for fusion reactor structural materials has been pointed out in previous publications [1,2]. In particular, when compared with ferritic-steel alloys, vanadium-base alloys exhibit better physical, mechanical, and nuclear properties. For example, compared to HT-9, vanadium-base alloys have a higher melting temperature, a lower thermal expansion coefficient, and a lower density. Furthermore, compared to ferritic alloys at 1 MW/m^2 of neutron wall load, vanadium-base alloys have about half the nuclear heating rate ($\sim 25 \text{ W/cm}^3$), about a third of the helium generation rate ($\sim 57 \text{ He appm}$), about half the hydrogen production rate ($\sim 240 \text{ H appm/MW y/m}^2$), and lower long-term afterheat [1].

The high melting temperature of vanadium alloys ($T_m = 1890^\circ\text{C}$) has significant bearing on safety related issues. The higher ultimate tensile strength ($\sigma_u \sim 600 \text{ MPa}$ at 600°C), the lower expansion coefficient, and the slightly higher thermal conductivity of vanadium-base alloys are reflected in higher thermal stress factors when compared to HT-9 [1]. The thermal stress factor is a measure of heat load capability. The high T_m , coupled with a high thermal stress factor, promises a high operating temperature and a high-neutron-wall-loading capability. High T_m combined with a low helium production rate is also desirable for fusion reactor materials, since below $\sim 0.5 T_m$ (K), strength and ductility are retained and fracture remains transgranular [3] ($0.5 T_m$: vanadium $\simeq 1082 \text{ K}$, HT-9 $\simeq 846 \text{ K}$). Because helium embrittlement is directly related to the helium production rate, a low helium generation rate in vanadium-base alloys is a favorable characteristic.

After consideration of several vanadium-base alloys, the V-3Ti-1Si alloy was chosen as the primary structural material for TITAN-I. Table 10.2-I shows selected properties of vanadium-base alloys. These values were adopted for the TITAN-I structural alloy throughout the study because physical properties of vanadium-base alloys are not sensitive to moderate compositional variations [1].

In the following subsections, the effects of gaseous transmutations (*i.e.*, hydrogen and helium) on the mechanical properties of vanadium-base alloys are first discussed (Section 10.2.1.1). Based on extrapolation of the limited available data to TITAN-I

Table 10.2-I.

**PHYSICAL PROPERTIES OF VANADIUM-BASE ALLOYS [1]
(ADOPTED FOR V-3Ti-1Si)**

Atomic weight	50.9
Density ^(a)	6100 kg/m ³
Crystal structure	b.c.c.
Melting temperature	1890 °C
Boiling temperature	3400 °C
Heat of fusion	143 kJ/kg
Heat of vaporization	2265 kJ/kg
Heat capacity	
at 400 °C	535 J/kg-K
at 500 °C	560 J/kg-K
at 600 °C	575 J/kg-K
Thermal conductivity	
at 400 °C	26.8 W/m-K
at 500 °C	28.0 W/m-K
at 600 °C	29.5 W/m-K
Coefficient of thermal expansion	
at 400 °C	10.2×10^{-6} /K
at 500 °C	10.3×10^{-6} /K
at 600 °C	10.5×10^{-6} /K
Electrical resistivity	
at 400 °C	0.67 μΩ m
at 500 °C	0.74 μΩ m
at 600 °C	0.81 μΩ m
Poisson ratio	0.36
Young's modulus	127 GPa

(a) Properties are at room temperature.

operating conditions, irradiation hardening and helium and hydrogen embrittlement of V-3Ti-1Si set an upper limit of approximately 18 MW y/m² for the TITAN-I first wall. Irradiation-induced swelling of the V-3Ti-1Si alloy was also investigated, and it was concluded that swelling would be negligible for the lifetime of the TITAN-I first wall. The creep behavior of V-3Ti-1Si was estimated by using minimum-commitment method, and based on the anticipated end-of-life mechanical properties of the alloy, a maximum allowable design stress was determined (Sections 10.2.1.2 and 10.2.1.3).

10.2.1.1. Embrittlement

Hydrogen generation in structural material during irradiation may cause hydrogen embrittlement. Loomis and Carlson [4] have reported that hydrogen concentrations above 5000 appm are required to raise the ductile-to-brittle transition temperature (DBTT) of vanadium above room temperature. Table 10.2-II shows anticipated hydrogen and helium production rates in the V-3Ti-1Si alloy first-wall structure exposed to an 18.1 MW/m² neutron wall loading. The hydrogen production rate in the TITAN-I first wall is slightly below the 5000 appm necessary to cause any significant increase in the DBTT following one full-power year (FPY) of operation.

Since the hydrogen concentration required for the formation of hydrides in metals is generally large, hydrogen production and pickup has not been of much concern [5]. Vanadium-hydrogen interactions are discussed in Section 10.2.2.2. In principle, vanadium forms stable hydrides only at relatively low temperatures (< 400°C). When the structure is kept at temperatures above 400°C, most of the hydrogen will migrate down the temperature gradient towards the coolant. It should be noted, however, that in addition to transmutations, tritium will also penetrate the first wall from the plasma side. Some aspects of plasma-driven tritium permeation through the first wall are discussed in Section 12.2.

Helium, unlike hydrogen, will not generally diffuse out of the metal at normal operating temperatures. Instead, helium accumulates inside the matrix to form helium-filled bubbles. These bubbles form mainly along the grain boundaries and sometimes inside the grain material. The formation of helium bubbles on grain boundaries is the basic mechanism responsible for helium embrittlement in materials exposed to high levels of neutron irradiation. Neutron irradiation increases the number of trapping sites available for nucleation of helium bubbles. The combined effects of helium generation plus irradiation-produced defects have been investigated recently [6]. Helium was pre-implanted into the vanadium-base alloy samples and then irradiated in the fast-flux test facility to up to

Table 10.2-II.

**HYDROGEN AND HELIUM PRODUCTION RATES
IN THE TITAN-I FIRST WALL (V-3Ti-1Si)^(a)**

Isotope	appm/y
H	4267
D	225
T	24
Total Hydrogen	4516
⁴ He	1018
³ He	nil
Total Helium	1018

(a) For 15 MW y/m² neutron fluence.

40 dpa. These studies were undertaken to examine and compare the response of different vanadium-base alloys to the anticipated high-energy neutron irradiation in fusion devices.

Among the various vanadium-base alloys, the most promising candidates for fusion reactor applications are V-15Cr-5Ti, VANSTAR-7 (V-9Cr-3Fe-1Zr), and V-3Ti-1Si. Previously, the V-15Cr-5Ti alloy received considerable attention [2]. In particular V-15Cr-5Ti has a higher thermal-creep resistance than the other two alloys. Investigation of the effects of neutron irradiation with pre-implanted helium atoms, however, suggests that V-15Cr-5Ti may be subject to unacceptable losses in ductility. Braski [6] irradiated specimens of V-15Cr-5Ti, V-3Ti-1Si, and VANSTAR-7 at 420, 520, and 600°C to a damage level of 40 dpa and implanted helium atoms up to a concentration of 80 appm. Tensile tests showed that helium pre-implanted V-15Cr-5Ti had zero ductility in tension after irradiation at 600°C. Under the same conditions, VANSTAR-7 and V-3Ti-1Si retained uniform elongations of about 3.3% and 8.3%, respectively. Figure 10.2-1 shows the stress-strain behavior of the candidate vanadium-base alloys before and after irradiation. These

Figure 10.2-1. Stress-strain behavior of three unirradiated (A) and irradiated (B) candidate vanadium-base alloys [6].

Table 10.2-III.

SWELLING, $\Delta V/V$ (%), OF THREE VANADIUM-BASE ALLOYS [6]^(a)

Alloy	420 °C	520 °C	620 °C
V-3Ti-1Si	0.09	0.002	0.05
VANSTAR-7	0.004	6.0	0.06
V-15Cr-5Ti	0.005	0.005	0.3

^(a) Irradiated to 40 dpa with a He content of ~ 80 appm.

measurements indicate that V-3Ti-1Si suffers the least amount of irradiation hardening and helium embrittlement.

Table 10.2-III shows the swelling results of the three vanadium-base-alloy samples with pre-implanted helium. The data, although limited, indicate that the V-3Ti-1Si alloy has the lowest swelling rates at temperatures above 500 °C after 40 dpa. Using the above test data and assuming a linear swelling behavior, the maximum swelling ($\Delta V/V$) of the TITAN-I first wall would be $\sim 0.2\%$ after 1 FPY of operation (164 dpa). Thus, a reasonable extrapolation of Braski's results [6] indicates that neutron-radiation-induced swelling of V-3Ti-1Si could be negligible.

Because the above test data show that V-3Ti-1Si has the lowest swelling, irradiation hardening, and helium embrittlement rates, this alloy is chosen as the primary structural material for the TITAN-I reactor design.

10.2.1.2. Creep behavior

Creep may be defined as the time-dependent deformation of the material under an applied load. Creep is a complex mechanism that depends on stress, strain, time, temperature, and microstructure. In a radiation environment microstructural changes are generally accelerated, and drastic effects on the creep behavior of metals can be expected. Many mechanisms which may cause creep have been identified, such as dislocation creep, diffusion creep, grain-boundary sliding, and irradiation creep. Except for the irradiation

creep mechanism, the others involve deformation of the solid by overcoming internal barriers caused by the thermal activation or applied stresses. Irradiation creep involves the production of internal stresses which are greater than the external stresses because of irradiation-induced microstructural changes. The material then creeps under the influence of this internal stress. Because of the complexity of creep deformation, designers rely heavily on empirical equations to predict creep deformation. For vanadium-base alloys, the expected service life is much longer than the duration for which experimental creep data are available. Extrapolation of the creep data, in particular the creep-rupture data, to longer times has been studied extensively over the past decades [7-9].

Widely used extrapolation techniques for creep-rupture data include the Larson-Miller, White-LeMay, and Orr-Sherby-Dorn methods [7]. These methods predict the value of the rupture time (t_r) as a function of the material temperature and for different applied stresses. The choice and success of these methods depends on the behavior of the creep-rupture data and assumed pattern for the data in each method. As a result, it is possible for these methods to predict very different values of stresses appropriate to long-life conditions [7]. To overcome this problem, the minimum-commitment method (MCM) was developed at NASA [8] to extrapolate creep-rupture data, without forcing the creep data to any specific pattern. Ghoniem *et al.* [9] developed a modified-minimum-commitment method (MMCM) in which the stress, the time to rupture, and the temperature are related by the following functional form:

$$\ln(\sigma_r) = A(T) + B(T) \ln(t_r), \quad (10.2-1)$$

where σ_r is the stress to rupture in MPa, t_r is the time to rupture in hours, and $A(T)$ and $B(T)$ are temperature-dependent parameters given by:

$$A(T) = a_0 + a_1 T \quad \text{and} \quad B(T) = b_0 + b_1 T. \quad (10.2-2)$$

The temperature (T) is in Kelvin and a_0 , a_1 , b_0 , and b_1 are constants determined by fitting creep-rupture data. The coefficients of Equation 10.2-2 for V-3Ti-1Si and V-15Cr-5Ti are given in Table 10.2-IV.

Creep-rupture stress data for vanadium alloys, in particular V-3Ti-1Si, is very limited. Some V-3Ti-1Si creep-rupture stress data [10] are available at 750 and 850°C. At 650°C, data were found only for a V-3Ti alloy containing unspecified amounts of silicon. The creep-rupture data at these three temperatures is used to develop a phenomenological stress-rupture equation (similar to Equation 10.2-1). A similar equation was also developed for V-15Cr-5Ti using more recent creep-rupture data [11].

Table 10.2-IV.
COEFFICIENTS FOR THE CREEP-RUPTURE STRESS EQUATION

Coefficient	V-3Ti-1Si	V-15Cr-5Ti
a_0	9.507209	7.239522
a_1	-3.50498×10^{-3}	-1.04844×10^{-3}
b_0	4.85748×10^{-1}	2.17853×10^{-2}
b_1	-6.07858×10^{-4}	-7.38258×10^{-5}

Figure 10.2-2 shows the experimental data points [10] and MMCM calculational results (lines) for the creep-rupture stress of V-3Ti-1Si at several temperatures, as well as the expected stress range in the first wall of TITAN-I during normal and off-normal operation. Based on the results of the MMCM extrapolation equation, operation of the first wall at a pressure close to 100 MPa and at temperatures below 700 °C will not lead to creep rupture within one year of normal operation. During off-normal conditions, coolant pressure is lost and creep rupture would not occur even if the structure were kept at elevated temperatures (1000 °C) for a prolonged period of time. However, high-temperature (850 °C) creep-rupture data are necessary to gain more confidence in the creep-rupture behavior at these higher temperatures.

Among the three candidate vanadium-base alloys, V-15Cr-5Ti has the highest resistance to thermal creep. For the purpose of comparison, a creep-rupture equation using the MMCM was also developed for V-15Cr-5Ti. Figure 10.2-3 shows the data and the extrapolated creep-rupture curves for V-15Cr-5Ti. Note that the creep-rupture stresses for V-15Cr-5Ti are well above 100 MPa at all temperatures, while those of V-3Ti-1Si fall below 100 MPa, depending on temperature and holding time.

Recovery of dislocations in vanadium-base alloys starts at ~ 800 °C. Above 900 °C, recrystallization (realignment of grain boundaries) sets in and around 1000 to 1100 °C grain growth begins. For design purposes, measurements of the 50% recrystallization temperatures are necessary (50% recrystallization temperature is the temperature at which 50% of the base material is completely recrystallized during a specified holding time). Because

Figure 10.2-2. Creep-rupture stresses of V-3Ti-1Si at various temperatures. Symbols are creep-rupture data points [10] and solid lines are estimates using MMCM [9]. Also shown are the expected stress ranges during normal and off-normal operations of the TITAN-I design.

Figure 10.2-3. Creep-rupture stresses of V-15Cr-5Ti at various temperatures. Symbols are data points [11] and solid lines are estimates using MMCM [9].

of the lack of data on these high-temperature processes, the predicted high-temperature ($> 900^\circ\text{C}$) creep behavior of V-3Ti-1Si is highly uncertain. Also, irradiation may enhance the thermal-creep component, but for lack of any data this enhancement is treated approximately, as is discussed in the following section.

10.2.1.3. Allowable design stresses

The design of structural components under high loads is based on specifications of the ASME Code Case 1592 for Class 1 Components in Elevated-Temperature Service [12]. Different allowable stresses for various stress classifications are assigned by specifying stress-intensity values for time-independent allowable stress (S_m), time-dependent allowable stress (S_t), or general primary membrane allowable stress (S_{mt}) which is taken as the lesser of S_m and S_t . The S_t is the lowest value of 2/3 of the minimum stress to cause rupture after time t , or 80% of minimum stress to cause tertiary creep after time t , or the minimum stress to produce 1% strain at time t . For the V-3Ti-1Si alloy, the creep-behavior data is too limited to base the maximum allowable stress on the 1% strain or on the tertiary-creep criteria. Therefore, the maximum allowable stresses in structural components made of V-3Ti-1Si was set at 2/3 of the minimum stress to cause rupture.

Because the creep-rupture analysis did not include any irradiation effects, an effort was made to allow for irradiation-induced loss of ductility (irradiation hardening and helium embrittlement) while establishing allowable design stresses. To incorporate these effects, a simple creep-rate equation of the form

$$\dot{\epsilon} \simeq K \sigma^n \quad (10.2-3)$$

was assumed. Here, $\dot{\epsilon}$ is the creep rate, K is a constant, σ is the applied stress, and n is the creep exponent. Using Equation 10.2-3, the irradiated and unirradiated creep behavior are related through

$$\frac{\epsilon_{irr}}{\epsilon} \simeq \left(\frac{\sigma_{irr}}{\sigma} \right)^n, \quad (10.2-4)$$

or solving for the stress of irradiated material, σ_{irr} ,

$$\sigma_{irr} \simeq \sigma \left(\frac{\epsilon_{irr}}{\epsilon} \right)^{1/n}. \quad (10.2-5)$$

Here, ϵ_{irr} and ϵ are the creep-rupture strain of irradiated and unirradiated material, respectively. Gold *et al.* [11] reported values for the creep exponent ranging from 2 to

Table 10.2-V.

**ELONGATION OF THREE VANADIUM-BASE ALLOYS
AS A FUNCTION OF HELIUM CONTENT AT 600°C [13]**

Helium (appm)	Elongation (%)		
	V-3Ti-1Si	V-15Cr-5Ti	VANSTAR-7
0	22	30	35
10	16	12	35
150	14	8	—
300	11	0	—
490	7	—	—

10 for V-3Ti-1Si alloys. For our estimates, a value of 4 is used for the creep exponent. This choice is based on experience with creep exponent values for other b.c.c structured alloys [9].

Braski [13] measured the total elongation of V-3Ti-1Si, V-15Cr-5Ti, and VANSTAR-7 as a function of implanted helium-atom content of up to 500 appm at 600°C. Table 10.2-V shows the total elongation as a function of helium content for three vanadium-base alloys. The helium embrittlement behavior of V-3Ti-1Si was extrapolated beyond the available 500-appm data. Next, the helium embrittlement data of Table 10.2-V was used to approximate the ratio of ϵ_{irr}/ϵ as a function of helium content (or irradiation time). Equation 10.2-5 was then used to modify the unirradiated stress-to-rupture values calculated previously (Figure 10.2-3) as a function of time and temperature. Figure 10.2-4 shows the calculated creep-rupture stresses as a function of time using this approach.

By taking S_t to be 2/3 the minimum stress to cause creep rupture, the maximum allowable design stress was estimated. Table 10.2-VI shows the values of S_t at 650, 750, and 850°C for the TITAN-I first wall. It should be emphasized that because of the lack of higher temperature data, the S_t values are all based on helium embrittlement data measured at 600°C.

Figure 10.2-4. Creep-rupture stress curves for V-3Ti-1Si at various temperatures estimated by MMCM [9]. Solid lines represent the creep behavior of unirradiated and dashed lines that of irradiated V-3Ti-1Si alloy. Also shown is the expected stress range during the normal operation of the TITAN-I design.

Table 10.2-VI.
MAXIMUM ALLOWABLE DESIGN STRESSES
FOR THE TITAN-I FIRST WALL^(a)

T (°C)	σ (MPa)
650	115
750	47
850	19

(a) Based on 2/3 of irradiated creep-rupture stresses
after one FPY of operation at 18 MW/m².

10.2.1.4. Lifetime analysis

Some of the issues associated with the lifetime of metallic structural materials in fusion reactors are: (1) the increase in ductile-to-brittle transition temperature (DBTT) with neutron damage, (2) irradiation-induced dimensional changes such as swelling and creep, (3) mechanical property changes such as tensile strength and toughness (irradiation hardening), and (4) helium and hydrogen embrittlement. A lifetime limit of about 200 dpa (~ 15 to 20 MW y/m^2) is generally quoted as the limit for most ferrous alloys. Because of the limited data on vanadium-base alloys, a limit of 15 to 18 MW y/m^2 was also used for the TITAN-I design (the parametric systems studies analysis of Section 3 has assumed the conservative value of 15 MW y/m^2 for the lifetime of the first wall). Analysis of the above lifetime-limiting processes are discussed in this section.

As discussed in the Section 10.2.1.1, the change in DBTT of vanadium-base alloys is expected to be negligible at high operating temperatures ($\geq 600^\circ\text{C}$). This expectation is based on the operating experience with ferritic alloys, which have the same crystal structure (b.c.c.) as that of vanadium-base alloys. Swelling caused by helium production was also addressed in Section 10.2.1.1. Among the three candidate vanadium-base alloys, V-3Ti-1Si exhibits the lowest amount of swelling. A conservative estimate of the total swelling after one FPY at 18 MW/m^2 of neutron wall loading is $\sim 0.2\%$. Thus, neither a change in the DBTT nor irradiation-induced dimensional changes would limit the lifetime of the TITAN-I design below 15 to 18 MW y/m^2 .

Neutron irradiation of metallic alloys can result in the formation and growth of new dislocation networks, resulting in an increased yield strength and decreased ductility (irradiation hardening). Figure 10.2-5 illustrates the irradiation hardening behavior of various metallic alloys. In general, V-20Ti, V-3Ti-1Si, and ferritic alloys exhibit radiation hardening below 500 °C. Above 500 °C, irradiation hardening anneals out and mechanical properties are no longer affected. Although irradiation data of V-3Ti-1Si is limited, irradiation hardening is not considered a major lifetime-limiting phenomena for this alloy, based on the TITAN-I high operating temperature and extensive experience with other b.c.c. ferrous alloys.

The effect of neutron-produced transmutations, mainly hydrogen and helium atoms, has also been addressed in Section 10.2.1.1. Three factors minimize the effects of hydrogen production in vanadium: (1) the high operating temperature ($\sim 700^\circ\text{C}$) does not allow the formation of stable vanadium hydrides, (2) the diffusion of hydrogen through vanadium is rapid at these temperatures, resulting in low levels of dissolved-hydrogen concentrations, and (3) the maximum amount of hydrogen produced during one FPY is estimated between 5,000 to 6,000 appm, which is roughly the solubility limit of hydrogen in vanadium. Based on these observations, hydrogen production in the vanadium-base-alloy TITAN-I structural material is not considered a lifetime-limiting factor.

The most severe lifetime-limiting effect is believed to be caused by neutron-generated helium atoms inside the bulk material. Helium, unlike hydrogen, does not diffuse out to free surfaces. Helium atoms are trapped inside the matrix at trapping sites, such as dislocations, voids, and precipitates. As more and more helium is generated during operation, the helium bubbles grow and cause changes in the mechanical property of the material. The most pronounced effect is a degradation of ductility generally known as helium embrittlement. The amount of helium generated during operation in the TITAN-I first wall and the accompanying effects on material properties were discussed in detail in Sections 10.2.1.1 and 10.2.1.3. Helium generation on material performance was included in the creep analysis. Based on these calculations, the maximum operating temperatures and stresses were estimated for end-of-life operating conditions.

Because of the incomplete data base on vanadium-base alloys, a lifetime limit of 15 to 18 MW y/m² is used for the TITAN-I design, which is similar to the generally quoted value for most ferrous alloys. Analysis of the lifetime-limiting processes for the TITAN-I design indicates that this value is reasonable. However, it should be noted that many effects, particularly the synergism of radiation damage and helium production, can not be modeled because of the lack of data. Furthermore, a more rigorous lifetime analysis would include fatigue and thermal shock responses of the material. The limited data base on

Figure 10.2-5. Irradiation hardening as a function of irradiation test temperature for vanadium alloys compared with similar data for austenitic and ferritic stainless steels [6].

vanadium-base alloys does not warrant such a more detailed analysis; more experimental data on vanadium-base alloys are needed.

10.2.2. Coolant Properties and Compatibility

The primary coolant of the TITAN-I FPC is liquid lithium. Liquid lithium, which also acts as the breeding material, has a low melting temperature, low density, and good thermal characteristics. Physical properties of lithium are reviewed in Section 10.2.2.1. The compatibility of liquid lithium with the structural materials should be addressed, since an attack by the coolant may lead to unacceptable failure modes of the structure. Attack by pure liquid metals differs from that of other corrosives since there is usually no chemical reaction involved. The rate of attack depends primarily on the solubility of the solid metal in the liquid metal and the solution rate. Chemical reactions become important only when impurities such as oxygen, nitrogen, and carbon are present. In Section 10.2.2.2, these attack mechanisms are discussed to establish the degree of compatibility between liquid lithium and vanadium-base alloys, particularly V-3Ti-1Si. The impact of high velocity coolant is reviewed Section 10.2.2.3.

10.2.2.1. Lithium properties

As an alkali metal, lithium requires careful handling because of its reactivity with water and atmospheric constituents (e.g., oxygen, nitrogen, hydrogen, and carbon dioxide). Because lithium will readily react with water to form gaseous hydrogen and heat, hydrogen explosions are one of the serious threats. Compared to sodium, however, lithium reacts much less vigorously with water [14]. Lithium also has a higher melting point, specific heat, and heat of fusion than sodium. Because of the experience of the fission industry with molten sodium (specifically in liquid-metal fast-breeder reactors), handling large amounts of liquid lithium should not pose a serious technological problem. Safety-related issues of lithium-cooled systems are discussed in Section 13.

The natural isotopic composition of lithium is 7.5% ${}^6\text{Li}$ and 92.5% ${}^7\text{Li}$. The ${}^6\text{Li}$ enrichment process is classified at the present time. The ${}^6\text{Li}$ isotope is a U. S. government-controlled substance and only research quantities of about 5 to 10 g can be purchased at a time. The cost of ${}^6\text{Li}$ (95 to 96 at.%) is ~ \$1,250 per kg; ${}^7\text{Li}$ (99.9 at.%) is ~ \$500 per kg, while natural lithium costs ~ \$30 per kg (1987 prices). The highest available enrichment of ${}^6\text{Li}$ is 95.2 at.% (94.82 wt.%). These figures were quoted by the Isotope Sales Office of the Oak Ridge National Laboratory (ORNL) [15]. As for the impurity

levels in lithium, the ORNL Isotope Sales Office has made chemical and spectroscopic analyses of batches of ^6Li and ^7Li . Typical impurity levels are given in Table 10.2-VII, and the chemical analysis results are shown in Table 10.2-VIII. All measurements were taken from lithium batches of April and May of 1987.

Lithium is used in many areas of medicine and electronics. Therefore, a nearly complete set of properties is available. Table 10.2-IX lists some of the physical properties of lithium. Among the interesting features of lithium is that it has about half the density of water and a boiling point of 1347°C . The low density of lithium minimizes pumping power while the high boiling point makes low-pressure systems possible. The *Materials Handbook for Fusion Energy Systems* [14] reports many relevant properties in the form of plots and analytical equations. Table 10.2-X lists some of these correlations.

10.2.2.2. Lithium attack

Structural materials in contact with liquid metals can experience some form of attack which, depending on the severity, can lead to degradation of properties and ultimately to failure. The major types of liquid-metal attack are as follows:

- Simple solution,
- Liquid-metal penetration,
- Temperature-gradient mass transfer,
- Liquid-metal embrittlement,
- Dissimilar-metal mass transfer,
- Impurity reactions (oxygen, nitrogen, carbon, hydrogen).

Simple solution attack. In simple solution attack, solid metal dissolves in the liquid metal to the extent of its solubility. It can result in uniform thinning of the material or cause intergranular penetration if a single constituent is preferentially leached from the solid. During the late 1950s and early 1960s, forced-circulation-loop tests with vanadium and liquid lithium were conducted as part of the space-power program [16]. Tests were run with unalloyed vanadium for up to 1170 hours. The lithium flow velocity was 4 m/s with a maximum temperature of 870°C . Reported corrosion rates for vanadium were *nil*. The BCSS [2] interpreted the reported value as $\sim 0.1\text{ }\mu\text{m/y}$, based on a reported

Table 10.2-VII.

IMPURITY LEVELS (appm)^(a) IN ${}^7\text{Li}$ (99.98 at.%) AND ${}^6\text{Li}$ (95.2 at.%)

Metal	Symbol	${}^7\text{Li}$	${}^6\text{Li}$	Metal	Symbol	${}^7\text{Li}$	${}^6\text{Li}$
Silver	Ag	1	1	Niobium	Nb	10	10
Aluminum	Al	10	10	Nickel	Ni	10	10
Arsenic	As	40	40	Lead	Pb	4	4
Gold	Au	4	4	Palladium	Pd	10	10
Barium	Ba	10	10	Rubidium	Rb	40	40
Cadmium	Cd	6	6	Rhodium	Rh	20	20
Cobalt	Co	20	20	Ruthenium	Ru	40	40
Chromium	Cr	1	1	Antimony	Sb	20	20
Cesium	Cs	40	40	Silicon	Si	20	20
Copper	Cu	20	50	Tin	Sn	20	20
Iron	Fe	20	20	Strontium	Sr	10	10
Gallium	Ga	10	10	Tantalum	Ta	40	40
Germanium	Ge	10	10	Thorium	Th	100	100
Hafnium	Hf	10	10	Titanium	Ti	10	10
Indium	In	40	40	Thallium	Tl	10	10
Iridium	Ir	60	60	Thulium	Tm	4	4
Lutetium	Lu	4	4	Vanadium	V	10	10
Magnesium	Mg	30	100	Tungsten	W	100	100
Manganese	Mn	1	1	Yttrium	Y	6	6
Molybdenum	Mo	4	4	Zirconium	Zr	10	10

(a) Maximum levels from spectroscopic analysis [15].

Table 10.2-VIII.
NONMETALLIC IMPURITY LEVELS^(a) IN ^7Li AND ^6Li

Substance	^7Li	^6Li
Calcium (appm)	67	167
Potassium (appm)	< 40	< 50
Sodium (appm)	88	49
Nitrogen (appm)	174	1561
Carbon (appm)	-	645
Chlorine (wt.%)	0.004	0.002
Heavy metals (wt.%)	0.01	0.018

(a) Chemical analysis results [15].

minimum corrosion rates of other alloys [2]. Capsule tests conducted at ORNL [17] and forced-circulation tests at Argonne National Laboratory [18] later confirmed the corrosion resistance of vanadium-base alloys to liquid lithium. Although the solubility of vanadium in lithium was not reported, these results indicate negligible vanadium solubility.

Liquid-metal penetration. The presence of impurities such as oxygen, carbon, and nitrogen in solid metals has been found to influence the compatibility of alloys with liquid alkali metals [19]. Liquid-metal penetration (LMP) has been observed to occur when the solid metal contains more than a certain threshold impurity concentration [19-21]. Rapid penetration along grain boundaries can occur. While vanadium was found not to be susceptible to LMP, a 1-mm specimen of tantalum containing 600 appm of oxygen was completely penetrated by lithium after exposure for 0.5 h at 600 °C [21]. This penetration phenomenon has been found in a number of refractory metals exposed to lithium, sodium, potassium, and the sodium-potassium eutectic, NaK [21-23].

While in refractory metals the oxygen concentration affects the LMP, in iron the carbon concentration is the most important factor. Iron containing small amounts of

Table 10.2-IX.
PHYSICAL PROPERTIES OF LITHIUM [2,14]

Atomic weight	6.94
Density (room temperature)	530 kg/m ³
Melting temperature	180.5 °C
Boiling temperature	1347 °C
Heat of fusion	432 kJ/kg
Heat of vaporization	19,595 kJ/kg
Heat capacity	
at 350 °C	4.24 kJ/kg-K
at 500 °C	4.17 kJ/kg-K
at 650 °C	4.16 kJ/kg-K
Thermal conductivity	
at 350 °C	46.8 W/m-K
at 500 °C	49.6 W/m-K
at 650 °C	52.4 W/m-K
Surface tension	
at 350 °C	0.373 N m
at 500 °C	0.350 N m
at 650 °C	0.325 N m
Electrical resistivity	
at 350 °C	0.311 μΩ m
at 500 °C	0.353 μΩ m
at 650 °C	0.395 μΩ m
Viscosity	
at 350 °C	0.41 cP
at 500 °C	0.32 cP
at 650 °C	0.27 cP

Table 10.2-X.
ANALYTICAL EQUATIONS FOR SELECTED Li PROPERTIES [14]

Property	Temperature Range	Expression
Specific heat (kJ/kg-K)	$453 < T < 692 \text{ K}$	$c_P = 4.3014 - 8.382 \times 10^{-4}T$
	$693 < T < 1173 \text{ K}$	$c_P = 4.1876 - 7.330 \times 10^{-5}T$
Enthalpy (MJ/kg)	$463 < T < 923 \text{ K}$	$H_T = 1.1321 + c_P(T - 453.6)$
	$923 < T < 1573 \text{ K}$	$H_T = 1.1227 + 4.19 \times 10^{-3}T$ $-21.66/(T + 273)$
Electrical resistivity ($\mu\Omega \text{ m}$)	$473 < T < 1673 \text{ K}$	$\rho = 0.1575 + 2.197 \times 10^{-4}T$ $+6.253 \times 10^{-8} T^2$ $-2.504 \times 10^{-11}T^3$
	$473 < T < 973 \text{ K}$	$\log \eta = 1.4936 + 109.95/T$ $-0.7368 \log T$
Viscosity (cP)	$973 < T < 1473 \text{ K}$	$\log \eta = 726.07/T - 1.338$
	$473 < T < 1873 \text{ K}$	$d = 504.6 - 0.101T$
Density (kg/m^3)	$473 < T < 1373 \text{ K}$	$k = 34.907 + 0.0191T$
Thermal conductivity (W/m-K)	$700 < T < 2000 \text{ K}$	$\log P = 4.8831 - 7877.9/T$
Vapor pressure (atm)	$473 < T < 1673 \text{ K}$	$\gamma = 0.16(3550 - T) - 95$
Surface tension (dyn cm)		

carbon was penetrated intergranularly by lithium [24-26]. The cause of corrosion was attributed to the reaction of lithium with the carbon in the grain boundaries. Corrosion-resistant niobium and tantalum alloys have been developed through the addition of minor amounts of reactive metals such as Zr and Hf. However, conflicting results are reported in the literature regarding the susceptibility of metals to LMP [24,27,28]. Not only are the mechanisms of the penetration not clear yet, but irradiation is expected to complicate this phenomena further. Fusion reactors that use liquid metals as coolant invariably have a large coolant-structure surface area and, thus, are susceptible to LMP. In conjunction with corrosion, transgranular penetration of vanadium-base alloys by liquid lithium has been investigated during the capsule tests performed at ORNL [17]. It was found that, unlike niobium and tantalum, vanadium did not suffer from any severe penetration rate regardless of whether oxygen was present or not [2].

Temperature-gradient mass transfer. The low solubility of structural materials in the coolants makes simple solution of most metals negligible. However, when a temperature gradient exists, such as in a coolant loop, the solid metal approaches saturation in the hotter zone only to crystallize out in the cooler regions because of the decrease in solubility. This effect sets up temperature-gradient mass-transfer mechanism that can ultimately result in tube plugging in the cold leg of a coolant loop.

Accumulation of deposits in heat exchangers can adversely affect their hydraulic performance by reducing the flow area. Furthermore, the heat-transfer resistance can increase because of the inferior thermal conductivity of the deposited scale compared to that of the base metal. An annual reduction of nearly 10% in the overall heat-transfer coefficient of the intermediate heat exchanger (IHX) is estimated from loop-corrosion data in sodium-stainless-steel systems of liquid-metal breeder reactors [29].

Radioactive constituents can become immobilized in the corrosion products and be deposited on the cooler surfaces of the loop, such as the IHX. This can lead to radioactivity levels sufficiently high to impair routine maintenance of components in the cold leg of a primary loop. The fission industry has, therefore, introduced a maximum mass-transfer limit range for sodium-steel systems of 5 to 20 $\mu\text{m}/\text{y}$ corresponding to flow restriction and 0.5 $\mu\text{m}/\text{y}$ for the radioactive mass transport. Applying these limits to liquid-lithium-cooled vanadium-base alloys may be overly conservative, and detailed investigation is required to establish similar guidelines for fusion applications.

Because of the low corrosion rates of vanadium-base alloys ($< 0.1 \mu\text{m}/\text{y}$ at 860°C [16]), the transport of structural material from high-temperature regions and consequent deposition at the cold leg of the primary loop does not seem to be a major concern, even

by fission-industry guidelines. However, an additional complication may arise from the presence of strongly varying magnetic fields which can impact the deposition of ferromagnetic substances. Such an effect has not yet been analyzed in sufficient detail to be incorporated in the fusion reactor studies.

Liquid-metal embrittlement. Liquid-metal embrittlement is caused by a reduction in bond strength of the atoms in a solid by chemisorption of a liquid atom at the tip of a crack or at the end of a pileup of dislocations near a surface obstacle. Attachment of an impurity atom reduces the stress required for cleavage. Thus, normally ductile materials may fail in a brittle manner by cleavage or by rapid intergranular crack propagation [30]. Ammon [19] reported that vanadium-base alloys are not susceptible to liquid-metal embrittlement by lithium.

Dissimilar-metal mass transfer. Since vanadium is an expensive material, it would be advantageous to use steels as the structural material of the primary-loop piping outside the FPC where the structural material is not subjected to extreme radiation and heat loads. When dissimilar metals are in contact with the same liquid metal, one of the metals may be transferred through the liquid metal to alloy with the other metal. Removal of the soluble metal from solution by this method ensures that saturation is never attained. This can lead to unacceptably high levels of corrosion even when the solubility of a metal in the liquid metal is very low. An example of this rapid attack is the rapid dissolution of SS-304 stainless steel by molten lithium in an iron container [31,32]. However, vanadium-steel bimetallic-loop tests have not reported a dissimilar-metal transfer problem [33,34].

Impurity reactions. Another effect of having dissimilar metals in contact with the same liquid metal is the transfer of nonmetallic constituents between the two alloys. This process is generally referred to as the bimetallic impurity pickup, which is believed to be a major concern in systems that use vanadium-base alloys as the primary structural material, and a ferrous alloy for the remainder of the loop. Steels may contain large amounts of nonmetallic additions such as carbon, nitrogen, and oxygen. These additions reside in steel, mostly in the form of interstitials, each with characteristic diffusion coefficients. The formation of lithium compounds (e.g., Li_3N , Li_2C_2 , Li_2O) can effectively leach these nonmetallic elements from the steel. Since 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 to the vanadium-base alloy. The lithium

oxide, Li_2O , is more stable than the oxides of vanadium at all temperatures, therefore transfer of oxygen from the steel to the vanadium does not occur.

To explain impurity reactions, DeVan and Klueh [35] have conducted an extensive analysis of the thermodynamics of interstitial interactions with a number of refractory-metal and lithium systems. The impurity reactions were analyzed in terms of equilibrium distribution coefficients, which are the ratio of the impurity-element concentrations in the solid metal to those in the liquid metal as a function of temperature. Distribution coefficients less than unity result in a tendency for interstitials to be transferred from the solid to the liquid metal, causing a reduction in strength of the alloy. Distribution coefficients larger than unity indicate a net transfer of impurities from the liquid to the solid, which may lead to embrittlement of the metal. For a vanadium-lithium system, the distribution coefficients of nitrogen and carbon are well above unity for the temperature range of interest (500-1200°C), while the distribution coefficient of oxygen and hydrogen is below unity.

Oxygen. Because in a vanadium-lithium system the oxygen equilibrium distribution coefficient is much less than unity ($< 10^{-4}$), oxygen will be lost to lithium. Oxygen added as a solution hardener to a vanadium-base alloy could be depleted by liquid lithium, leading to a reduction of strength. The rate of loss of oxygen would depend primarily on the temperature gradient across the metal. Investigation by Schmidt and Warner [36] and Powers and Doyle [37] show that the diffusion coefficient of oxygen in vanadium, D_O^V (cm^2/s), can be estimated by the following equation:

$$D_O^V = (0.0246 \pm 0.0031) \exp \left(-\frac{29,495 \pm 110}{RT} \right), \quad (10.2-6)$$

where R is the gas constant (1.986 cal/K mol) and T is temperature (K).

In contrast to the vanadium-lithium system, the oxygen equilibrium distribution coefficient of a sodium-steel system is greater than unity; oxygen will be picked up from sodium by steel, leading to the formation of soluble iron oxides.

Nitrogen. Nitrogen has a distribution coefficient above unity and, therefore, will be picked up by vanadium from liquid lithium. The nitrogen-pickup rate is determined by various factors such as solubility, vanadium-nitrogen phase reaction, surface layer, and temperature. The solubility limits of nitrogen in vanadium are well known and are given in Table 10.2-XI. The diffusion coefficient of nitrogen in vanadium, D_N^V (cm^2/s), was reported by Schmidt and Warner [36] as follows:

$$D_N^V = (0.0417 \pm 0.0034) \exp \left(-\frac{35,459 \pm 111}{RT} \right). \quad (10.2-7)$$

Table 10.2-XI.

SOLUBILITY EQUATIONS FOR VANADIUM-NITROGEN SYSTEM [19]

Solid Solubility Limit (at.%)	Temperature Range (K)
$C_N = 50.0 \exp(-5,500/RT)$	$548 < T < 848$
$C_N = 33.0 \exp(-3,700/RT)$	$873 < T < 1473$
$C_N = 31.5 \exp(-3,800/RT)$	$773 < T < 1773$

Experiments performed by Gulbransen and Andrews [38] showed that the rate of weight gain of vanadium exposed to nitrogen gas between 600 and 900°C was insensitive to the nitrogen pressure. This indicates the formation of a nitride layer on the surface. An activation energy of 31.4 kcal/mol for the formation of vanadium nitride and a migration energy of 35.5 kcal/mol for nitrogen through vanadium was reported. The small difference between the nitride-formation and nitrogen-migration energies indicates a preference to form a nitride layer rather than simple diffusion of nitrogen through vanadium. The rate of weight gain was found to follow a parabolic behavior [38] which is indicative of a saturation process. Saturation of the surface layer with nitrogen can lead to the formation of a barrier and consequently a reduction of nitrogen pickup. Subsequent investigators [39] confirmed these findings and reported the formation of V_3N and VN . Above 800°C, the parabolic behavior of the rate of weight gain was also observed.

Recently Adelhelm *et al.* [33] studied the corrosion behavior of the vanadium-base alloy V-3Ti-1Si in a pumped lithium-stainless-steel loop at 533 to 550°C for up to 2056 hours. They measured the nitrogen and carbon pickup of V-3Ti-1Si as a function of time. The nitrogen uptake was correlated to a parabolic pickup behavior as

$$C_N^{up} = 0.024 + 2.2 \times 10^{-3} t^{0.59}, \quad (10.2-8)$$

where C_N^{up} is the nitrogen uptake in wt.% and t is the exposure time in hours. Metallographic investigations revealed a very thin (2-5 μm), dark-gray layer in exposed coupons of V-3Ti-1Si specimen which had undergone a marked titanium enrichment and a strong vanadium loss. Across this dense surface layer, the titanium concentration increased from ~ 3 to 11 wt.% and the vanadium concentration decreased from ~ 90 to ~ 75 wt.%.

The layer was identified as a vanadium-titanium-nitride compound, $(V,Ti)_xN$, with a stoichiometric parameter, x , between 1.55 and 1.67. An interesting observation was that the maximum impurity reactions were measured at an intermediate time and not after the longest exposure time.

This unusual behavior was easily explained by examining the nitrogen content of the lithium. It had been reported earlier by Olson *et al.* [40] that the nitrogen content of the liquid lithium influences the corrosion rates in lithium-stainless-steel systems. For the V-3Ti-1Si-lithium system, Adelhelm *et al.* [33] found that the lowest corrosion rate occurred for a nitrogen concentration of 30 wppm during the 2056-hour test and not for those tests with nitrogen concentrations of 8 wppm for 362 hours or 64 wppm for 1073 hours. This observation indicates that a protective nitride layer forms at an optimum nitrogen concentration of about 30 wppm. Furthermore, examination of the nitride layers at these three different nitrogen concentrations reveals that the nitrogen content influenced both the thickness and the degree of porosity of the layer. At a nitrogen content of 8 wppm, the layer showed the highest degree of porosity, while at 30 wppm the layer had the highest density. The very compact layer without any porosity at high nitrogen content, therefore, acts as a good corrosion-resistant protection barrier. The measured corrosion rates for the V-3Ti-1Si-lithium loop ranged between 10^{-3} and 10^{-2} g/m² h (or 1.5-15 μ m/y) depending on the nitrogen content, with the highest corrosion rate at the lowest nitrogen content of 8 wppm.

Structural identification of the nitride layer was not performed by Adelhelm *et al.* [33]. However, Verkhoglyadova *et al.* [39] identified hexagonal cubic-packed (h.c.p.) V_3N and face-centered cubic (f.c.c.) VN phases in experiments with vanadium exposed to nitrogen gas at 101.3 kPa for up to 4 hours in the temperature range of 500 to 1500 °C. In general, radiation-damage resistance of h.c.p. structured materials is not as good as that of f.c.c. structures because of the geometric anisotropy in h.c.p. crystals (crystal is longer in one direction, leading to anisotropic dimensional changes). Therefore, in an intense radiation environment, as expected in the first wall of a fusion device, the structural integrity of the protective layer is questionable. However, since the layer forms during operation, a "self-healing" mechanism would be repairing the damaged nitride layers continuously.

The corrosion behavior of vanadium alloys in flowing lithium as a function of nitrogen content was also investigated by Chopra *et al.* [34]. Vanadium-alloy specimens were exposed to lithium at 427, 482, and 528 °C. Lithium was circulated at 1 lit/min with runs of 1500 to 2500 hours. The dissolution rates of the various vanadium alloys exposed to lithium at 482 °C decreased in the following order: pure V, V-3Ti-1Si, V-5Ti-(12 to 15Cr), V-(10 to 15Cr)-3Fe-1Zr, and V-(15 to 20Ti)-7.5Cr. They reported

that only the alloys containing 15 to 20 Ti developed a protective nitride scale. Contrary to Adelhelm's findings, the nitrogen pickup rate reported by Chopra *et al.* did not show a parabolic behavior. The effect of carbon on the dissolution rate of vanadium alloys was also investigated by Chopra *et al.* [34]. All vanadium alloys picked up carbon and nitrogen and lost oxygen in lithium. They concluded that concentrations of 20 to 50-wppm nitrogen and 8 to 12-wppm carbon in lithium are above acceptable limits for vanadium-alloy-lithium systems. Table 10.2-VIII lists typical nitrogen contents of ^6Li and ^7Li . These nitrogen contents are well above the levels used in the Adelhelm and Chopra experiments.

The above discussion indicates that the behavior of vanadium-base alloys in liquid lithium depends strongly on the chemistry of the lithium. Variations in the amount of the dissolved nitrogen can affect the formation rate and stability of the vanadium-nitride layer. Therefore, control of liquid-lithium chemistry is crucial for bimetallic loops. More experimental data on this subject are required.

Carbon. The solubility of carbon in vanadium appears to be very low at temperatures less than 1200 °C, based on the phase diagram for the vanadium-carbon system [41]. However, the equilibrium distribution coefficient of carbon between lithium and vanadium is about 10^4 at 800 °C and increases to 10^8 at 400 °C [5]. This large distribution coefficient indicates that carbon is readily absorbed from liquid lithium by vanadium. Thus, carbon pickup will always be an issue when vanadium is used as a structural material. The diffusion of carbon in vanadium, D_C^V (cm²/s), has been reported [36] as

$$D_C^V = (0.0088 \pm 0.0018) \exp \left(- \frac{27,792 \pm 180}{RT} \right). \quad (10.2-9)$$

Adelhelm *et al.* [33] measured the carbon uptake of V-3Ti-1Si exposed to liquid lithium at 550 °C containing a few wppm of carbon. Carbon contents were calculated based on solubility data and also the data from titanium gettering. While the nitrogen concentration in vanadium was found to have increased by a factor of 10, the carbon concentration only increased by a factor of 1.5. The carbon uptake showed a linear relationship for both the hot (550 °C) and cold (533 °C) legs of the test loops. The estimated pickup rates were given as:

$$c_c = 0.0424 + 7.31 \times 10^{-6} t, \quad (10.2-10)$$

for the hot leg, and

$$c_c = 0.0428 + 6.64 \times 10^{-6} t, \quad (10.2-11)$$

for the cold leg, where c_c is the carbon concentration of the vanadium foils (in wt.%), and t is the exposure time in hours. The low carbon pickup rate results from either: (1) a (V,Ti)_xN layer formed and served as a physical barrier to carbon, or (2) the use of austenitic stabilized steel minimized the supply of carbon.

Hydrogen. The vanadium-hydrogen (V-H) system has been studied to some extent in recent years. Veleckis and Edwards [42] investigated the range of 246 to 554°C and developed a semiempirical equation for the P-C-T isotherms of a high purity vanadium-hydrogen system:

$$\sqrt{P} = K \frac{r}{s-r} \exp \left(\frac{1}{RT} \sum_{i=0}^4 A_i r^i \right), \quad (10.2-12)$$

where P is the hydrogen pressure in atmospheres, r is the hydrogen-metal atom ratio, and K , s , and A_i are parameters given in Table 10.2-XII. Measurements of the solubility of H₂ and D₂ in vanadium over the temperature range 400 to 800°C by Mareche *et al.* [43] agree with those reported by Veleckis and Edwards [42]. Mareche *et al.* also reported no isotope effect for D₂ in vanadium. Thus D₂ and H₂ exhibit the same solubility as a function of pressure and temperature.

The diffusion coefficients of hydrogen (D_H^V) and deuterium (D_D^V) were measured over the temperature range of 110 to 930°C by Cantelli *et al.* [44] and found to be

$$D_H^V = (4.4 \pm 1.5) \exp \left(-\frac{0.059 \pm 0.007}{kT} \right), \quad (10.2-13)$$

$$D_D^V = (3.1 \pm 0.8) \exp \left(-\frac{0.073 \pm 0.006}{kT} \right), \quad (10.2-14)$$

where D_H^V and D_D^V are in cm²/s and k is the Boltzmann constant.

Hydrogen embrittlement is one of many forms of hydrogen attack on a metals. In general, refractory metals are embrittled by hydrogen, primarily by hydride-phase formation. However, vanadium, niobium, tantalum, and their alloys do not form stable hydrides, unlike titanium and zirconium which suffer severe hydrogen embrittlement [45]. Loomis *et al.* [4] report that the maximum solubility level of hydrogen in vanadium alloys is about 5000 appm. Once the hydrogen content exceeds terminal solid solubility, damage may proceed by hydride-precipitate formation. Three sources of hydrogen in the first-wall structure are possible: (1) neutron reactions with vanadium, (2) plasma-driven permeation, and (3) tritium in the liquid lithium. The latter source of hydrogen (from lithium) is not a concern, since the equilibrium distribution coefficient of hydrogen between vanadium and lithium is well below unity ($\sim 10^{-2}$ to 10^{-3} for a temperature range

Table 10.2-XII.
PARAMETERS OF EQUATION 10.2-12 [42]

Parameter	Value
K	801
s	0.779
A_0	-6.93×10^3
A_1	-6.50×10^3
A_2	5.09×10^3
A_3	-1.51×10^3
A_4	-9.57×10^3

of 900 to 400 °C). The formation of lithium hydride, therefore, is preferential over the formation of vanadium hydrides. Experiments [45] have also shown that in vanadium alloys containing 5 to 20 Ti, ductility is restored to values of uncharged samples when the temperature is raised above 250 °C. Therefore, hydrogen embrittlement of vanadium through hydride formation is believed not to be a problem at TITAN-I operating temperatures.

10.2.2.3. Effects of high-velocity coolant

In the TITAN-I design, the liquid-lithium coolant flows at a high velocity of 21 m/s in the first-wall channels. The effects of velocity on corrosion rates are complex and depend on the characteristics of the metal and the environment. Velocity affects corrosion through two distinct mechanisms: (1) agitation of reaction constituents can increase reaction rates, and (2) the increased momentum of fluid particles can lead to an increase in wear (i.e., erosion). Increased reaction rates are generally found in aqueous solutions, where the concentration of cations and anions play a large role in corrosion rates. In general, liquid metals do not interact chemically with solid surfaces and, therefore, the

effects of velocity on corrosion rates of vanadium alloys in a liquid-lithium environment fall mostly into the second category.

Wear by erosion can be caused by intense pressure or shock waves traveling in the fluid. Constant impingement of high-velocity particles on metallic surfaces gradually leads to strain hardening of the material. Strain-hardened surfaces are less ductile than the base material. This situation can result in the formation of micro-cracks in the surface layer when the metal is exposed to varying stresses during operation. Rapid flow of the coolant can also cause erosive shear of the surface. When maximum surface elongation has been attained, transgranular micro-cracks occur. After formation, micro-cracks may grow and propagate, resulting in an overall loss of surface strength and consequent removal by the flowing medium.

The surface strain-hardening rate is determined by an erosion "incubation time" which tends to become longer with increasing base-metal hardness. Surface treatment, such as sulfur-nitriding, leads to hardening of the metal surfaces and improves erosion resistivity by "interesting" proportions [46]. The presence of nitrogen in liquid lithium can result in the formation of a complex vanadium-titanium-nitride layer in V-3Ti-1Si, as discussed before. This formation will result in an increased surface hardness which is beneficial for erosion resistance.

A literature search regarding erosion by liquid lithium showed that this issue has not been investigated in any detail, specifically for vanadium alloys. Most of the research regarding erosion deals with water-steel systems, particularly distinguishing between particle-free or particle-containing water or slurry. Some of the findings regarding the effects of velocity of a water-metal system can be applied to many other fluid-metal systems [46-51]. These findings are summarized below.

Wear as a function of time. For long periods of exposure, the weight of a metal worn away is approximately a linear function of time. Velocity is an essential parameter as is the angle of impact of the fluid against the wall. For ordinary E-26/2 steel ($\sim 0.20\%$ C) at room temperature, erosion after a time t (in hours) may be expressed as follows:

$$e_m = 9 \times 10^{-13} v^5 \sin^2 \alpha, \quad (10.2-15)$$

where e_m is the erosion depth in mm, v is the fluid velocity in m/s, and α is the impact angle. For example, if $v = 40$ m/s and the average impact angle is about 10° , then the erosion rate would be about 0.02 mm/y.

Equation 10.2-15 is specifically measured for the E-26/2 steel exposed to room temperature water. Experiments with steels containing 0.3% to 25% chromium or between 0.3% and 80% cobalt resulted in a much lower erosion rate given by:

$$e_m = \frac{2 \times 10^{-13} v^5 \sin^2 \alpha}{(Cr + Co)}, \quad (10.2-16)$$

where Cr and Co are, respectively, the chromium and cobalt content in the steel in percent. Equation 10.2-16 shows that the addition of chromium and cobalt has not changed the dependence of the erosion rate on the velocity or the impact angle, but rather has led to a drastic reduction in the coefficient of the erosion rate. Under the same conditions of $v = 40$ m/s and a 10° impact angle, the erosion rate is now only about 0.007 mm/y for steels containing 0.3% Cr and 0.3% Co, versus 0.2 mm/y for the E-26/2 steel.

Threshold velocity for erosion. Equations 10.2-15 and 10.2-16 show that the erosion rate strongly depends on the fluid velocity. However, for most materials a threshold velocity exists, below which no measurable erosion rate is detected. This threshold velocity depends mostly on the strength (hardness) of the material in contact with the liquid. Threshold velocities are determined experimentally by performing jet-impact experiments where the sample is subjected to the impact of a high-velocity water jet for varying periods of time and impact angles. Threshold velocities are generally quoted for impact angles of 90° (perpendicular impact). For steels containing 12% chromium, the threshold velocity has been measured to be between 100 and 120 m/s. Because of high threshold velocities, the loss of weight for tangential flow (coolant flowing parallel to the material) can be negligible compared to perpendicular coolant impact. Experiments show that the ratio between tangential and perpendicular erosion rates is about 10^{-4} . This ratio was determined by examining the erosion rate on two different locations along a tube with a 90° bend. To reduce the effects of high-velocity fluids, therefore, sudden changes in flow patterns caused by sharp bends or extraction-contractions should be avoided.

Erosion of hard surfaces. Exposure of vanadium alloys to liquid lithium invariably results in hardening of the surface because of interstitial pickup by the vanadium alloy. These impurities form carbides and nitrides which produce a ceramic-type surface layer in contact with the fluid [33]. Therefore, it is necessary to investigate the effects of high-velocity fluids on erosion of brittle materials. Researchers have been concerned with

Table 10.2-XIII.

THRESHOLD VELOCITIES OF SEVERAL BRITTLE MATERIALS [50]

Material	Velocity ^(a) (m/s)
Zinc sulfide	125
Soda-lime glass	150
Germanium	150
Sapphire	350
Silicon nitride	500

(a) Minimum velocity of a 0.8-mm jet to cause fracture in the sample.

a range of infrared-transmitting materials (e.g., zinc-sulfide, germanium, sapphire) and other ceramics such as Al_2O_3 and soda-lime-silicate glass. Results of erosion-corrosion threshold velocities with 90° jet-impact experiments of several brittle materials are given in Table 10.2-XIII. These threshold velocities reflect the minimum velocity of a jet to cause stress fractures. It is assumed that after fracture the material becomes highly susceptible to erosion by the high-velocity fluid.

These findings suggest that relatively thin coatings of micron thickness and high modulus of elasticity could be beneficial in protecting substrate solids from damage by high-velocity fluid impact. Thin coatings can lead to a sizeable reduction of substrate stresses and hard coatings improve the threshold conditions for damage. Ball-indentation experiments on carbon-coated germanium have shown that a coating of 1 to 3 μm can increase the critical load for ring-crack formation by as much as 100% to 200%, respectively. To investigate the effect of temperature on the velocity threshold for erosion of brittle material, Hockey *et al.* [51] have investigated the erosion rate of Al_2O_3 and soda-lime-silicate glass as a function of temperature between 25 and 1000°C, using SiC particles of 3 to 5 μm and 30 μm in size. Particle velocities up to 125 m/s and impact angles ranging from 15° to 90° were used. It was concluded that temperature has little effect on the erosion rates of silicon nitride, polycrystalline aluminum oxide, and soda-lime-silicate glass.

In addition to the use of coatings, surface modification by ion implantation of titanium, zirconium, or yttrium has proven to be highly beneficial against erosion. For example, abrasion by fast-moving hard particles (sand particles in fuel oil) was reduced by a factor of 10 after titanium was implanted into the steel surface. A similar effect could be present in V-3Ti-1Si in which the Ti close to the surface may act as such a hardener.

Using the results of water-steel jet-impact experiments, the effects of high-velocity fluids on the erosion rate of metallic materials can be summarized as follows:

- Erosion rate is a linear function of time, strongly depends on the fluid velocity (v^5), and scales as the square of the $\sin$ of the impact angle;
- Hardening of the surface reduces the erosion rate significantly;
- The threshold for reduction of fracture stress of most ceramics is above a fluid-impact velocity of about 120 m/s;
- The incubation time for erosion of strengthened steel (12% Cr) for fluid velocities below 100-120 m/s is long. For all practical purposes the erosion rate due to low velocity fluids (< 100 m/s) may be negligible;
- Tangential erosion rates are lower by a factor of 10^{-4} than those of perpendicular fluid impact;
- Hardened surfaces improve the erosion resistance of the base material appreciably. Thus, ceramic coatings or nitrogen-rich semi-ceramic surface layers can significantly reduce erosion concerns;
- The number of sharp bends and contractions-expansions should be kept to a minimum to reduce large impact angles.

A limited number of experiments were conducted in the late 1960s to study the effects of high-velocity liquid lithium. The corrosion resistance of Nb-1Zr in lithium flowing at 50 m/s was evaluated for potential application in a magnetohydrodynamic space-power system [52]. The layer of material removed after 500 hours of exposure at 1073 to 1145 °C was measured to be 7.5- μ m thick which corresponds to a corrosion rate of about 0.1 mm/y. Also, the compatibility of TZM with potassium vapor flowing at a velocity of 180 m/s was determined in a 3000-h turbine test [53]. The TZM turbine blades showed negligible corrosion after exposure to the 750 °C potassium vapor. Although the first experiment

used a niobium alloy with lithium and the second one used TZM with potassium, these findings suggest that high-velocity liquid metals should not lead to unexpected erosion processes.

Based on the experience of high-velocity water on erosion rates of metals and ceramics and on a limited experimental data base on high-velocity liquid lithium on Nb-1Zr, it does not seem that the 20 to 25-m/s velocity of the liquid lithium in the first-wall- and divertor-coolant channels of TITAN-I design would result in unacceptable erosion corrosion rates. It should be noted that further study is required to expand the database for lithium-vanadium systems.

10.2.2.4. Ferrous alloys

As discussed above, using a second material for the remainder of the coolant loop introduces the problem of bimetallic interstitial pickup. The vanadium alloy used as the primary structural material will pick up nitrogen and carbon depleted from any steel in contact with the same liquid lithium. Degradation of the mechanical properties will occur in both the V-3Ti-1Si and the steel.

Ferritic and austenitic steels have been investigated as potential structural materials for liquid-lithium systems [54-57]. The corrosion rates of austenitic and ferritic steels are compared in Figure 10.2-6 showing that the corrosion of ferritics can be an order of magnitude less than that of austenitics. In these experiments [55], the surface of 316-SS was found to be depleted of both nickel and chromium. The preferential leaching of these elements is believed to be the major cause of the high corrosion rates seen in austenitic stainless steels. The surface of austenitics exposed to high-temperature lithium was found to be highly porous [54]. Below 520°C, porosity is minimal and deposits become the predominant surface feature.

A general characteristic of liquid-metal-corrosion test loops is that the weight loss is maximum at the point of the highest temperature. However, Tortorelli and DeVan [54] report that for HT-9, the coldest specimen (350°C) had the largest weight loss. The steady-state weight-loss rate of ferritic steel HT-9 above 1000 hours of exposure to flowing lithium was determined to be 0.4 mg/m²-h at 350°C and 1.0 mg/m²-h at 500°C [55]. Chopra *et al.* [56] reported a slight drop in corrosion rate for 316-SS and a drastic rise in corrosion rate for HT-9, with increasing temperature.

Most recent forced-loop experiments performed by Bell *et al.* [58] on HT-9 samples have not shown any clear dependence between temperature and corrosion rates. For low

Figure 10.2-6. Weight loss as a function of the exposure time for HT-9 and 316-SS exposed to convective lithium at 500 °C [54].

exposure times of about 300 h, the dissolution rate of HT-9 was measured to be fairly temperature insensitive with a weight-loss rate of about 2 mg/m²-h. After exposure times of 3000 h, the weight-loss rate was measured between 0.15 and 0.3 mg/m²-h, respectively, for 500 and 350 °C. These latest findings [58] confirm the results from Tortorelli and De Van [54] of a decrease in corrosion rate of ferritics with increasing temperatures.

Although the above experiments did not report parabolic corrosion rates, Borgstedt et al. [57] estimated the corrosion rate of 304-SS by a parabolic equation as:

$$\Delta W = 6.2 \times 10^{-2} t^{0.60}, \quad (10.2-17)$$

where ΔW is the weight-loss rate in g/m²-h, and the time t is in hours. The test conditions resulting in Equation 10.2-17 were 551 °C with liquid lithium flowing at 7 cm/s.

These measured corrosion rates of HT-9 all fall below the mass-transfer limit of 5 μ m/y ($\sim$ 5 mg/m² h) specified for nuclear power plants. The radioactive-mass-transport limit of 0.5 μ m/y ($\sim$ 0.5 mg/m² h) is also satisfied for long-term exposure ($>$ 1000 h) of HT-9 to flowing lithium at temperatures up to 600 °C. Austenitic steel, on the other hand, barely falls below the mass transfer limit of 5 μ m/y and does not satisfy the radioactive transfer limit of 0.5 μ m/y. As pointed out before, these limits were derived from light-water-reactor experience with water-steel systems, which may be too conservative for a liquid-metal-cooled fusion reactor. Additional study of the redeposition of radioactive corrosion products and the redistribution of radioactive source terms is necessary. Furthermore, it may be possible to reduce the corrosion rate of austenitic steels by special techniques.

Inhibition of steel corrosion. Several methods for minimizing the corrosion rate of austenitic-steel alloys in liquid lithium have been investigated [59,60]. In particular, the emphasis was to reduce the nonmetallic impurity levels (*e.g.*, nitrogen and carbon) to minimize the mass transfer, and to eliminate the localized grain-boundary attack. Reduction of nonmetallic impurity levels was achieved by using conventional getters such as titanium wool or soluble chemical getters such as calcium [61]. The addition of corrosion inhibitors (*e.g.*, aluminum, calcium, barium, magnesium) to liquid lithium was tried as part of the space-nuclear-power program during the late 1950s [62]. However, the high mass-transfer rates were not affected by any of the corrosion inhibitors because of the high operating temperature ($>$ 800 °C). Recent experiments at lower temperatures show a factor of 5 reduction in corrosion of 316-SS when 5-wt.% aluminum was added to flowing lithium [63].

A third approach to corrosion inhibition has been the use of corrosion-resistant diffusion coatings. Diffusion coatings are produced by exposing the alloy to a chemical com-

pound of the desired coating material, which then dissociates and diffuses inward upon heating and forms an adhesive surface layer. Nickel-bearing alloys showed an increased resistance to corrosion by high-temperature lithium (700 °C) when diffusion-coated with molybdenum, molybdenum-titanium, and aluminum [64]. Diffusion-coated alloys are now commercially available for use in liquid sodium for breeder-reactor applications [65]. Recently, Burrow *et al.* [66] tested these commercially available diffusion-coated alloys in liquid lithium. They used a proprietary powder-pack process to coat samples of austenitic, ferritic, and high-nickel structural alloys with a 40- to 60- μ m-thick aluminized diffusion coating. *Both coated and non-coated specimens were tested in flowing lithium at 400 °C for 2000 h with an axial temperature differential of about 30 °C. The flow rate was kept constant at 2 cm³/s. These tests showed that all aluminized materials demonstrated excellent compatibility with liquid lithium. While a minimal weight loss (1 g/m²) and unaltered profiles for iron, chromium, and aluminum substantiated the stability of aluminized ferritics, no real benefit could be identified from these preliminary screening tests. Aluminized, high-nickel alloys, however, showed a drastic reduction in weight loss compared to the non-coated samples. Additional lithium studies are required before a definitive statement regarding the corrosion-inhibiting processes can be made.*

10.2.3. Insulator Material

The mechanical integrity of insulating materials and retention of properties during exposure to radiation environments has always been a design concern. Electrically insulated components, such as magnets, need to be shielded to ensure acceptable performance. The amount of shielding material required will undoubtedly affect reactor dimensions. Another application of electric insulator materials is to mitigate magnetohydrodynamic (MHD) pressure drops. When a liquid metal is used as a coolant, the motion of the conducting fluid induces eddy currents which flow in the coolant and the structure. Interaction of these eddy currents with the magnetic field exerts a body force on the liquid metal and results in MHD pressure drops. Electrical insulation between the structure and the coolant reduces eddy-current flow in the structure and thereby reduces the MHD pressure drop.

The electrical insulators are also used as current breaks in electrically conducting components. Depending on their location, these breaks can be exposed to high levels of radiation and can play a significant role in plasma equilibrium and stability. Arcing between structure gaps or breaks is another concern that can be mitigated by the use of proper insulating material. A major consequence of arcing is localized melting and the subsequent release of high-*Z* material into the plasma chamber.

Insulating material requirements can be specific, depending on the location and function of the individual subsystems. However, the following properties are generally required from insulating materials exposed to radiation environments:

- Dimensional stability,
- Retention of mechanical properties,
- Compatibility with structural and coolant materials,
- Adequate electrical resistivity,
- Resistance to radiation-induced conductivity,
- Ease of fabrication.

In the TITAN-I reactor, no insulating material is in direct contact with coolant. Therefore, coolant compatibility is not a major issue in selecting an insulating material. The selection criteria is based primarily on satisfying minimum irradiation-induced swelling, retention of strength, and minimum radiation-induced conductivity.

10.2.3.1. Candidate insulating materials

Organic insulating materials generally do not meet high-temperature requirements and suffer from rapid degradation of their resistivity when exposed to ionizing radiation. Recently, however, Plansee [67] has succeeded in developing a polyamide insulator, SINTIMID, that is stable up to 290 °C for prolonged use and up to 400 °C for short exposure times. In preliminary radiation experiments, SINTIMID has shown to be highly resistant to radiation damage.

Ceramic insulating materials, on the other hand, possess high melting or decomposition temperatures ($> 2000^{\circ}\text{C}$). Ceramics that have been investigated for fusion applications can generally be divided into graphites, carbides, nitrides, and oxides.

Graphites. Graphites suffer from large radiation-damage-induced strains because of the layered structure of the lattice. Isotropic graphites have been manufactured which show low radiation swelling at moderate fluence and good retention of thermal and mechanical properties [68]. At high fluence ($> 2 \times 10^{26} \text{ n/m}^2$), however, disintegration along grain boundaries occurs which is caused by cracking [69]. Graphites also suffer from rapid chemical sputtering when exposed to plasmas.

Carbides. Silicon carbide (SiC), a high-strength, high-thermal-conductivity ceramic, is the most widely studied carbide. At 800 K, the Young's modulus is about 400 Gpa and the yield strength is about 350 MPa. The thermal conductivity of SiC is about 37 W/m-K for unirradiated material and the thermal-expansion coefficient is 4.9×10^{-6} /K between 300 and 800 K. Because of its cubic structure, SiC swells isotropically under neutron irradiation. Silicon carbide shows relatively high dimensional stability with minimum swelling of about 1.2% at 1000 °C [70]. Swelling saturates at fluence between 10^{24} and 10^{25} n/m², depending on the temperature [71].

The most drastic effect of fast-neutron irradiation of SiC is the reduction of thermal conductivity. At 552 °C, SiC irradiated to a fluence of 2.7×10^{25} n/m² ($E_n > 0.18$ MeV) showed a reduction in the thermal conductivity by as much as 87% [70]. Other experiments at higher fluences showed an 80% reduction in thermal conductivity after irradiation to 2×10^{26} n/m² at 540 and 740 °C [72]. Recent experiments at room temperature indicate an increase in thermal conductivity of SiC from 50 to 270 W/m-K with the addition of 2 at.% BeO [73]. The addition of BeO had little or no effect on the thermal-expansion coefficient and theoretical density.

Silicon carbide is often considered as a structural material because of its strength, irradiation stability, resistance to creep and thermal stress, commercial availability, fabricability, and tritium retention. Although SiC has excellent resistance to growth and swelling in a wide range of temperatures and neutron doses, large amounts of transmutation gases have been shown to adversely affect the mechanical properties of SiC [74].

Nitrides. Nitride ceramics are the least studied among insulating materials for fusion applications. This is mostly because of their instability at high temperatures. Nitrides that are stable at high temperatures (≥ 300 °C) include: AlN, BN, and Si₃N₄. Most nitrides have hexagonal crystal structures and show anisotropic growth and swelling leading to high radiation-induced strains. Most nitrides are stable in contact with vanadium, except Si₃N₄ which readily forms VN. Among the ceramics, nitrides show some of the best electrical resistivities, with BN having the lowest electrical conductivity (10^{-14} /Ω-cm at 500 °C).

Another consideration regarding the use of graphites, carbides, or nitrides as insulating materials in contact with refractory alloys is the high affinity of most refractories to carbon and nitrogen. At elevated temperatures, carbon can be picked up by structural materials, resulting in carburization of the refractory metal and consequent loss of ductility. Furthermore, carbides suffer from a high level of hydrogen and helium generation rates. The hydrogen produced can bind with carbon to form volatile hydrocarbons

such as methane (CH_4). Helium will generally migrate to grain boundaries where it is trapped inside grain-boundary bubbles. This trapping is one of the major causes of crack formation and propagation along grain boundaries in irradiated graphites.

Oxides. Among the many oxide ceramics (*e.g.*, BeO , MgO , Al_2O_3 , MgAl_2O_4 , Y_2O_3 , $\text{Y}_3\text{Al}_5\text{O}_{12}$, Si_2ON_2 , and $\text{Si}_4\text{Al}_4\text{O}_2\text{N}_6$) considered for fusion applications, Al_2O_3 (alumina) is the most highly developed refractory ceramic, exhibiting good structural and electrical properties in a radiation-free environment. Alumina has a hexagonal cubic structure, resulting in anisotropic swelling and growth under neutron irradiation [75]. Therefore, neutron irradiation results in a relatively rapid degradation of properties [76].

Cubic-structured alloys of alumina, such as MgAl_2O_4 (spinel) and $\text{AlN}(\text{Al}_2\text{O}_3)_{1.8}$ (ALON), have shown the most resistance to radiation-induced swelling, and retained thermal conductivity and mechanical properties [77,78]. For example, the following insulators experience a 50% to 90% reduction in thermal conductivity after exposure to a fast-neutron fluence of $2.8 \times 10^{29} \text{ n/m}^2$: BeO , MgO , Al_2O_3 , MgAl_2O_4 , Y_2O_3 , $\text{Y}_3\text{Al}_5\text{O}_{12}$, Si_2ON_2 , and $\text{Si}_4\text{Al}_4\text{O}_2\text{N}_6$ [70]. Spinel is one of the few ceramics that experience a relatively small drop of 8% in thermal conductivity after exposure to the same fluence [77].

Another favorable characteristic of spinel is its mechanical behavior upon exposure to radiation. Table 10.2-XIV shows the changes in the strength of polycrystalline spinel after fast-neutron irradiation to $2 \times 10^{26} \text{ n/m}^2$ at 680 and 815 K [77]. This increase in the strength is attributed to the suppression of crack nucleation or propagation by the damaged microstructure. The fracture-toughness and hardness of single-crystal spinel showed little or no change after the same exposure levels at 925 and 1100 K [76]. These results indicate that spinel will retain its resistance to crack propagation under high-dose irradiation conditions.

Although spinel is not a high-strength, high-thermal-conductivity ceramic, its good behavior under irradiation makes it one of the more promising refractory ceramics for fusion reactor applications and it is chosen as the insulator material for the TITAN designs. Table 10.2-XV lists selected properties of spinel.

10.2.3.2. Swelling of spinel

Atomic-displacement damage can lead to the formation of voids in materials exposed to nuclear radiation. These voids are generally unstable at high temperatures because of thermal dissociation. In addition, transmutation reactions induced by neutrons introduce

Table 10.2-XIV.

TENSILE STRENGTH OF IRRADIATED^(a) SPINEL [77]

Irradiation Temperature (°C)	Strength (MPa)		
	Controlled	Irradiated	% Change
407	129	178	38
542	129	173	34

(a) Polycrystalline spinel irradiated to 10^{26} n/m².

soluble and non-soluble foreign atoms. Non-soluble elements such as helium are readily trapped in voids, resulting in the stabilization of otherwise unstable void embryos. At low temperatures (below room temperature) the voids can pack a high number of helium atoms and indirectly lead to a low swelling rate. At higher temperatures, helium atoms can become de-trapped and diffuse out of the matrix, also resulting in a low swelling rate. In the intermediate temperature range, however, mechanical equilibrium conditions between the surface tension and helium content of bubbles reduces the amount of helium per bubble to low values. Furthermore, the available thermal energy is not enough to de-trap a sufficient number of helium atoms. Thus, at intermediate temperatures, which can coincide with operating temperatures of most energy-producing systems, most materials suffer a swelling peak, as seen in Figure 10.2-7.

While pure materials generally experience high levels of swelling, mixing or alloying with different materials can drastically reduce swelling levels. One such example is spinel, which is a mixture of MgO with Al₂O₃ (alumina). Spinel is more swelling resistant than either alumina or MgO. For a damage dose of 10 dpa, alumina swells up to about 4% [81,82] between 650 and 830 °C, MgO swells up to 3.0% [81] at 167 °C, while spinel shows virtually no swelling at these temperatures [81]. Figure 10.2-8 shows the drastically different swelling rates between alumina and spinel after neutron irradiation [76-84].

Swelling can have significant effects on insulator performance. Nonuniform swelling can lead to a variation in thermal conductivity across the insulator. In the presence of a temperature gradient, a swelling gradient can develop across the material since swelling

Table 10.2-XV.
SELECTED SPINEL PROPERTIES

Density	3540	kg/m ³
Crystal structure	cubic	
Decomposition temperature	2135	°C
Coefficient of thermal expansion		
at 600 °C	8×10^{-6}	/K
at 1000 °C	9×10^{-6}	/K
Thermal conductivity		
at 23 °C	20	W/m-K
at 600 °C	8.1	W/m-K
at 1000 °C	6	W/m-K
Electrical resistivity		
at 100 °C	$> 10^{16}$	$\Omega \text{ m}$
at 200 °C	10^{15}	$\Omega \text{ m}$
at 600 °C	10^9	$\Omega \text{ m}$
Young's modulus (1000 °C)	206	GPa
Flexural strength (1000 °C)	250	MPa
Typical impurities [79]		
Si	400	wppm
Ca	100	wppm
Na	80	wppm
B	35	wppm
Fe	20	wppm
Grain size	10-100	μm
Porosity (polycrystalline spinel)	6%

Figure 10.2-7. Swelling of polycrystalline alumina as a function of temperature after a damage dose of 20 dpa [80].

is temperature dependent. The swelling gradient in turn compounds the effects of the temperature gradient, resulting in unacceptable thermal stresses. Stress concentrations around surface or lattice defects can result in crack formation. Cracks that propagate to the surface will allow impurity gases to penetrate the ceramic. The insulating property of the material can in this way be dominated by filler gases (higher conductivity), rather than the bulk insulating material itself.

Because of the degradation of mechanical and physical properties, swelling of insulating materials is of great concern. Generally, swelling is the life-limiting criteria for a load-bearing insulator. Volume changes of alumina and spinel induced by irradiation with 14-MeV neutrons at 50°C were measured by Tanimura and Clinard [75]. It was shown that the volume change of Al_2O_3 is anisotropic and is larger than that of spinel by about a factor of five. Above 900 K the swelling of spinel is less than 10% of that of Al_2O_3 [85,86]. Table 10.2-XVI shows some of the experimental data on swelling of spinel. These experimental data on swelling as a function of temperature and damage

Figure 10.2-8. Swelling of single-crystal alumina and spinel after neutron irradiation at high temperatures [84].

dose, however, are not sufficient to extrapolate empirical design equations. Therefore, a phenomenological swelling equation based on the limited swelling data was developed to establish a lifetime criteria for spinel for the TITAN-I design.

Most ceramics exhibit a bell-shaped swelling curve as a function of temperature, with a swelling peak in the range of 0.2 to $0.5 T_m$ (melting point of the material in K). Such swelling curves are available for CaF_2 , ZrO_2 - Y_2O_3 , alumina, and SiC [81]. A similar temperature-dependent, bell-shaped swelling behavior for spinel can also be seen in Table 10.2-XVI. The approximate swelling equation as a function of temperature and dpa was found to be

$$\frac{\Delta V}{V} \approx 3.46 \times 10^{-2} \delta \exp \left[- \left(\frac{T - 220}{122} \right)^2 \right], \quad (10.2-18)$$

where $\Delta V/V$ is the swelling rate in %, δ is the damage dose in dpa, and T is the irradiation temperature ($^{\circ}C$). Coghlan *et al.* [87] have recently estimated the dpa for spinel to be $\sim 1.17 \times 10^{-25} \text{ dpa}/(n/m^2)$.

Table 10.2-XVI.

SWELLING OF NEUTRON-IRRADIATED SPINEL (MgAl_2O_4)

Temperature (K)	$\Delta V/V$ (%)	Fluence (n/m^2) ^(a)	Reference
323	0.03 ± 0.01	3.2×10^{26}	[87]
407	0.08 ± 0.01	2.1×10^{30}	[81]
680	-0.19 ± 0.01	2.2×10^{30}	[77]
815	-0.35 ± 0.01	2.2×10^{30}	[76]
925	± 0.01	2.3×10^{28}	[81]

(a) Fast-neutron fluence ($E_n > 0.1$ MeV).

For TITAN-I at 18 MW/m^2 of neutron wall loading, spinel located at the first wall or divertor will experience a total damage dose of $\sim 180 \text{ dpa}$ after one FPY of operation. Figure 10.2-9 shows the swelling of spinel at the first wall of TITAN-I as a function of temperature and exposure time, as estimated by Equation 10.2-18. This swelling curve shows that operating spinel below 150°C or above 350°C ensures low swelling rates and operation in these temperature ranges is favored.

10.2.3.3. Dielectric-breakdown strength and radiation-induced conductivity

Dielectric-breakdown strength (DBS) of ceramic insulators is defined as the maximum potential gradient in the dielectric without the occurrence of an electric breakdown. High temperatures degrade the DBS since excess thermal energy leads to an increase in drift currents and additional heating, eventually avalanching into a breakdown current. Although no data are available for the DBS of spinel, the DBS has been measured for alumina. Since spinel consists of 66% alumina and $\sim 33\%$ MgO , it is assumed that the DBS of spinel is about the same as that of alumina. Below 200°C , the DBS for alumina is about 12 kV/mm and it reduces to, respectively, about 8 and 2.5 kV/mm at 350 and 700°C . Since high temperatures degrade DBS, it is desirable to operate insulating materials at low temperatures.

Figure 10.2-9. Swelling of spinel as a function of damage dose and temperature.

Ionizing radiation, mostly by γ -rays, also leads to an increase in electrical conductivity. The γ -ray photons interact with electrons which increases the concentration of conduction-charge carriers. This phenomenon is a flux-dependent effect (instantaneous) and is commonly known as radiation-induced conductivity (RIC). Klaffky [88] measured the RIC of alumina, both doped and un-doped with Cr_2O_3 , as a function of ionizing dose rate between 300 and 1250 K in a radiation field of a 1.5-MeV electron beam. He showed that the conductivity of single-crystal Al_2O_3 increased as a function of dose rate, up to 10^{-4} mho/m for un-doped material at 6.6×10^2 Gy/s (1 Gy = 100 rads).

Doping, as well as neutron irradiation, significantly reduces conductivity at a given temperature. Un-doped alumina is characterized by the presence of shallow electron traps, while doped or neutron-irradiated material contains deep traps. The temperature dependence of the conductivity is complex, showing a minimum conductivity around 440°C for all doped specimens. The conductivity falls as the temperature is increased from room temperature because of the release of electrons from shallow traps. For doped alumina, there is a characteristic temperature at which the shallow traps are depleted and conductivity reaches its minimum value. As the temperature is increased beyond this point, the thermal energy provided to electrons residing in deep traps is sufficient to cause an increase in conductivity at higher temperatures.

Recently, Pells *et al.* [89] measured the conductivity of spinel exposed to ionizing radiations of 7.5 Gy/s (X-rays) between 20 and 800°C. While unirradiated spinel has a conductivity of about 10^{-13} mho/m, the irradiated spinel conductivity was measured around 10^{-8} mho/m at 200°C. The conductivity of the irradiated spinel is not very sensitive to temperature and ranges between 10^{-8} to 10^{-7} mho/m between 20 and 800°C. These data are used [89] to estimate the dependence of the conductivity of spinel on the fluence, $\dot{\gamma}$, as

$$\sigma_{RIC}^{sp} \sim 1.33 \times 10^{-9} \dot{\gamma}, \quad (10.2-19)$$

where σ_{RIC}^{sp} is the spinel RIC in mho/m and $\dot{\gamma}$ is the dose rate (Gy/s). For the 18-MW/m²-neutron-wall-loading TITAN-I design, $\dot{\gamma}$ at the first wall is $\sim 2.7 \times 10^4$ Gy/s. This increases the conductivity of spinel at the first wall to $\sim 3.5 \times 10^{-5}$ mho/m. Klaffky [90] showed that alumina doped with less than 0.03 wt.% Cr₂O₃ experiences a reduction of conductivity by more than one order of magnitude. Similar doping techniques can be developed for spinel, but have not yet been investigated.

Radiation-produced defects also contribute to RIC. These defects increase the number of electron-scattering sites and the number of deep electron traps, resulting in an enhancement of overall conductivity. This effect is time dependent and, thus, a function of fluence. A single-crystal alumina irradiated to 3×10^{24} n/m² at 150°C was shown to have an RIC even lower than that of the Cr₂O₃-doped alumina by Klaffky [88]. Neutron-irradiated samples showed the lowest radiation-induced conductivity.

Based on the above observations, spinel has been chosen as the primary electrical-insulator material for the TITAN-I design. Most ceramic insulating materials studied in the past are "off-the-shelf" insulators used commonly in nonirradiation environments. Although spinel appears to possess adequate properties for severe radiation environments, nonconventional insulating materials are being developed with far superior characteristics. One such relatively new insulator is AlN(A₂O₃)_{1.8} (aluminum oxynitride spinel), a ceramic referred to as ALON which is being developed for infrared-window applications [91]. Several specimens of polycrystalline ALON were irradiated with 0.8- and 1-MeV electrons between 300 and 1100 K [78]. No defects were observed at any temperature through either thick or thin sections. The lack of any bulk irradiation effect was attributed to the difficulty of forming dislocations in ALON. From these preliminary investigations it appears that ALON is radiation-damage resistant. Future investigations into the radiation behavior of ALON are currently underway [78].

10.2.4. Summary

From the three candidate vanadium-base alloys, V-3Ti-1Si was chosen as the primary structural material for TITAN-I. The choice was primarily based on the irradiation behavior of this alloy. The V-3Ti-1Si alloy outperforms V-15Cr-5Ti and VANSTAR as far as helium embrittlement, irradiation hardening, and swelling are concerned after exposure to a damage dose of 40 dpa by fast neutrons. However, V-3Ti-1Si has the lowest thermal-creep resistance when compared to the other two alloys.

The minimum-commitment method was used to extrapolate the creep-rupture data and establish the creep behavior during normal and off-normal operating conditions. From the limited creep data, it appears that V-3Ti-1Si will be able to operate satisfactorily at elevated temperatures (700 °C). To include the effects of the irradiation hardening, helium embrittlement data was used to estimate the maximum allowable design stress based on a 2/3 creep-rupture-stress criteria. Additional creep-rupture experiments are needed to develop more precise creep-rupture models for V-3Ti-1Si.

Compatibility of the vanadium-base alloys with lithium coolant was investigated. Recent test results were used to establish the anticipated degree of lithium attack on the V-3Ti-1Si alloy. Various lithium-attack processes were examined, and particular attention was given to the interaction between vanadium and nonmetallic impurities such as oxygen, nitrogen, carbon, and hydrogen. The limited available data do indicate the possibility of a self-limiting corrosion rate on V-3Ti-1Si because of the formation of complex vanadium-titanium-nitride surface layers. The effects of a bimetallic-loop containing liquid lithium was also investigated. Low-carbon, titanium-stabilized ferritic steel exhibits good resistance against lithium corrosion.

The effects of high-velocity lithium on erosion was estimated using water-steel data. From a very limited set of data on erosion of refractory metals by a high-velocity liquid lithium and from the water-steel experience, it seems that lithium velocity of 20 to 25 m/s should not introduce unacceptable erosion rates.

Spinel (MgAl_2O_4) has been chosen as the primary electrical-insulating material for the TITAN-I design based on its excellent resistance to radiation-induced swelling and retention (or increase) of strength. A phenomenological swelling equation was developed as a function of temperature and damage dose. The swelling data were incorporated into a phenomenological swelling equation which shows that spinel located close to the first wall should not experience more than a maximum of about 5% swelling if operated below 150 °C or above 300 °C. High operating temperatures may ensure a low swelling rate but

could bring about dielectric breakdown of the insulator. Therefore, low-temperature operation ($< 150^{\circ}\text{C}$) is suggested.

Radiation-induced conductivity (RIC) is a complex function of temperature, ionizing radiation dose, and neutron damage. Experiments performed on samples of single-crystal alumina and polycrystalline spinel indicate adequate resistance to RIC in a fusion environment. Radiation-induced conductivity of doped alumina has been investigated and it has been shown that doping can drastically reduce RIC. It is anticipated that doped spinel will show similar positive effects.

Development of new ceramic insulating materials capable of withstanding intense radiation environments is in the early stages. The relatively new insulator, ALON, has shown to be unusually radiation-damage resistant.

10.3. NEUTRONICS

The neutronics design of the blanket and shield for the TITAN reactors is unique because of the high neutron wall loading (18 MW/m^2). The resulting shorter lifetime (in reactor years) of various components makes the annual replacement mass (and cost) of the blanket and shield components an important factor in the neutronics optimization. Note that even though the lifetime of various components is shorter, these components are usually smaller and, therefore, the annual replacement mass is somewhat insensitive to the neutron wall loading. The other unique aspect of the TITAN reactors is the use of normal-conducting coils in the toroidal-field, divertor, and ohmic-heating (OH) magnets. The use of normal-conducting coils will greatly reduce the shielding requirements for these components from radiation damage and will maintain the compactness of the design.

Over the TITAN-I reactor lifetime (30 FPY), the neutron-fluence limit of the magnets is $4 \times 10^{26}\text{ n/m}^2$ (for $E_n \geq 0.1\text{ MeV}$), or even higher [92], and is set by the spinel insulator lifetime (Section 10.2.3). The neutron-fluence limit for a superconductor magnet is only about $1 \times 10^{23}\text{ n/m}^2$ (for $E_n \geq 0.1\text{ MeV}$) [93]. This 3 to 4 orders of magnitude difference in the neutron-fluence limit allows a 0.6- to 0.8-m reduction in the shielding space between the plasma and the magnet system for the TITAN-I design.

This section describes the neutronics aspects of the TITAN-I fusion power core (FPC). A detailed survey of the performances of various blanket and shield combinations (*e.g.*, thickness and composition) was performed and reported in Section 10.3.1. The final neutronics design of the FPC is given in Section 10.3.2. Waste-disposal ratings of the TITAN-I design are discussed separately in Section 13.7.

10.3.1. Neutronics Optimization

Tritium breeding, blanket energy multiplication, afterheat, radiation damage to the structural materials and the OH magnets, annual replacement mass (and cost) of blanket and shield, and the waste-disposal ratings are some of the important parameters that were considered for the neutronics optimization of the TITAN-I design. Neutronics calculations were performed to investigate each of the above parameters based on a 1-D blanket and shield model in a cylindrical geometry, with the center of the poloidal cross section of the plasma located on the centerline of the cylinder. The parameters of the final design were verified by a set of 3-D calculations. The neutron and gamma-ray transport code, ANISN [94], is used with the cross-section library ENDF/B-V-based MATXS5 which was processed with the NJOY system at Los Alamos National Laboratory [95].

The neutronics model used in the optimization study consists of a 1-cm first wall, a 1-cm gap, a 75-cm blanket and shield zone, and a 1-cm gap followed by the OH magnet. The first wall is composed of 40% vanadium alloy, 38.5% lithium, and 21.5% void (in this section, the composition of the FPC components are in volume fraction). The blanket consists of vanadium alloy and lithium with varying volume fractions. The shield is made of 10% lithium and 90% vanadium. The OH magnet consists of 70% copper, 10% insulator (spinel), 10% structure (304-SS), and 10% coolant (helium). For the purpose of estimating the lifetime of various components, a 200-dpa damage limit has been used.

Table 10.3-I summarizes the results of neutronics calculations for several combinations of blanket and shield thicknesses, varying amounts of structure in the blanket, and with different levels of ^6Li enrichment in the lithium coolant. The next subsections review the results of this table and the approach to the optimization of various neutronics parameters.

10.3.1.1. Tritium breeding

Adequate tritium breeding is a fundamental requirements for a DT fusion reactor. A tritium-breeding ratio (TBR) of no less than 1.20, based on a 1-D full-coverage calculation, was used as the goal of the neutronics study. Table 10.3-I shows that adequate TBR is obtainable in most blanket arrangements, except when the blanket thickness is less than 40 cm and the blanket contains more than 60% vanadium. The enrichment level of ^6Li in the lithium coolant can be used to control the TBR in these blanket systems. At a higher ^6Li enrichment, the contribution of tritium breeding from ^7Li will decrease, as is shown in Table 10.3-I.

Table 10.3-I.

**NEUTRONICS PERFORMANCE OF
SEVERAL SELF-COOLED LITHIUM BLANKET SYSTEMS**

Blanket System	⁶ Li Enrichment (%)	Tritium-Breeding Ratio ^(a)	$M^{(b)}$	Energy Leakage (% M)	Max. dpa at Shield ^(c)	Max. Fluence at Magnet (n/m^2) ^(c)
10% V blanket	7.4	1.413 (34.)	1.12	6.6	373	2.1×10^{27}
60-cm blanket	40.	1.358 (22.)	1.13	4.3	271	1.1×10^{27}
15-cm shield	75.	1.211 (10.)	1.15	3.5	235	9.6×10^{26}
<hr/>						
	7.4	1.205 (21.)	1.17	2.9	306	5.8×10^{26}
30% V blanket	20.	1.278 (17.)	1.16	2.3	272	5.4×10^{26}
50-cm blanket	30.	1.293 (15.)	1.15	2.0	248	5.0×10^{26}
25-cm shield	40.	1.284 (13.)	1.15	1.8	233	4.8×10^{26}
	75.	1.222 (5.5)	1.16	1.4	201	4.1×10^{26}
<hr/>						
60% V blanket	15.	1.077 (8.7)	1.18	1.1	360	2.6×10^{26}
37-cm blanket	30.	1.159 (6.6)	1.16	0.92	304	2.2×10^{26}
38-cm shield						
<hr/>						
60% V blanket						
40-cm blanket	75.	1.205 (2.3)	1.16	0.63	199	1.8×10^{26}
35-cm shield						

(a) The values given in parentheses are fractional contributions (in %) to TBRs from ${}^7\text{Li}(n,n'\alpha)$ reactions; the rest are from ${}^6\text{Li}(n,\alpha)$.

(b) Blanket energy multiplication, M , is defined as ratio of total nuclear energy deposited in the first wall, blanket, and shield to the incident DT neutron energy.

(c) For 30-FPY operation at 18 MW/m² of neutron wall loading.

10.3.1.2. Blanket energy multiplication

The blanket energy multiplication of fusion neutrons in the systems of Table 10.3-I ranges from 1.12 to 1.18, depending on the amount of vanadium structure (10% to 60%) in the blanket. In principle, the energy released by the exothermic capture of neutrons by the structural elements exceeds the energy produced by the absorption of neutrons by ${}^6\text{Li}$ to produce tritium. The capture of excess neutrons in the blanket to enhance blanket energy multiplication is common in recent reactor studies. Beryllium has been used in some designs to provide a large quantity of excess neutrons.

Manganese in manganese stainless steel (e.g., Fe14Mn2Cr2Ni) has shown to be effective in increasing the blanket energy multiplication by about 0.4. However, about 45% of this excess energy is actually produced by the decay of ${}^{56}\text{Mn}$ nuclide, resulting from the absorption of neutrons in manganese. After shutdown of the reactor, the large level of ${}^{56}\text{Mn}$ afterheat would impose a potential safety problem, particularly for high-power-density systems such as the TITAN-I design; therefore, manganese stainless steel is not recommended as a shield material.

The final TITAN-I design uses a vanadium shield. Furthermore, a ${}^6\text{Li}$ -rich (30% enrichment) blanket coolant is used in order to further reduce the level of afterheat, to minimize the neutron flux at the shield and magnet, and to suppress the production of long-lived radionuclides and radiation damage to these components. At this level of ${}^6\text{Li}$ enrichment, the blanket energy multiplication (~ 1.16 , Table 10.3-I) tends to be somewhat independent of the blanket and shield composition.

10.3.1.3. Afterheat

The level of afterheat in the vanadium-alloy structural material of the blanket and shield is affected by ${}^6\text{Li}$ enrichment. Table 10.3-II shows a comparison of estimated levels of afterheat at different times for two extreme enrichment levels in a blanket composed of 20% vanadium. For a highly enriched ${}^6\text{Li}$ coolant, a factor of eight reduction in the level of afterheat at shutdown can be achieved which lasts for about one hour. The afterheat level at shutdown is dominated by ${}^{52}\text{V}$ which is produced by ${}^{51}\text{V}(n,\gamma)$ reactions. The large population of low-energy neutrons in the depleted ${}^6\text{Li}$ system enhances the production of ${}^{52}\text{V}$ in the vanadium alloy. Fortunately, the half-life of ${}^{52}\text{V}$ is only 3.75 minutes, which significantly reduces the afterheat at longer times after shutdown. It was also found that the afterheat can be reduced by about 30% if vanadium alloy in the blanket is decreased from 20% to 10%.

Table 10.3-II.
COMPARISON OF AFTERHEAT LEVELS
FOR DIFFERENT ^6Li ENRICHMENT

Time After Shutdown	Afterheat/Reactor Thermal Power ^(a)	
	2% ^6Li	75% ^6Li
At shutdown	3.3%	0.43%
5 minutes	1.5%	0.29%
1 hour	0.13%	0.13%
1 day	0.09%	0.09%
10 days	0.025%	0.025%

(a) For a blanket composed of 20% V in a 2700-MWt reactor.

10.3.1.4. Atomic displacement and annual replacement mass

Table 10.3-I shows the maximum atomic displacement in the shield, accumulated over 30 FPY at 18 MW/m^2 of neutron wall loading, for different blanket compositions and ^6Li enrichments in lithium. The atomic displacement in the shield decreases drastically as the ^6Li enrichment increases; the atomic displacement can be reduced by more than 50% when a highly enriched (75%) coolant is used instead of natural lithium (7.42%). This reduction is also seen for the fast-neutron fluence ($E_n \geq 0.1\text{ MeV}$) at the OH magnet.

The radiation-damage limit for the structural alloy in the TITAN-I design was set at 200 dpa. The average annual replacement mass of vanadium (first wall, blanket, and shield) is illustrated in Figure 10.3-1 as a function of allowable fast-neutron fluence at the magnet for several blanket compositions and thicknesses. This figure shows that for a given fluence at the magnet: (1) ^6Li enrichment tends to reduce the replacement mass, (2) a blanket with smaller amount of structure gives a lower replacement mass since lithium also attenuates the neutron flux, and (3) a thicker blanket and shield yield less replacement mass.

Figure 10.3-1. Averaged annual replacement mass of vanadium alloy in the TITAN-I blanket and shield as a function of 30-FPY integrated fast-neutron fluence at the OH magnet.

10.3.1.5. Waste-disposal ratings

One of the objectives of the TITAN study has been to meet the 10CFR61 [96] Class C waste-disposal criteria for low-level waste qualified for shallow-land burial. This objective has guided the material selection and neutronics optimization. Suppression of the low-energy neutron population by increasing the ^6Li enrichment in the coolant was found to be effective in reducing the production rate of long-lived radionuclides.

The alloying elements for vanadium alloy (V-3Ti-1Si) do not produce significant quantities of long-lived radionuclides. However, the impurity elements will affect the waste classification of the disposed TITAN-I components and, therefore, should be controlled. Niobium is the most important among the elements shown as impurities in the vanadium alloy [2]. The amount of the long-lived radionuclide from niobium, ^{94}Nb , was found to depend strongly on the ^6Li enrichment. The production rate of ^{94}Nb can be reduced by a factor of nine if a 60% enriched lithium is used instead of natural lithium composition. Detailed evaluation of radioactive-waste-disposal ratings of the TITAN-I design is given in Section 13.7.

10.3.2. Reference Design

Based on the neutronics optimization considerations of Section 10.3.1, the reference design of the TITAN-I reactor is determined, and is illustrated in Figure 10.3-2.

The structural material is the vanadium alloy, V-3Ti-1Si. The lithium coolant is enriched to 30% ^6Li which is a compromise between neutronics performance and enrichment cost (Section 10.2.2.1). The first-wall region is 1-cm thick and consists of 40% structure, 37.5% lithium, and 22.5% void. The composition and size of the first wall is dictated by thermal-hydraulic considerations. The first wall is followed by a 1-cm gap and by the TITAN-I integrated blanket coil (IBC) which is 28-cm wide and consists of 18% structure, 72% lithium, and 10% void. The IBC is followed by a 1-cm gap and a hot shield. The width of the IBC section is chosen such that the shield lifetime is 4 FPY (five reactor years), based on the damage rate at the front of the shield. The hot shield is 45-cm wide and is made of two zones. The first zone is 30-cm thick and composed of 30% structure and 70% lithium; the second zone is 15-cm thick with 90% structure and 10% lithium. The coolant channels in the hot shield have a rectangular cross section so that there is no void. If the cost of vanadium alloy proves to be very high, the shield material may be replaced by an appropriate "modified" low-activation stainless steel without imposing large penalties on tritium breeding, nuclear heating, afterheat, and magnet-shielding issues.

The neutronics performance of the reference design with a vanadium-alloy shield is given in Table 10.3-III, as calculated by a 1-D analysis. The TBR is 1.33, of which 1.084 is from the $^6\text{Li}(n,\alpha)\text{T}$ reaction, and 0.247 from the $^7\text{Li}(n,n'\alpha)\text{T}$ reaction. The total nuclear heating is 16.05 MeV per DT neutron (11.11 MeV from neutron nuclear heating and 4.94 from γ -rays), resulting in a blanket energy multiplication of 1.14. The maximum fast-neutron fluence at the OH magnet after 30 FPY of operation at 18 MW/m^2 of neutron wall loading was found to be $7 \times 10^{26}\text{ n/m}^2$ (for $E_n \geq 0.1\text{ MeV}$), which is substantially lower than the estimated lifetime limit of $2 \times 10^{27}\text{ n/m}^2$ for the spinel insulator.

A set of more accurate, 3-D neutronics calculations was performed with the Monte-Carlo code, MCNP [97], taking into account the toroidal geometry and the divertor modules. Figure 10.3-3 shows the plan view of the 3-D MCNP model for the TITAN-I reactor which includes half of the 13° segment for the divertor module in the toroidal direction. The elevation view of the model, also illustrated in Figure 10.3-3, shows the opening of the divertor in the outboard region. The divertor coils are located 5 cm from the first wall and protected by a two-zone divertor shield, which is made of 10% lithium and 90% vanadium alloy. The first zone is 15-cm thick and is replaced after one FPY

RADIUS (m)		LIFETIME
0.0	PLASMA	
0.60	VOID	
0.66	FIRST WALL	1 FPY
0.67	VOID	
0.68	IBC 72% Li, 18% V, 10% VOID	
0.96	VOID	
0.97	HOT SHIELD (1st ZONE) 70% Li, 30% V	4 FPY
1.27	HOT SHIELD (2nd ZONE) 10% Li, 90% V	
1.42	VOID	
1.43	OH MAGNET 70% COPPER, 10% HELIUM 10% STRUCTURE, 10% SPINEL	30 FPY
1.85		

Figure 10.3-2. Schematic of the blanket and shield for the TITAN-I reference design.

Table 10.3-III.
NUCLEAR PERFORMANCE
OF THE TITAN-I REFERENCE DESIGN^(a)

⁶ Li enrichment	30%		
Tritium-breeding ratio:			
⁶ Li (n,α)	1.084		
⁷ Li (n,n'α)	0.247		
TOTAL	1.33		
Blanket energy multiplication, <i>M</i>	1.14		
Nuclear heating (MeV per DT neutron):			
<u>Component</u>	<u>Neutron</u>	<u>Gamma-Ray</u>	<u>Sum</u>
First wall	0.341	0.183	0.524
Blanket	7.382	2.603	9.985
Shield (1st zone)	3.148	1.595	4.743
Shield (2nd zone)	0.235	0.560	0.795
TOTAL	11.106	4.941	16.047
OH coils	0.038	0.438	0.476

(a) From 1-D ANISN calculations.

Figure 10.3-3. Plan view (A) and elevation view (B) of the 3-D MCNP model for the TITAN-I FPC.

Figure 10.3-4. Tritium-breeding ratio from MCNP calculations for the TITAN-I design as a function of ^6Li enrichment in lithium.

of operation, while the second region is 35-cm thick and is replaced after 4 FPY. The maximum fast-neutron fluence at the OH magnet in the inboard region, calculated from the 3-D model, was found to be $1.6 \times 10^{27} \text{ n/m}^2$; this fluence is well below the assumed lifetime limit for the spinel insulator. A detailed description of the neutronics performance of the divertor module is given in Section 11.7.

The TBRs found for the 3-D models are summarized in Table 10.3-IV and depicted in Figure 10.3-4 as a function of ^6Li enrichment in lithium. The net TBR was found to be 1.18 for the reference design, where the ^6Li enrichment is 30%; about 0.96 of the tritium breeding is contributed by the $^6\text{Li}(n,\alpha)\text{T}$ reaction.

The ^6Li enrichment of 30% was selected for the reference design because of the improved afterheat, good magnet protection performance, and acceptable enrichment cost. Future optimization of the TITAN-I design may be possible by considering different low-activation reflector materials to reduce cost and by more detailed trade-off studies between ^6Li enrichment, cost, annual replacement mass, and waste-disposal concerns.

Table 10.3-IV.
TRITIUM-BREEDING RATIOS IN THE TITAN-I DESIGN
CALCULATED BY MCNP

Region	From ${}^6\text{Li}$	From ${}^7\text{Li}$	TOTAL TBR
CASE 1 - 30% ${}^6\text{Li}$^(a)			
First wall	0.0106	0.0068	0.017
Blanket	0.5355	0.1735	0.709
Hot shield (1st zone)	0.2764	0.0404	0.417
Hot shield (2nd zone)	0.0186	0.0005	0.019
Divertor shields	0.0216	0.0011	0.023
TOTAL	0.962	0.222	1.184
CASE 2 - 90% ${}^6\text{Li}$			
First wall	0.0150	0.0010	0.016
Blanket	0.6306	0.0245	0.655
Hot shield (1st zone)	0.3626	0.0058	0.368
Hot shield (2nd zone)	0.0176	0.0001	0.018
Divertor shields	0.0295	0.0001	0.030
TOTAL	1.055	0.032	1.087
CASE 3 - Natural Li			
First wall	0.0067	0.0091	0.016
Blanket	0.3800	0.2380	0.618
Hot shield (1st zone)	0.3420	0.0580	0.400
Hot shield (2nd zone)	0.0144	0.0007	0.015
Divertor shields	0.0105	0.0013	0.012
TOTAL	0.754	0.307	1.061

(a) Reference TITAN-I design.

10.4. THERMAL AND STRUCTURAL DESIGN

The thermal-hydraulic design is intimately coupled to the power conversion scheme, the safety design of the reactor, the configuration, and the neutronics aspects of the first wall and blanket. Power-cycle efficiency is affected by the coolant inlet and exit temperatures. Safety-design requirements dictate that the structural temperature, temperature gradient, coolant pressure, and pressure drop be low enough that the design limits on temperature, pumping power, and material stress are not exceeded. The thermal-hydraulic design should also facilitate the removal of decay afterheat under accident conditions and be favorable to blanket energy multiplication and tritium breeding.

A high thermal conductivity and a high heat capacity are among the desirable properties of a coolant. Liquid metals are generally excellent coolants in this regard, because of their high thermal conductivity and the high boiling point, as compared to water. Therefore, the FPC thermal energy can be removed at a much higher temperature, resulting in a higher thermal-energy conversion efficiency. The removal of thermal energy at high temperatures is also possible with gas coolants such as helium. However, high operating pressure and large pumping power are necessary because of low density and low specific-heat capacity of gas coolants.

The TITAN-I FPC is cooled by liquid lithium. Several other studies have also considered liquid lithium as coolant for fusion reactors [98-100]. The eutectic mixture of lithium and lead, LiPb, has been proposed [101] and used in reactor studies such as MARS [102] and CRFPR [103]; but fusion neutrons produce polonium radionuclide from the lead. One of the issues for liquid-metal coolants in fusion reactors is the MHD-induced pressure drop, a consequence of the high electrical conductivity of the coolant. In an RFP fusion reactor such as TITAN, the toroidal magnetic field at the first wall is quite small, thus the MHD pressure drop can be kept low by aligning the coolant channels with the poloidal field.

This section summarizes the thermal-hydraulic design of the TITAN-I FPC, including the thermal analysis of the first wall, blanket, and shield (Sections 10.4.2 and 10.4.3), the MHD-induced pressure drops, and the required pumping powers (Section 10.4.4). The thermal-hydraulic design window for lithium-cooled, high-power-density RFP reactors is discussed in Section 10.4.5, and a detailed thermal and structural analysis of the first wall and blanket using finite-element techniques is given in Section 10.4.6. The coolant-circulation pumps are reviewed in Section 10.4.7, and the impact of high-velocity coolant on the erosion of the first wall is discussed in Section 10.4.8.

10.4.1. Coolant-Channel Configuration

The geometry, size, and configuration of the coolant channels are important parameters in thermal-hydraulic design. Orientation of the coolant channels with respect to the magnetic field greatly affects the MHD pressure drop. Channel geometry and size affect the coolant velocity and heat transfer. In the RFP confinement concept, the dominant magnetic field at the plasma edge (or first wall) is the poloidal field. The coolant channels in the first wall and blanket are, therefore, aligned with the poloidal field in order to minimize the induced MHD effects.

Table 10.4-I summarizes the heat load on the TITAN-I FPC components. The first wall is exposed to a radiation heat flux of about 4.6 MW/m^2 in the TITAN-I design, while in the blanket and hot shield the heat load is mainly from the volumetric nuclear heating. Tubular coolant channels with circular cross section are suitable for the first wall, since a circular tube has the best heat-transfer capability (highest Nusselt number) and highest strength. In addition, tubes are easy to manufacture with small tolerances on size and wall thickness. The requirements of heat-transfer capability and material strength for the blanket-coolant channels are less stringent because of the much smaller heat load and low coolant velocity.

Figures 10.4-1 and 10.4-2 show the elevation view and horizontal (midplane) cross section of the coolant channels used in the TITAN-I FPC. The first wall is a bank of circular coolant tubes. To adjust for the shorter toroidal length on the inboard as compared to the outboard side, the first-wall tubes slightly overlap on the inboard side of the torus, as is shown in Figure 10.4-2. Therefore, two sets of coolant tubes are used with one set having a slightly larger poloidal diameter. The inside diameter and wall thickness of the first-wall coolant tubes are, respectively, 8 and 1.25 mm. The inside diameter of the first-wall tubes reflects a compromise between the total number of coolant tubes and the heat-transfer coefficient; reducing the diameter increases the heat-transfer coefficient, but it also increases the number of tubes and may result in a lower reliability (more likelihood of tube failure). The tube wall thickness of 1.25 mm includes a 0.25-mm allowance for erosion even though the sputtering erosion of the first wall is estimated to be negligible (Section 5).

The blanket- and shield-coolant channels are designed with the consideration of heat transfer, blanket energy multiplication, tritium breeding, and shielding requirements. The TITAN-I blanket is configured as an integrated blanket coil (IBC) [104] and serves as the toroidal-field coil and part of the oscillating-field current-drive coils. The heat-transfer requirements of the blanket and shield are not as stringent as for the first-wall-

Table 10.4-I.
HEAT LOAD (MW) ON TITAN-I FPC COMPONENTS

Power to First Wall	
Surface heating	462.2
Nuclear heating	
Structure	115.9
Coolant	118.1
Pumping power ^(a)	37.7
Total	<u>735.9</u>
 Power to IBC	
Nuclear heating	
Structure	373.3
Coolant	870.1
OFCD joule heating ^(b)	17.0
TF-IBC joule heating	25.0
Pumping power ^(a)	6.0
Total ^(c)	<u>1292.0</u>
 Power to Hot Shield	
Nuclear heating	
Structure	458.9
Coolant	247.3
Pumping power ^(a)	3.0
Total	<u>709.2</u>

(a) A pump efficiency of 90% is assumed.

(b) Joule heating in the IBC, which is also the toroidal-field driver coil of the OFCD system, and from eddy currents (1 MW).

(c) Excluding 168.8 MW which is deposited in the divertor IBC.

Figure 10.4-1. Elevation view of the TITAN-I first-wall and blanket-coolant channels.

Figure 10.4-2. Horizontal, midplane cross section of the TITAN-I fusion power core through blanket and divertor regions.

coolant channels, and there is a greater margin for varying the size, shape, and wall thickness of the coolant channels. The final design of the blanket and shield is based on design iterations among thermal-hydraulic design, structural analysis, neutronics, and IBC electrical engineering aspects.

The overall thickness of the blanket and shield is 75 cm. The IBC zone is 28-cm wide, is located 1 cm behind the first wall, and consists of 6 rows of tubular coolant channels. Each coolant channel has an inside diameter of 4.75 cm and a wall thickness of 2.5 mm. The primary reason for using tubular coolant channels for the IBC zone, which results in more voids, is to reduce the number of load-bearing welded joints near the plasma. The IBC-coolant channels have varying cross sections (Figure 10.4-2) in order to minimize the void fraction of this zone. The resulting IBC zone consists of 18% structure, 72% lithium, and 10% void by volume.

The hot shield is located 1 cm behind the IBC, is 45-cm thick, and has two zones. The first zone is 30-cm thick and consists of 5 rows of square coolant channels with outer dimensions of 6 cm and a wall thickness of 5 mm. The inside corners are rounded and have the radius-to-wall thickness ratio of unity. The structure volume fraction is 30%, the coolant volume fraction is 70%, and there is no void. The second zone of the hot shield is 15-cm thick and consists of 4 rows of rectangular channels with thick walls to increase the structure volume fraction in this zone. The structure volume fraction is 90% and the coolant volume fraction is 10%. The channels have outer dimensions of 11.25 cm by 3.75 cm and a wall thickness of 16.25 mm.

Figure 10.4-3 shows a detailed cross section of the TITAN-I FPC including the manifolds, inlet and outlet headers, coolant channels, and the coolant flow directions. The coolant flow in both first wall and blanket are single pass in the poloidal direction. In the hot shield however, the poloidal flow circuit is double pass, as is illustrated in Figure 10.4-3. Lithium flows in through the first three square channels of the hot shield, makes a 180° turn at the inboard side, and exits through the last two square channels and the rectangular channels of the hot shield. Because of this double-pass flow pattern, the hot shield can be constructed as two separate units. During the annual maintenance, the top half of the shield will be removed so that the torus assembly (including the first wall, IBC, and divertor modules) can be replaced. The estimated lifetime of the shield is 4 FPY and, therefore, this portion can be reinstalled after the completion of the annual maintenance.

Figure 10.4-3. Elevation view of the TITAN-I first-wall and blanket-coolant channels showing the manifolds, headers, and coolant flow direction.

Figure 10.4-4. The thermal-analysis model of the first-wall-coolant tubes.

10.4.2. Thermal Analysis of the First Wall

Analytical estimates of the thermal performance of the first-wall tubes have been made in order to define the thermal-hydraulic design window. These analyses were then verified by finite-element analysis (Section 10.4.6). Figure 10.4-4 shows the notation for the dimensions, heat fluxes, volumetric heating rates, and temperature drops in the first-wall-coolant tubes. The inner radius of the tube is denoted by a , the outer radius by b , and the wall thickness by $t = b - a$. The heat load comprises radiation heat flux (q_o'') and volumetric nuclear heating in the wall (q_w''') and coolant (q_f'''). The radiation heat flux on the plasma-facing surface of a first-wall-coolant tube is represented by a cosine distribution, with a maximum at $\theta = 0$ denoted as point A in Figure 10.4-4.

The heat conduction along the axis of the tube can be neglected in the analysis since the temperature gradient in the axial direction is much smaller than that along the radial or circumferential (θ) directions. Furthermore, because of the large aspect ratio of the TITAN-I design, the surface heat flux and volumetric heating rates do not vary appreciably along the axis of the coolant tube. The ratio of peak-to-average neutron wall loading (and volumetric heating rate) is estimated to be $\sim 8\%$ (Section 3) and the variation in the surface heat flux is expected to be smaller. The wall temperature can,

therefore, be expressed as

$$T_w(\theta, z) = T_f(z) + \Delta T_f(\theta) + \Delta T_w(\theta), \quad (10.4-1)$$

where T_w is the wall temperature, ΔT_f is the film temperature drop, and ΔT_w is the temperature drop across the wall. The coolant temperature at point z along the tube axis, $T_f(z)$, can be calculated from energy balance equation to be

$$T_f(z) \approx T_{in} + \frac{(2/\pi) b q_o'' + a^2 q_f''' + (b^2 - a^2) q_w'''}{a^2 U \rho_f c_p} z, \quad (10.4-2)$$

where T_{in} is the inlet temperature of the coolant, L is tube length, U is the mean coolant velocity, and ρ_f and c_p are, respectively, the density and specific heat capacity of the coolant.

One of the main constraints on the thermal-hydraulic design is the maximum temperature of the structure. Examination of Equation 10.4-1 shows that the maximum wall temperature occurs at the coolant exit point

$$T_{w,Max} = T_{ex} + (\Delta T_f + \Delta T_w)_{Max}, \quad (10.4-3)$$

where T_{ex} is the coolant exit temperature, $T_{ex} = T_f(z = L)$, and L is the length of the tube.

A complete 2-D solution of the temperature field in the tube wall and in the fluid would adequately describe the temperature distribution, but such an approach is not useful for parametric surveys of the thermal-hydraulic design window. Because of the strong variation of the heat flux on the first-wall tubes and the resulting circumferentially varying Nusselt number, a 2-D analysis of the coolant temperature distribution has been performed for laminar flow; for the turbulent-flow regime, an empirical correlation for the Nusselt number with a correction for nonuniform surface heat flux is used. The first-wall tubes are thin, however, and the temperature drop across the tube wall, ΔT_w , can be estimated accurately by assuming that the heat conduction is local and one dimensional in the radial direction.

At any radial position r and any circumferential angle θ around the first-wall tube, the temperature profile is

$$T(r) = T_{wi} + \frac{q_w'' b^2}{2k_w} \ln\left(\frac{r}{a}\right) - \frac{q_w'''}{4k_w} (r^2 - a^2) + \frac{q_o'' \cos(\theta) b}{k_w} \ln\left(\frac{r}{a}\right), \quad (10.4-4)$$

where T_{wi} is the temperature at the inner surface of the wall and k_w is the thermal conductivity of the wall material. The maximum wall temperature drop, therefore, occurs at point A ($\theta = 0$) of Figure 10.4-4, where the heat flux is maximum:

$$\Delta T_{w,Max} = \frac{q_w''' b^2}{2k_w} \ln \left(\frac{b}{a} \right) - \frac{q_w'''}{4k_w} (b^2 - a^2) + \frac{q_o'' b}{k_w} \ln \left(\frac{b}{a} \right). \quad (10.4-5)$$

10.4.2.1. Laminar heat transfer in the first-wall tubes

For a laminar coolant flow in the first-wall tubes, the film temperature drop is calculated from the 2-D solution of the convection equation

$$\frac{\partial^2 T}{\partial r^2} + \frac{1}{r} \frac{\partial T}{\partial r} + \frac{1}{r^2} \frac{\partial^2 T}{\partial \theta^2} = \frac{u(r)}{\alpha} \frac{dT}{dz}, \quad (10.4-6)$$

where $\alpha = k_f / (\rho_f c_p)$ is the thermal diffusivity of the coolant and $u(r)$ is the fully developed, laminar velocity profile of the coolant. To account for the flattening of the velocity profile caused by the perpendicular magnetic field, a power velocity profile for $u(r)$ is assumed

$$u(r) = U \left(\frac{m+2}{m} \right) \left[1 - \left(\frac{r}{a} \right)^m \right], \quad (10.4-7)$$

where U is the mean coolant velocity

$$U = \frac{2}{a^2} \int_0^a r u(r) dr. \quad (10.4-8)$$

The exponent m would be large for a flat velocity profile and $m = 2$ for a parabolic profile.

The boundary and symmetry conditions for Equation 10.4-6 are

$$k_f \left. \frac{\partial T}{\partial r} \right|_{r=a} = q''(\theta) \quad (10.4-9)$$

$$\left. \frac{\partial T}{\partial \theta} \right|_{\theta=0} = \left. \frac{\partial T}{\partial \theta} \right|_{\theta=\pi} = 0$$

and the surface heat flux, $q''(\theta)$, is given by

$$q''(\theta) = \begin{cases} q_{NH}'' + q_o''(\theta) & 3\pi/2 \leq \theta \leq \pi/2 \\ q_{NH}'' & \pi/2 \leq \theta \leq 3\pi/2 \end{cases} \quad (10.4-10)$$

where q''_{NH} is the average heat flux at the interface between the tube wall and the coolant (from nuclear heating in the tube wall):

$$q''_{NH} = \frac{(b^2 - a^2) q''_w}{2a}. \quad (10.4-11)$$

Equation 10.4-6 can be solved by the method of separation of variables [105], and the film temperature drop, ΔT_f , is found to be

$$\Delta T_f(\theta) = f(m) \frac{a}{\pi k_f} \int_0^{2\pi} q''(\omega) d\omega - \frac{a}{\pi k_f} \int_0^{2\pi} q''(\omega) \ln \left[2 \sin \left(\frac{\theta - \omega}{2} \right) \right] d\omega, \quad (10.4-12)$$

where

$$f(m) = \frac{(m+2)^2 - 4}{4m(m+2)} - \frac{(m+2)^2}{m^2} \left[\frac{m}{8(m+4)} - \frac{m+2}{(m+1)(m+2)^3} \right]. \quad (10.4-13)$$

Using Equation 10.4-12, the Nusselt number, $Nu(\theta)$, can be determined:

$$Nu(\theta) = \frac{2a q''(\theta)}{k_f \Delta T_f(\theta)}, \quad (10.4-14)$$

which shows that the Nusselt number varies along the circumference of the tube. For a parabolic velocity profile ($m = 2$) and a uniform heat flux, Equations 10.4-12 and 10.4-14 result in $Nu = 48/11 = 4.36$. When the velocity profile is flat ($m \rightarrow \infty$) and the heat flux is uniform, $Nu = 8$, which is the well-known result for the slug flow.

When the surface heat flux is not uniform, the Nusselt number and film temperature drop vary along the circumference of the tube. Figure 10.4-5 shows this variation, as computed from Equations 10.4-12 and 10.4-14 for the case of $q''_o = 4.6 \text{ MW/m}^2$ and $q''_{NH} = 0.12 \text{ MW/m}^2$. The Nusselt number is positive for θ from 0 to about 90° as the surface temperature equalizes with the average coolant temperature. In the vicinity of $\theta = 90^\circ$, the Nusselt number is infinite, reflecting that ΔT_f is zero at this point. The Nusselt number then become negative because the average coolant temperature is higher than the wall temperature.

The maximum film temperature drop occurs at $\theta = 0$ (point A in Figure 10.4-4), where $Nu \simeq 3.0$ instead of the average value of 7.1, as has been calculated for uniform heat flux and transverse Hartmann number greater than 100 [106]. This lower value of the Nusselt number should be used in determining the film temperature drop; otherwise the maximum wall temperature would be seriously underestimated, considering that the maximum heat flux also occurs at this point.

Figure 10.4-5. Variation of the Nusselt number (A) and the film temperature drop (B) around the circumference of the coolant tube for $q''_o = 4.6 \text{ MW/m}^2$ and $q''_{NH} = 0.12 \text{ MW/m}^2$.

The effect of the nonuniform surface heat flux can be reduced or eliminated by utilizing the swirl flow of the coolant in the tube [107] which can be produced by helical vanes laying inside the tube. The curvature of the tube also introduces secondary flow patterns similar to swirl flow [108]. However, the effect of the magnetic field on the swirl flow has not been investigated and, therefore, the swirl-flow patterns have not been used for the TITAN-I design.

10.4.2.2. Turbulent heat transfer in the first-wall tubes

For a given size for the first-wall tubes, the maximum wall temperature constraint would result in a maximum limit on the surface heat flux. A higher surface heat flux can be accommodated by using a turbulent coolant flow, which is accompanied by a higher Nusselt number. The magnetic field, generally, tends to suppress turbulence in the flow of an electrically conducting fluid, and the onset of turbulence would occur at higher Reynolds numbers (for non-MHD pipe flow, onset of turbulence occurs at Reynolds numbers in excess of 2300). The Reynolds number is defined as $Re = \rho_f U d_h / \eta_f$, where d_h is the hydraulic diameter of the channel and η_f is the viscosity of the coolant.

Experimental data on MHD turbulent flow are limited. Some experimental data in the presence of a transverse magnetic field are reported [109-111] with Hartmann numbers ranging up to 1300. Experimental data in the presence of longitudinal magnetic field are also reported [112,113], but the Hartmann number was low (up to about 150). The onset of turbulence was detected from pressure fluctuations and skin friction data, and it has been shown that the onset of turbulence is delayed by both the transverse and longitudinal fields, with the effect of the transverse field being stronger. The transition Reynolds number, Re_t , corresponding to the onset of the turbulence, was proposed to be directly proportional to the Hartmann number as [114]

$$Re_t \geq \begin{cases} C_1 Ha_{\perp} & \text{for perpendicular field} \\ C_2 Ha_{\parallel} & \text{for parallel field} \end{cases} \quad (10.4-15)$$

where $Ha_{\perp}$ and $Ha_{\parallel}$ are, respectively, the transverse and longitudinal Hartmann numbers and C_1 and C_2 are constants. Hoffman [114] suggested $C_1 = 500$ and $C_2 = 60$. The data from Gardner and Lykoudis [109], which extends to $Ha_{\perp} = 1300$, result in $C_1 \leq 250$, while the data from Globe [112] suggest $C_2 \simeq 40$. Lykoudis [115] suggested a value of 236 for C_1 and Branover *et al.* [116] suggested a value of 155 for circular tubes and square ducts.

An interesting aspect of Equation 10.4-15 is that the turbulent velocity, v_t , is independent of the channel size and is solely determined by the magnetic field strength and the coolant properties

$$v_t \geq \begin{cases} C_1 B_{\perp} (\sqrt{\sigma_f \eta_f} / \rho_f) \\ C_2 B_{\parallel} (\sqrt{\sigma_f \eta_f} / \rho_f), \end{cases} \quad (10.4-16)$$

where $B_{\perp}$ and $B_{\parallel}$ are, respectively, the perpendicular and parallel magnetic field and σ_f is the electrical conductivity of the coolant. Using the conservative values of $C_1 = 500$ and $C_2 = 60$ [114], a poloidal flow velocity of about 20 m/s is estimated for the liquid-lithium flow in the first wall of the TITAN-I design.

Few studies are available on the turbulent-flow heat transfer in liquid metals in the presence of a magnetic field. Kovner *et al.* [117] performed experiments on the effect of a longitudinal magnetic field on turbulent heat transfer in liquid-gallium flow in a tube. The following empirical correlation for Nusselt number was then proposed

$$Nu = 6.5 + \frac{0.005 Pe}{1 + 1890 (Ha_{\parallel} / Re)^{1.7}}, \quad (10.4-17)$$

where $Pe = Re Pr$ is the Peclet number and $Pr = \eta_f c_p / k_f$ is the Prandtl number. Equation 10.4-17 reduces to that for ordinary turbulent flow for $Ha_{\parallel} = 0$. This equation also predicts the expected effects of the magnetic field: the Nusselt number increases with increasing field strength, while for a given Re , the Nusselt number approaches the value for laminar flow as $Ha_{\parallel}$ increases. Conversely, for a given $Ha_{\parallel}$, the Nusselt number approaches its value for standard liquid-metal correlations without magnetic field as Re is increased. Even though Equation 10.4-17 is based on experimental data up to $Ha_{\parallel} = 550$, it is expected to hold beyond this range.

The experimental data from Gardner and Lykoudis [118], with transverse magnetic field and Hartmann number ranging up to $Ha_{\perp} = 1300$, show similar dependence. Krasilnikov [119] performed a numerical study on turbulent-flow heat transfer in a channel between two parallel plates in the presence of a transverse magnetic field. His results also show similar effects of the magnetic field on turbulent-flow heat transfer. Branner *et al.* [116] reported experimental evidence to suggest that under certain conditions the magnetic field may increase turbulence intensity in an anisotropic fashion and, as a result, the total thermal diffusivity will be greatly increased.

For the TITAN-I design, Equation 10.4-17 is used. Figure 10.4-6 shows the variation of the Nusselt number with Peclet number for $B = 0$, $Ha_{\parallel} = 1000$, and $Ha_{\parallel} = 3040$ (the

expected $Ha_{||}$ in TITAN-I first wall). The range of the experimental data is also shown. It is seen that the flow remains laminar up to large values of Peclet number as the Hartmann number is increased.

The nonuniform circumferential heat flux on the first-wall tubes will reduce the turbulent Nusselt number at the point of highest heat flux, as has been shown before for the case of laminar flow (Section 10.4.2.1). Until further data become available, the Nu given by Equation 10.4-17 is reduced by a factor of 2 for the TITAN-I design to account for this nonuniform circumferential heat flux.

10.4.3. Thermal Analyses of the Blanket and Shield

Thermal analyses of the blanket and shield are simpler than for the first wall. No radiation heat flux exists and the volumetric heating rates in individual coolant channels can be taken as uniform because the gradient of the volumetric heating across each channel is small. Therefore, the surface heat flux incident on the coolant will be uniform, and the Nusselt number can be assumed to be constant.

The TITAN-I IBC is made of tubular channels while the hot shield consists of rectangular coolant channels to minimize the void fraction. Since the volumetric heating rate decreases away from the first wall, thermal analysis for each row of coolant channels is performed separately. However, the analysis is similar for all types of coolant channels.

An outline of the thermal analysis for both the tubular blanket channels (similar to those of Figure 10.4-4) and for square channels (Figure 10.4-7) is given below. From the desired coolant inlet (T_{in}) and outlet (T_{ex}) temperatures, the average coolant velocity is obtained by energy balance.

$$U = \frac{(b^2 - a^2) q_w''' + a^2 q_f'''}{a^2 \rho_f c_p (T_{ex} - T_{in})} L, \quad (10.4-18)$$

where L is the length of the channel and $b = a + t$. The maximum wall temperature, as for the first wall, occurs at the exit point of the coolant:

$$T_{w,Max} = T_{ex} + \Delta T_f + \Delta T_w. \quad (10.4-19)$$

The film temperature drop, ΔT_f , is given by

$$\Delta T_f = \frac{(b^2 - a^2) q_w'''}{Nu k_f}. \quad (10.4-20)$$

Figure 10.4-6. Variation of the Nusselt number, Nu , with the Peclet number, Pe , for turbulent flow as predicted by Equation 10.4-17. The range of experimental data as well as the operating point of the TITAN-I first wall are also shown. The Nusselt number from this graph should be halved to account for nonuniform heat flux on the TITAN-I first wall.

Figure 10.4-7. The thermal-analysis model of the hot-shield-coolant channels.

Similar expressions for U and ΔT_f can be written for rectangular shield channels. The flow is always laminar in the blanket and shield channels, and the Nusselt number for laminar flow in a square channel with constant heat flux is 3.6 in the absence of the magnetic field. In the presence of the magnetic field with perpendicular Hartmann number greater than 100, the average Nusselt number would be close to 5.5.

The temperature drop across the wall, ΔT_w , can be estimated from a 1-D heat-conduction analysis of the wall. The temperature profile would be parabolic, and the wall temperature drop would be

$$\Delta T_w = \frac{(b-a)^2 q_w'''}{2k_w} \quad (10.4-21)$$

At the corners, ΔT_w will be slightly higher than that given by Equation 10.4-21, but the difference would not be significant for thin walls.

10.4.4. Pressure Drop and Material Stress

A liquid metal flowing in the presence of a perpendicular magnetic field encounters a combination of pressure drops caused by the magnetic body force and friction,

$$\Delta p = \Delta p_f + \Delta p_{MHD} \quad (10.4-22)$$

In a straight channel, the friction pressure drop, Δp_f , is given by

$$\Delta p_f = f \frac{\rho_f U^2 L}{2d_h}, \quad (10.4-23)$$

where U is the mean flow velocity of the coolant, L is the length of the channel, ρ_f is the density of the liquid metal, and d_h is the hydraulic diameter of the channel. The parameter f is the friction factor: for laminar flow, $f = 64/Re$; for turbulent flow, f is given as a function of Re and the surface roughness in the Moody friction-factor chart [120]. Semi-empirical equations are also available for friction pressure drop at bends, contractions, expansions, etc.

For a uniform straight channel in the presence of a constant, transverse magnetic field ($B_\perp$), the associated MHD pressure drop is given by [121]

$$\Delta p_{MHD} = \sigma_f U B_\perp^2 a \frac{\phi}{1 + \phi} L. \quad (10.4-24)$$

For a rectangular channel, a is half the length of the inner side of the channel parallel to the magnetic field; for a circular tube, a is the inside radius. The wall-to-coolant electrical-conductance ratio, ϕ , is

$$\phi = \frac{\sigma_w t}{a \sigma_f}, \quad (10.4-25)$$

where t is the channel wall thickness and σ_w and σ_f are, respectively, the electrical conductivities of the wall material and coolant.

It is difficult to derive an analytical relationship for the MHD pressure drop in a bend, a contraction, or a channel with a varying cross section or in a varying magnetic field. Semi-empirical equations are, however, available for these cases. The MHD pressure-drop equations which appear to be best suited for the TITAN-I design are discussed below [99]. At a contraction or along the length of a channel, where the magnetic field is varying, the MHD pressure drop is estimated as

$$\Delta p = 0.2 \sigma_f U B_\perp^2 a \sqrt{\phi}. \quad (10.4-26)$$

For flow through a bend, if both legs of the bend are normal to the magnetic field, then the MHD pressure drop is zero. If one leg of the bend is normal and the other leg is parallel, the MHD pressure drop is

$$\Delta p = \sigma_f U B_\perp^2 a \sqrt{\phi}. \quad (10.4-27)$$

In order to complete the thermal-hydraulic design, pressure and thermal stresses in the coolant channels were estimated by 1-D equations (along r) for a thick-walled tube [122,123]. The radial pressure stress, σ_r^p , the hoop stress, σ_θ^p , and the equivalent pressure stress, σ_{eq}^p , are given by

$$\begin{aligned}\sigma_r^p(r) &= \frac{a^2 p_i}{b^2 - a^2} \left(1 - \frac{b^2}{r^2}\right), \\ \sigma_\theta^p(r) &= \frac{a^2 p_i}{b^2 - a^2} \left(1 + \frac{b^2}{r^2}\right), \\ \sigma_{eq}^p(r) &= \sqrt{\frac{1}{2} [(\sigma_r^p - \sigma_\theta^p)^2 + (\sigma_r^p)^2 + (\sigma_\theta^p)^2]},\end{aligned}\quad (10.4-28)$$

where p_i is the coolant pressure and the external pressure to the tube is assumed to be zero. The radial, σ_r^T , circumferential, σ_θ^T , and equivalent, σ_{eq}^T , thermal stresses are given by

$$\begin{aligned}\sigma_r^T(r) &= \frac{\alpha E}{r^2} \left[\frac{r^2 - a^2}{b^2 - a^2} \int_a^b r T(r) dr - \int_a^r r T(r) dr \right], \\ \sigma_\theta^T(r) &= \frac{\alpha E}{r^2} \left[\frac{r^2 + a^2}{b^2 - a^2} \int_a^b r T(r) dr + \int_a^r r T(r) dr - r^2 T(r) \right], \\ \sigma_{eq}^T(r) &= \sqrt{\frac{1}{2} [(\sigma_r^T - \sigma_\theta^T)^2 + (\sigma_r^T)^2 + (\sigma_\theta^T)^2]},\end{aligned}\quad (10.4-29)$$

where α is the coefficient of linear expansion and E is the Young's modulus of the tube material. Equation 10.4-4 with $\theta = 0$ is used for the temperature profile in the above equations.

For rectangular coolant channels of the shield, the thermal stress is estimated by

$$\sigma^T \simeq \frac{\alpha E \Delta T_w}{2(1 - \nu)}, \quad (10.4-30)$$

where ν is Poisson ratio of the wall material. It is more difficult to estimate the pressure stress in square or rectangular channels because of bending and corner effects. A rough estimate can be obtained from

$$\sigma^p \simeq \frac{a}{t} p_i. \quad (10.4-31)$$

The effect of bends and corners depends on the ratio of the side length to the wall thickness, a/t . A two-dimensional analysis of the pressure stress in square channels was

performed by using the finite-element code, ANSYS, and the results are reported in Section 10.4.6. The finite-element analysis has given the maximum stress in the coolant channel wall as a function of a/t . For the parametric survey of the thermal-hydraulic design window, the size of each square or rectangular coolant channel is chosen such that for a given coolant pressure, the ratio a/t is smaller than the maximum value that is predicted by finite-element analysis.

10.4.5. Thermal-Hydraulic Design Window

10.4.5.1. Design considerations

The first wall of the TITAN-I design intercepts an average radiation heat flux of 4.6 MW/m^2 . In order to remove such a high heat flux, the first wall consists of small-diameter, circular tubes. Other thermal-hydraulic design features of TITAN-I are the separation of the first-wall and blanket-coolant circuits and the use of MHD turbulent-flow heat transfer to remove the high surface heat flux at the first wall. Figure 10.4-8 is a flowchart of the computer code used to perform the design calculations [124].

The objective of the calculations is to obtain a thermal-hydraulic design window based on the maximum allowable temperature and pressure stresses in the structure and pumping power. The maximum allowable temperature of the structure corresponds to a maximum value for the average coolant exit temperature for a given heat flux. The maximum allowable stress and pumping power result in minimum values for the average coolant exit temperature. Therefore, the design window for the coolant exit temperature is in between these limits. Other parameters influencing the design window are the neutron wall loading, the coolant channel size, and the coolant inlet temperature. Changing any of these parameters would alter the design window.

The input to the code includes the reactor parameters, physical properties of the coolant and structural material, coolant-channel geometry, and design constraints. For a given first-wall tube size and a given radiation heat flux on the first wall, the maximum wall temperature drop for the first-wall tube is found from Equation 10.4-5. Assuming a laminar flow, the maximum film temperature drop is then calculated from Equation 10.4-12, and the maximum-permissible coolant exit temperature for the first wall, $T_{ex,FW}$, is obtained from Equation 10.4-3. With this exit temperature, the average coolant velocity is computed from Equation 10.4-2. If $T_{ex,FW} \leq T_{in}$, or the coolant velocity is greater than or equal to the turbulent velocity, no solution for the laminar flow exists. In this case, the coolant velocity is set equal to the turbulent velocity, and the

Figure 10.4-8. Flowchart of the TITAN-I thermal-hydraulic design code [124].

film temperature drop, ΔT_f , is recalculated using the Nusselt number for turbulent flow from Equation 10.4-17 (which is halved to account for nonuniform circumferential heat flux). The coolant exit temperature is found using this value of the film temperature drop in the turbulent regime and, if necessary, the coolant velocity is increased until the maximum structure temperature, $T_{w,Max}$, is within the design limit.

For design window calculations, the blanket is treated as a lumped-parameter system. The blanket-coolant exit temperature, $T_{ex,BL}$, is determined by energy balance so that, using the determined $T_{ex,FW}$, the mixed temperature of the first-wall and blanket circuits would match the input value of average coolant exit temperature. The film and wall temperature drops are calculated from Equations 10.4-20 and 10.4-21, and the maximum temperature of the blanket-coolant channel is found from Equation 10.4-19. If the maximum wall temperature is within the design limit, the calculated average coolant exit temperature is recorded as the highest possible exit temperature for the imposed radiation heat flux and structural-temperature limit.

Pressure drop and pumping power are calculated from the coolant velocity. The equations given in Section 10.4.4 are used to estimate the MHD and friction pressure drops. The coolant pressure is highest at the inlet of a coolant channel. Pressure stresses are, therefore, determined at the inlet point of all circuits.

The above procedures are repeated for different values of mixed coolant exit temperature and then for a range of heat fluxes on the first wall.

10.4.5.2. Design window and reference design

The major parameters of the TITAN-I design are given in Table 10.1-I, and the power flow to various FPC components is listed in Table 10.4-I. Relevant parameters of the thermal-hydraulic design are given in Table 10.4-II.

Figure 10.4-9 illustrates the thermal-hydraulic design window for the TITAN-I FPC and shows that a design with a radiation heat flux on the first wall of 4.9 MW/m^2 is possible. The sudden change in the slope of the top curve in Figure 10.4-9, corresponding to the structural-temperature limit, is caused by the change in flow from laminar to turbulent. Also, the pumping-power limit of 5% of electric output is more restrictive in this regime than the pressure stress of 108 MPa. The thermal-stress limit is not reached up to the maximum heat flux on the first wall.

The coolant flow paths in the first-wall and blanket circuits of TITAN-I are shown in Figure 10.4-10, and the variation of the coolant pressure along the length of the coolant

Table 10.4-II.
THERMAL-HYDRAULIC DESIGN PARAMETERS

First-wall heat flux	4.6 MW/m ²
Poloidal field at first wall	5.4 T
Toroidal field at first wall	-0.36 T
Coolant inlet temperature	320 °C
Structural-temperature limit	750 °C
Pressure-stress limit	108 MPa
Thermal-stress limit	300 MPa
Pumping-power limit ^(a)	50 MW

(a) Pumping-power limit is set at 5% of the net electric output.

Figure 10.4-9. The thermal-hydraulic design window for the TITAN-I FPC.

- 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

First-Wall 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 Flow Circuit

Figure 10.4-10. Coolant flow paths in the first-wall and blanket circuits.

channels of the first wall and blanket is shown in Figure 10.4-11. For the blanket components, the pressure distributions in rows 1 and 6 of the coolant channels of the IBC zone are shown. The pressure drop in the hot shield is negligible because of the low power density and low coolant flow rate.

The coolant flow velocity in the TITAN-I first-wall tube is 21 m/s and the maximum pressure drop is 10 MPa. Since substantial simplification is made by cooling two components (first wall and divertor) from the same cooling circuit, the delivery pressure of the coolant pump for the first-wall and divertor-coolant circuits is set at 12 MPa, the maximum pressure drop at the divertor (Section 11.5). The coolant pressure for the first wall is then reduced to 10 MPa by using an orifice (Section 10.4.7).

The coolant velocities in rows 1 and 6 (last) of IBC-coolant channels are, respectively, 0.5 and 0.2 m/s. The pressure drop is 3.0 MPa in row 1 and 0.5 MPa in row 6. The supply pressure of the blanket coolant pump is 3 MPa. Orifices are used to reduce the pressure to the required values at the inlet of each row of IBC channels (Section 10.4.7).

The pressure and thermal stresses at the inlet of a first-wall tube are shown in Figure 10.4-12. The maximum equivalent pressure and thermal stresses are, respectively, less than 50 and 150 MPa. The allowable pressure stress is a function of material temperature, increasing with the decreased temperature. For a coolant tube, the pressure is highest at the inlet and lowest at the exit. Conversely, the wall temperature is lowest at the inlet and highest at the exit. Figure 10.4-13 presents the pressure stress, average wall temperature, and the allowable stress along the length of a first-wall tube. The pressure stress in the first-wall tubes is 4 to 6 times smaller than the allowable stress. The stresses in the blanket channels are well below the design limits because of the low pressure, thick channel walls, and the small temperature gradient. Finite-element analyses of the pressure and thermal stresses in the first-wall and blanket components have verified these results (Section 10.4.6).

The main results of the thermal-hydraulic analyses of the TITAN-I FPC are given in Tables 10.4-III through 10.4-V.

10.4.6. Finite-Element Thermal and Stress Analyses

Two-dimensional thermal and stress analyses of the TITAN-I FPC were performed by the finite-element code, ANSYS [125], in order to refine and confirm the thermal-hydraulic design estimates. The blanket and shield of the TITAN-I design are not subjected to a high surface heat flux. For the first-wall channels, however, accurate descriptions of both

Figure 10.4-11. Pressure distributions in the first-wall (A) and the blanket-coolant channels (B). The pressure drop in the hot shield is negligible. Points A through H correspond to the coolant flow path in Figure 10.4-10.

Figure 10.4-12. Pressure stresses (A) and thermal stresses (B) in the TITAN-I first wall as function of the radius of the coolant tube.

Table 10.4-III.

THERMAL-HYDRAULIC DESIGN OF TITAN-I FIRST WALL

Pipe outer diameter, b	10.5 mm
Pipe inner diameter, a	8.0 mm
Wall thickness, t	1.25 mm
Erosion allowance	0.25 mm
Structure volume fraction	0.400
Coolant volume fraction	0.375
Void volume fraction	0.225
Coolant inlet temperature, T_{in}	320 °C
Coolant exit temperature, $T_{ex,FW}$	440 °C
Maximum wall temperature, $T_{w,Max}$	747 °C
Maximum primary stress	50 MPa
Maximum secondary stress	288 MPa
Coolant flow velocity, U	21.6 m/s
Pressure drop, Δp	10 MPa
Total pumping power ^(a)	37.7 MW
Reynolds number, Re	1.90×10^5
Magnetic Reynolds number, Re_m	0.48
Parallel Hartmann number, $H_{ }$	3.04×10^3
Perpendicular Hartmann number, $H_{\perp}$	2.01×10^2
Parallel magnetic interaction parameter, $N_{ }$	48.6
Perpendicular magnetic interaction parameter, $N_{\perp}$	0.21
Nusselt number, Nu	10.35
Prandtl number, Pr	4.08×10^{-2}
Peclet number, Pe	7.76×10^3

(a) A pump efficiency of 90% is assumed.

Table 10.4-IV.

THERMAL-HYDRAULIC DESIGN OF TITAN-I IBC

Pipe outer diameter	52.5 mm
Pipe inner diameter	47.5 mm
Wall thickness	2.5 mm
Structure volume fraction	0.18
Coolant volume fraction	0.72
Void volume fraction	0.10
Coolant inlet temperature	320 °C
Coolant exit temperature	700 °C
Maximum wall temperature	747 °C
Maximum primary stress ^(b)	30 MPa
Maximum secondary stress	5 MPa
Coolant velocity ^(b)	0.5 m/s
Pressure drop ^(b)	2.8 MPa
Total pumping power ^(a)	6.0 MW
Reynolds number ^(b)	2.75×10^4
Magnetic Reynolds number ^(b)	0.066
Parallel Hartmann number ^(b)	1.9×10^4
Perpendicular Hartmann number ^(b)	1.3×10^3
Parallel magnetic interaction parameter ^(b)	1.33×10^4
Perpendicular magnetic interaction parameter ^(b)	6.15×10^1
Nusselt number ^(b)	5.6

(a) A pump efficiency of 90% is assumed.

(b) Values for the first row of IBC tubes.

Table 10.4-V.

THERMAL-HYDRAULIC DESIGN OF TITAN-I HOT SHIELD

Coolant inlet temperature	320 °C
Coolant exit temperature	700 °C
Maximum wall temperature	747 °C
Pressure drop	0.1 MPa
Total pumping power ^(a)	3.0 MW
First Zone	
Channel outer dimensions	60 × 60 mm
Wall thickness	5 mm
Structure volume fraction	0.30
Coolant volume fraction	0.70
Reynolds number	9.79×10^3
Parallel Hartmann number ^(b)	7.00×10^3
Perpendicular Hartmann number ^(b)	nil
Nusselt number	3.6
Second Zone	
Channel outer dimensions	112.5 × 37.5 mm
Wall thickness	16.25 mm
Structure volume fraction	0.90
Coolant volume fraction	0.10
Reynolds number	1.84×10^3
Parallel Hartmann number ^(b)	1.30×10^3
Perpendicular Hartmann number ^(b)	nil
Nusselt number	8.2

(a) A pump efficiency of 90% is assumed.

(b) Values for $B_p \sim 2T$, $B_t \sim 0$.

Figure 10.4-13. Safety factors for pressure stress in the first-wall tube.

thermal and pressure stresses should be included. 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 toroidal directions. A 2-D 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 [2,123], 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. Various assumptions and models for the finite-element analysis of the TITAN-I FPC are discussed in the next subsection and the results are summarized in the following subsections.

Figure 10.4-14. Displacement components for plane (A) and axisymmetric (B) problems.

10.4.6.1. Finite-element models

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:

$$\epsilon_{ij} = \frac{1}{2} \left(\frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} \right), \quad (10.4-32)$$

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 Figure 10.4-14 are assumed to have the following form:

$$\begin{aligned} u_1 &= u(x, y), \\ u_2 &= v(x, y), \\ u_3 &= 0. \end{aligned} \quad (10.4-33)$$

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,

$$\begin{aligned}\epsilon_{xx} &= \frac{\partial u}{\partial x}, \\ \epsilon_{yy} &= \frac{\partial v}{\partial y}, \\ \epsilon_{xy} &= \frac{1}{2} \left(\frac{\partial u}{\partial y} + \frac{\partial v}{\partial x} \right), \\ \epsilon_{zz} &= 0.\end{aligned}\tag{10.4-34}$$

The constitutive equations including thermal strains are

$$\epsilon_{ij} = \frac{1}{E} [(1 + \nu)\sigma_{ij} - \nu(\sigma_{xx} + \sigma_{yy} + \sigma_{zz})] + \alpha T, \tag{10.4-35}$$

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. Using Equation 10.4-35 for $\epsilon_{zz} = 0$,

$$\sigma_{zz} = \nu(\sigma_{xx} + \sigma_{yy}) - E\alpha T. \tag{10.4-36}$$

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:

$$\begin{aligned}\epsilon_{xx} &= \frac{1}{E'}(\sigma_{xx} - \nu'\sigma_{yy}) + \alpha'T, \\ \epsilon_{yy} &= \frac{1}{E'}(\sigma_{yy} - \nu'\sigma_{xx}) + \alpha'T, \\ \epsilon_{xy} &= \frac{1 + \nu'}{E'}\sigma_{xy}.\end{aligned}\tag{10.4-37}$$

Additional mechanical field equations include equilibrium and compatibility equations.

Plane-stress assumptions. These assumptions approximately correspond to the behavior of a thin disk that is free of loading at the flat ends:

$$\begin{aligned}u_1 &= u(x, y), \\ u_2 &= v(x, y), \\ \sigma_{zz} &= \sigma_{xz} = \sigma_{yz} = 0.\end{aligned}\tag{10.4-38}$$

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:

$$\begin{aligned}\epsilon_{xx} &= \frac{1}{E}(\sigma_{xx} - \nu\sigma_{yy}) + \alpha T, \\ \epsilon_{yy} &= \frac{1}{E}(\sigma_{yy} - \nu\sigma_{xx}) + \alpha T, \\ \epsilon_{xy} &= -\frac{1+\nu}{E}\sigma_{xy}, \\ \epsilon_{zz} &= -\frac{\nu}{E}(\sigma_{xx} + \sigma_{yy}) + \alpha T.\end{aligned}\quad (10.4-39)$$

Comparing these expressions with Equation 10.4-37 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 Equation 10.4-36. 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

$$\begin{aligned}u_1 &= u(x, y), \\ u_2 &= v(x, y), \\ u_3 &= 0.\end{aligned}\quad (10.4-40)$$

The strain components are:

$$\begin{aligned}\epsilon_{xx} &= \frac{\partial u}{\partial x}, \\ \epsilon_{yy} &= \frac{\partial v}{\partial y}, \\ \epsilon_{xy} &= \frac{1}{2}\left(\frac{\partial u}{\partial y} + \frac{\partial v}{\partial x}\right), \\ \epsilon_{zz} &= \frac{u}{r}.\end{aligned}\quad (10.4-41)$$

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. Note that 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

$$\sigma_{eq} = \sqrt{\frac{1}{2}[(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_1 - \sigma_3)^2]}, \quad (10.4-42)$$

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 associated with a given 3-D stress state and the distortional energy caused by the uniaxial equivalent stress. Note that a hydrostatic state produces zero equivalent stress.

10.4.6.2. Thermomechanical analysis of the first wall

In this section the results obtained from plane-stress and axisymmetric analyses of the TITAN-I first wall are discussed. Pressure stresses as well as thermal stresses are calculated. The geometry analyzed is based on a circular tube with inner diameter of 8 mm, outer diameter of 10.5 mm, and radius of curvature of 66 cm. The internal pressure is 10 MPa. The radiation heat flux is uniform and emanates from the center of curvature of the first-wall tube and, hence, is incident only on the inner side of the tube. The heat flux on the circumference of a tube is assumed to be a cosine distribution, as is mentioned in Section 10.4.2. The peak heat flux is 4.6 MW/m², the internal heat-generation rate is 195 MW/m³, the fluid bulk temperature is 427 °C, and the Nusselt number is 10.34. The calculations were performed using ANSYS [125]. The cross section of the first-wall tube is shown in Figure 10.4-15 and was modeled by utilizing symmetry. The boundary conditions for the thermal and mechanical analyses are also explained on Figure 10.4-15.

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 41 MPa in the plane-stress analysis and 39 MPa in the axisymmetric analysis. The latter stress is lower because the curvature produces tensile stresses in the poloidal direction, and the stress state becomes closer to a hydrostatic state.

Figure 10.4-15. Model of the TITAN-I first-wall tube for finite-element analysis and the thermal and mechanical boundary conditions.

Figure 10.4-16. Temperature contours in the first wall of the TITAN-I design.

Temperatures. The maximum temperature and the temperature drop across the wall are lower than in the uniform-heat-flux case because of the cosine distribution of the surface heat flux. The temperature contours in Figure 10.4-16 are from the axisymmetric analysis. Results from the 2-D models are also very close to those given in Figure 10.4-16, and show the weak dependence of the thermal field on the fairly large radius of curvature. The maximum temperature of 741°C occurs at the point closest to the plasma, and the minimum temperature of 429°C is at the point farthest from the plasma at the inner radius. These results are in good agreement with the 1-D analysis reported in Section 10.4.5.2.

Thermal stresses. The equivalent stresses from the plane-stress model are shown in Figure 10.4-17. Figure 10.4-18 illustrates the stress contours of the poloidal and equivalent stresses, both of which are based on axisymmetric analysis. The asymmetry of the temperature field causes significant poloidal stresses with stress contours resembling the temperature contours. The maximum and minimum stresses are, respectively, 98.7 and 0.17 MPa in the plane-stress model, and 279 and 11.6 MPa in the axisymmetric model. Although ignoring the poloidal stresses in the calculation of the pressure stresses does not cause significant errors and proved to be conservative, the thermal stresses developed in a thin disk are different from those in an axisymmetric structure. Plane-stress results still

Figure 10.4-17. Equivalent stress in the first wall of the TITAN-I design from the plane-stress model.

give a good estimate of the magnitude of the stresses but have to be treated cautiously because of their non-conservative nature. Also, the asymmetric temperature distribution caused by the one-sided heating raises the stress level even further.

Summary: Assuming a value of 108 MPa as the allowable primary stress and 750 °C as the maximum wall temperature for the vanadium alloy, all structural design criteria are satisfied and the current design is feasible. Future analyses should address the effect of the poloidally varying coolant temperature and possible methods to relieve some of the poloidal thermal stresses.

10.4.6.3. Thermomechanical analysis of blanket and shield

The TITAN-I blanket consists of an array of self-supporting tubes. The blanket structure is subjected to heat generation and internal coolant pressure. Based on arguments given above, an axisymmetric finite-element model was set up for the analysis of the blanket structure. The results from the thermal-hydraulic code indicate that a low blanket-coolant pressure of 1 to 3 MPa is required (Section 10.4.5.2). Accordingly, the

Figure 10.4-18. Poloidal stresses (A) and equivalent stresses (B) in the first wall of the TITAN-I design from the axisymmetric model.

blanket tubes will have a large ratio of radius-to-wall thickness, and a simple 1-D analysis is sufficient. The results of the thermal-hydraulic code have been verified by comparison to the finite-element results; the maximum primary and secondary stresses in the blanket are found to be about 30 and 5 MPa, respectively.

The hot shield of the TITAN-I design consists of a set of self-supporting, rounded-corner square and rectangular channels which offer the advantage of a lower void fraction, improving both the neutron energy multiplication and the shielding of the OH magnets. Coolant pressure is the main factor determining the size of the shield channels. Figure 10.4-19 shows the contours of the primary equivalent stresses, displaying stress concentrations at the corners. Figure 10.4-20 gives the dependence of the primary stresses on the channel geometry. Increasing the channel size would result in higher bending stresses at the corners. The radius of the fillet acts oppositely (*i.e.*, larger radius relieves stress concentration). Figure 10.4-20 is used in the thermal-hydraulic design (Section 10.4.5.1). For a given coolant pressure (< 3 MPa in TITAN-I shield) and a given allowable primary stress (108 MPa), the appropriate aspect ratio for the channel (c/t) is found from Figure 10.4-20. The wall thickness is then independently determined by the heat-transfer calculations based on maximum allowable wall temperature, which, together with c/t , completely prescribe the size of the channel.

Figure 10.4-19. Contours of the equivalent primary stresses in TITAN-I shield.

Figure 10.4-20. Geometry of the shield channels and the sensitivity of the pressure stresses to the channel geometry.

10.4.7. Coolant-Circulation Pumps

The pressure drops in the first-wall and divertor circuits are much higher than in the blanket circuit. The maximum pressure drops in the divertor, first-wall, and blanket-coolant circuits are, respectively, 12, 10, and 3 MPa. In order to simplify the design, the first-wall and divertor coolants are supplied from the same circuit. Therefore, two separate coolant pumps are used to circulate the lithium: one for the first-wall and divertor circuit, and the second for the blanket and shield circuit. The pressure drop in the intermediate heat exchanger is small (~ 0.1 MPa). The delivery pressure of the first-wall and divertor-coolant pump is 12 MPa. A single orifice is used to reduce the lithium pressure to 10 MPa for the first-wall circuit (Figure 10.4-11). Additional orifices are used, where necessary, to reduce the coolant pressure from 12 MPa to the required inlet pressure of the individual rows of divertor-coolant tubes. The blanket-coolant pump has a delivery pressure of 3 MPa. Orifices are used to reduce the pressure to match the requirements for each row of IBC and shield channels.

Figure 10.4-21. A schematic of the coolant circulation in the TITAN-I reactor.

Figure 10.4-21 is a schematic of the coolant flow in TITAN-I. All of the coolant-circulation pumps are centrifugal pumps. The pump capacity, Q (gallon per minute, GPM), the pump speed, N (rpm), and the head, H (feet of the fluid pumped) of a centrifugal pump are related through the specific speed, N_s (rpm) [126]. Conversion to SI units (Q in m^3/s and H in m) results in

$$N_s = 2.12 \times 10^{-2} \frac{N \sqrt{Q}}{H^{3/4}}. \quad (10.4-43)$$

The fusion power core of TITAN-I is divided into three sectors separated by the three divertor modules. The coolant flow rates per sector are $1.2 \text{ m}^3/\text{s}$ (16,000 GPM) in the first-wall and divertor circuit, and $1.1 \text{ m}^3/\text{s}$ (14,500 GPM) in the blanket and shield circuit. Centrifugal pumps for feed water in large central stations have stage pressure of 7 MPa and speed of 9,000 rpm [126]. Therefore, a two-stage centrifugal pump with stage pressure of 6 MPa is suggested for the first-wall and divertor circuit and a single-stage pump is used for the blanket circuit. From Equation 10.4-43, the required speed of the first-wall and divertor pump, at the capacity of $1.2 \text{ m}^3/\text{s}$, is 8,100 rpm. The required speed of the blanket and shield pump is 5,200 rpm at the capacity of $1.1 \text{ m}^3/\text{s}$. The specific speed is assumed to be 2,000 rpm which results in a pump efficiency of about 90%.

10.4.8. First-Wall Erosion

Material erosion is one of the critical issues of the first-wall design. While a thin wall will reduce the temperature drop across the wall of the coolant tube, a thicker wall is necessary to reduce the pressure stress and material erosion. Therefore, it is necessary to include an erosion allowance so that enough material remains to keep the pressure stress within the design limit at the end of life of the first wall. The erosion allowance for the first-wall tube is 0.25 mm, which is adequate according to the present state of understanding of the erosion mechanisms and the quantification of various erosion rates.

Several potential processes for the erosion of first-wall material have been identified:

1. Sputtering by ions and neutral atoms,
2. Erosion by the high-velocity coolant impact and/or cavitation,
3. Mechanical abrasion caused by the impact of high-velocity suspended foreign particles,
4. Fretting abrasion resulting from flow-induced vibration of the coolant tubes,
5. Corrosion caused by chemical reaction with the coolant.

The TITAN reactors operate with a highly radiative plasma and utilize high-recycling divertors as the impurity-control system. As a result, the particle flux on the first wall is substantially reduced and the edge-plasma temperature is below the sputtering threshold. The sputtering erosion of the TITAN-I first wall is expected to be less than 0.1 mm/y (Section 5). Erosion from the impact of lithium and foreign particles is expected to be very small. Cavitation is not likely because the coolant pressure is high and the vapor pressure of lithium is low. To prevent vibration, the first-wall-coolant tubes are rigidly attached to each other by tack-welding the adjacent tubes together at the back.

Corrosion of vanadium alloys by lithium is not well tested at this time. Hays [52] measured a total depth of material removal of 7.6×10^{-3} mm (or 0.133 mm/y) in a 500-h test of corrosion resistance of Nb-1Zr alloy exposed to 1073 to 1143 °C liquid lithium flowing at 50 m/s. Given the very low corrosion rate of Nb-1Zr by hot lithium flowing at 50 m/s, it can be expected that the lithium coolant at 350 °C flowing in the first-wall tube at 21 m/s will have a very small corrosion effect on vanadium alloys. A detailed discussion of the first-wall erosion is given in Section 10.2.2.3.

10.4.9. Summary

The thermal-hydraulic design of the first wall and blanket of the TITAN-I reactor is feasible using liquid lithium as the coolant. This design is economically attractive because of the high exit temperature of the blanket coolant and the resulting high thermal-energy conversion efficiency of 44% (Section 10.6). The main design features and improvements which have contributed to the feasibility of this design are as follow:

1. Use of high-recycling divertors so that the first-wall sputtering erosion is almost negligible;
2. Use of small-diameter, thin-walled circular tubes as coolant channels for the first wall;
3. Separation of the first-wall and blanket-coolant circuits;
4. Alignment of the coolant channels with the dominant, poloidal magnetic field;
5. Use of turbulent-flow heat transfer to remove the high heat flux on the first wall.

The present design of the TITAN-I FPC is based on the most suitable experimental data and appropriate extrapolations available. A number of critical issues remain. The MHD pressure-drop equations for bend, contraction, and varying magnetic field need to be substantiated by further large-scale experiments and numerical and theoretical analyses. In MHD turbulent-flow heat transfer, experimental data should be extended to cover a wider range of Hartmann numbers especially suited for the TITAN-I regime of operation (*i.e.*, high coolant velocity in a relatively weak magnetic field). The combined effect, if any, of the parallel and perpendicular magnetic fields on flow transition and turbulent-flow heat transfer should also be investigated.

The effect of nuclear volumetric heating in the coolant on film temperature drop has been neglected in the present design, as it has been in most previous fusion-reactor design studies. For high volumetric nuclear heating in the coolant, the additional film temperature drop could be appreciable [127] and should be included in future design studies.

10.5. MAGNET ENGINEERING

Three types of magnets are used in the TITAN-I design: normal-conducting copper coils for the ohmic-heating (OH) and equilibrium-field trim magnets, normal-conducting, integrated blanket coils (IBC) for the divertor and toroidal-field (TF) magnets, and superconducting coils for the equilibrium-field (EF) magnets. Each coil system has a unique set of operating conditions that lead to unique constraints on the coil design. A detailed magnetics design of the TITAN-I magnet systems is presented in Section 4. The engineering and design constraints of TITAN-I magnet systems are discussed in the following subsections.

A poloidal cross section of the TITAN-I FPC is shown in Figure 10.5-1. The OH, EF, and trim coils are symmetric about the midplane and the upper OH coils are labeled 1 through 7 in a counterclockwise direction. The six concentric blanket pipes shown in this figure are the IBCs which produce the toroidal magnetic field. Figure 10.5-2 shows the poloidal cross section of the TITAN-I design through one of the divertor modules, illustrating the divertor IBCs.

10.5.1. Ohmic-Heating Coils

The OH coils are used for start-up and subsequently utilized as part of the poloidal-field (PF) driver-coil set of the oscillating-field current-drive (OFC) system. The TITAN-I OH coils are normal conducting with copper as the conductor and helium as the coolant. Water coolant was ruled out because of the TITAN-I safety guideline that no water is allowed inside the primary-loop containment structure because of the presence of the liquid-lithium coolant of the FPC (Section 13.3). The insulating material is spinel (MgAl_2O_4), chosen for its superior resistance to irradiation damage (Section 10.2.3).

The TITAN-I design has 14 OH coils, connected in series, with the same current density. Table 10.5-1 shows the size, location, and current (number of turns) in each coil as have been determined by the magnetics constraints (Section 4) and the plasma start-up requirements (Section 6). The TITAN-I start-up scenario is chosen such that the start-up power is directly extracted from the power grid without requiring any on-site power-storage system (other than the coils themselves).

The TITAN-I start-up switching sequence is shown in Figure 10.5-3. The corresponding current and voltage waveforms are presented in Figure 10.5-4. The TITAN-I start-up sequence uses a bipolar swing of the OH-coil currents and begins with charging the OH

Figure 10.5-1. Poloidal cross section of the TITAN-I FPC illustrating the coil layout.

Figure 10.5-2. Poloidal cross section of the TITAN-I FPC through one of the divertor modules, illustrating the divertor IBCs.

Table 10.5-I.
PARAMETERS OF TITAN-I POLOIDAL-FIELD COILS

Function	$R^{(a)}$ (m)	$\pm z^{(a)}$ (m)	$\Delta R^{(a)}$ (m)	$\Delta z^{(a)}$ (m)	$I^{(b)}$ (MA)	$j^{(c)}$ (MA/m ²)	Mass ^(d) (tonne)	Turns
Trim	5.77	1.20	0.20	0.30	0.	0.	15.9	10
EF	6.50	2.49	0.70	0.70	-9.62	19.78	144.9	88
OH-1	6.00	1.20	0.20	0.30	0.75	12.50	16.5	10
OH-2	4.11	2.41	0.40	0.42	2.10	12.50	31.7	28
OH-3	3.29	2.40	0.30	0.20	0.75	12.50	9.1	10
OH-4	2.71	1.65	0.30	0.60	2.25	12.50	22.4	30
OH-5	2.25	1.43	0.60	0.30	2.25	12.50	18.6	30
OH-6	2.15	0.81	0.40	0.78	3.90	12.50	30.8	52
OH-7	2.15	0.21	0.40	0.39	1.95	12.50	15.4	26

(a) Each coil has a mean radius of R , a height of z (coils are symmetric about the equatorial plane), and a cross section of $\Delta R \times \Delta z$.

(b) Mean steady-state values for the EF coils and back-bias values for the OH coils for a symmetric bipolar swing.

(c) Averaged over the entire coil cross section.

(d) A density of 7.3 tonne/m³ is assumed.

coils to their full back-bias values. The OH coils are then discharged into a transfer resistor, while the EF coils are connected in parallel to the OH coils ("Formation and fast discharge" phase in Figures 10.5-3 and 10.5-4). The value of the transfer resistor is set by the constraint on the maximum voltage across the superconducting EF coils. The fast-discharge phase lasts about 1 to 2 seconds.

As the OH coils are discharging, the voltage across the circuit drops. When the voltage across the OH coils reaches that of the grid power supply, the transfer resistor is disconnected from the circuit and the grid power supply is directly applied to the OH and EF coils ("Slow-ramp" phase). The OH coils are driven to their full, forward-bias current value and the EF coils and the plasma to their respective steady-state currents. The voltage of the grid power supply is usually a few kilovolts and its value is determined by the maximum power from the grid. The current-drive system begins operation during this phase and will be fully operational at the burn phase, maintaining the steady-state current in the plasma. With the proper magnetics design of the OH and EF coils, this start-up scenario will provide approximate plasma equilibrium throughout the start-up sequence (Section 6). A pair of small, normal-conducting trim coils are used to maintain exact equilibrium during the start-up sequence and are also used during the steady-state operation for plasma-equilibrium control and OFCD cycles (Section 7). After achieving the steady-state burn condition, the OFCD system is initiated while the OH coils are discharged slowly, from the full forward-bias current value to zero, in order to minimize the recirculating power and the coil-cooling requirements. Initiation of OFCD operation during the slow-ramp phase is advantageous.

The electrical requirements for each of the 14 OH coils are listed in Table 10.5-1 for a symmetric bipolar swing (i.e., forward- and back-bias current values are the same). Electrical engineering aspects of OH and EF coils (voltages and power consumption) are discussed in Section 6. In this section, magnetic engineering issues such as removal of joule heat during the current swing, and the magnetic forces on the coils and associated support structure are discussed. Some of the features of the TITAN-I final PF-coil design which mitigate these concerns are: (1) the lower value of the current density in the OH coils has reduced the joule losses, (2) the inner OH coil (coils 6 and 7) are positioned in a vertical stack to simplify the design of the support structure and to help ease the FPC maintenance, and (3) the distance between the EF coil and OH-1 (the OH coil closest to the EF coil) is increased to reduce the vertical forces on both OH-1 and EF coils during the start-up sequence.

Figure 10.5-3. Start-up sequence for the TITAN-I reactor: (A) Charge-up - OH coils are charged to full back-bias value, (B) Formation and fast discharge - OH coils are discharged through a transfer resistor (EF coils are connected in parallel to the OH coils), (C) Slow ramp - grid power drives the OH coils to full forward bias current and the EF coils to their steady-state value, (D) Transition - the OH coils are slowly discharged while OFCD is initiated, and (E) Steady state - current-drive system is fully operational. Initiation of OFCD operation during the slow-ramp phase is advantageous.

Figure 10.5-4. Schematic of coil currents and voltages during the TITAN-I start-up sequence corresponding to Figure 10.5-3: (A) Charge-up, (B) Formation and fast-discharge, (C) Slow-ramp, (D) Transition, and (E) Steady-state phases. Note that the time axis is not in scale.

10.5.1.1. Heat removal

During normal operation, about 14 MW of heat is generated in the OH and trim coils by the OFCD cycles (OH and trim magnets are also the poloidal-field drive coils of OFCD) as well as by nuclear heating. During the start-up sequence which lasts about 120 s, however, a much larger amount of heat is produced in the OH coils with peak heating rates in the range of 200 to 300 MW (depending on the details of the start-up sequence such as the charge-up time, maximum power from the grid, *etc.*). The TITAN-I OH-magnet cooling system is designed only to handle the steady-state load of normal operation. The additional joule heating in the copper conductor of the OH coils during the start-up sequence ($\sim 200 \text{ J/cm}^3$) is absorbed by the coils, and adiabatically raises the temperature of the copper conductor by about 60°C (from 20 to 80°C).

The internal design of the OH coils is shown in Figure 10.5-5. Coolant channels are provided on one face of the conductor while the insulator material, spinel (MgAl_2O_4), is plasma-sprayed on the opposite face. The conductor is then packed in a double-pancake form. The current enters the top pancake and exits through the bottom one, as is shown in Figure 10.5-5 (the coolant channel inside the conductor is aligned in the vertical direction in this figure). A coolant plenum is added inside each coil casing and is fed by six coolant inlets, located 60° apart in the toroidal direction. The helium coolant in the plenum enters between the two pancakes, flows vertically through the coolant channels in the conductor, and exits at the top and bottom into the two outlet plena. The inlet and exit temperatures of the helium coolant are, respectively, 20 and 80°C during the normal operation of the reactor. For a coolant pressure of 20 atm, the total coolant flow rate (OH and trim coils) is about $14 \text{ m}^3/\text{s}$, resulting in a peak velocity of about 5 m/s in each coil. Lower-pressure coolant could be used but would result in a higher coolant velocity in the coils.

The coolant for the OH-2 through OH-7 coils is supplied through six pairs of vertical manifolds. Each pair is located 60° apart in the toroidal direction and feeds into the corresponding inlet and outlet of each OH coil. Assuming a maximum coolant velocity of 20 m/s in the manifolds, the diameter of each manifold would be about 30 cm; smaller size manifolds are also possible but require a higher coolant velocity. The coolant for OH-1 and trim coils is supplied through the EF shield (see Figure 10.5-1) which also cools the shield itself.

Figure 10.5-5. The cross section of one of the TITAN-I OH or trim coils (A) and the conductor for the OH and trim coils (B). A coolant channel is provided on one face of the conductor with the insulator material plasma-sprayed on the opposite face.

Table 10.5-II.

VERTICAL FORCES ON PF COILS DURING START-UP (MN)

Coil	Back Bias	Forward Bias
Trim ^(a)	0.	0.
EF ^(b)	0.	-59.8
OH-1	-3.2	35.1
OH-2	-30.1	19.3
OH-3	-15.6	-1.1
OH-4	-43.8	2.9
OH-5	-28.2	-8.0
OH-6	-22.3	16.0
OH-7	-4.6	1.7

(a) Peak force on trim coils is about -26 MN.

(b) Peak repulsive force on EF coils is about +40 MN,
average force during steady-state operation is -43.9 MN.

10.5.1.2. Support structure for OH coils

The vertical forces on the upper OH coils during the start-up sequence are tabulated in Table 10.5-II. Negative values indicate forces towards the equatorial plane (attractive forces between each pair of coils). Because of the up-down symmetry of the coil set, the forces on lower coils are equal and opposite to those of the upper coils. Vertical forces on coils OH-4 through OH-7 are directly transmitted through the vertical stack of the coils. Vertical forces on the OH-1 and trim coils are much smaller than the weight of the upper EF shield and are supported by 12 columns between the upper and lower EF shields. The vertical forces on the EF coils are supported by 12 cold (cryogenic) columns. The oscillating forces on the TITAN-I coil system during OFCD cycles are described in Section 10.5.3.4.

Figure 10.5-6. The model geometry used for buckling analysis of the conical-shell support structure for the OH coils.

The support scheme for OH-2 and OH-3 coils uses a conical-shell framework, as is shown in Figure 10.5-1. The force of each coil is carried between neighboring coils by conical shells of structural steel. Structural requirements were determined by calculating the buckling force and applying a safety factor of 6. The required thickness of the support-shell, t , is given by [129]

$$t = \left[\frac{6F \sqrt{3(1-\nu^2)}}{2\pi E \cos^2 \alpha} \right]^{1/2}, \quad (10.5-1)$$

where F is the applied load (from Table 10.5-II), ν is Poisson ratio, E is Young's modulus, and α is the angle measured between the conical shell and vertical direction. Figure 10.5-6 illustrates the geometry. The calculated thicknesses of the conical shell for $\nu = 0.3$ and $E = 193$ GPa are listed in Table 10.5-III. Explicit dynamic analysis of the support structure was not performed. The safety factor of 6 takes into account the reversing stresses, from compression to tension [128]. The support shells are continuous in toroidal extent to avoid asymmetric sagging along the toroidal length of the coil.

10.5.2. Equilibrium-Field Coils

The TITAN-I design has only two superconducting coils, the equilibrium-field (EF) magnets. These coils are horizontal, circular coils with a mean radius of ~ 6.5 m and are located about 2.5 m above and below the midplane of the device. The diameter of these coils is chosen so that there is sufficient radial clearance between the coil and the FPC to allow vertical removal of the torus during annual maintenance. The coils are designed to last the lifetime of the plant and can, therefore, be permanently installed. A robust support system and adequate nuclear shielding will ensure that normal reactor transients will have little or no effect on the EF coils. Typical operating currents of the EF coils are listed in Table 10.5-I, and the magnetic forces are listed in Table 10.5-II.

The TITAN-I superconducting EF coils are made of NbTi conductor and steel structure. Because of their permanent nature, simple geometry, and the relatively low field produced by these coils, little or no extrapolation of current technology should be required. Therefore, detailed engineering analyses of the cryogenics systems, conductor design, and internal design of the coils, were not performed.

The TITAN-I design also uses a pair of small, normal-conducting EF-trim coils (Figure 10.5-1 and Table 10.5-I). The trim coils help to produce the exact equilibrium

Table 10.5-III.

THICKNESS OF STRUCTURAL-SUPPORT SHELL

Between Coils	Angle, α	Shell Thickness (cm)
OH-2 & OH-4	55°	2.7
OH-3 & OH-4	45°	1.6
OH-4 & OH-7 ^(b)	20°	3.1 ^(a)
OH-5 & OH-7 ^(b)	—	5.7 ^(a)
OH-6 & OH-7 ^(b)	—	6.7 ^(a)

(a) Based on compressional stress and not buckling.

(b) Carries the forces of the upper coils.

field during the start-up sequence and the OFCD cycles, and are used for controlling the plasma position during the steady-state burn. The design of the trim coils is similar to that of the OH coils and these trim coils are cooled and supported by the OH-coil cooling system and support structure (Section 10.5.1).

10.5.3. Integrated Blanket Coils (IBCs)

The divertor and the TF coils of the TITAN-I design are based on the integrated-blanket-coil (IBC) concept [104]. The IBC concept utilizes the lithium coolant of the primary circuit, flowing in the poloidal direction within the blanket, as the electrical conductor for the divertor and TF coils. Although the electrical resistivity of lithium is about 20 times that of copper, the low toroidal-field requirement of the RFP, combined with the large cross-sectional area available to the IBC and the recovery of the joule losses by the primary coolant, results in acceptable power requirements for the divertor and TF coils. The joule losses in the TITAN-I divertor and TF coils are, respectively, 24 and 120 MW.

The TITAN-I IBC also acts as the toroidal-field driver coil for the OFCD system. Intrinsic plasma processes in RFPs generate voltages and currents within the plasma in order to maintain the plasma in a near-minimum-energy state. This nonlinear coupling between the toroidal and poloidal magnetic fluxes in the plasma can be used to rectify current oscillations created at external coils into a net steady-state current in plasma. Detailed magnetics design of the TITAN-I OFCD system is reported in Section 7. The TF coils of TITAN-I oscillate at 25 Hz with TF-coil currents ranging between 30% and 170% of the mean steady-state value of 7.0 MA-turns (including the current in the divertor trim coils). The poloidal-field system also oscillates at 25 Hz. It is also necessary to oscillate the divertor coils to maintain the plasma separatrix at the proper location.

Several critical engineering issues which have been identified for the IBC design adopted for TITAN-I are: (1) steady-state and oscillating (OFCD) power-supply requirements for low-voltage, high-current coils; (2) time-varying forces caused by the OFCD cycles; (3) integration of the primary heat-transport system with the electrical systems; (4) sufficient insulation to stand off induced voltages; and (5) suitable time constants for various components to permit the coil currents to oscillate at 25 Hz. Heat removal is not an issue for the IBC because the joule heating is produced directly in the primary coolant.

10.5.3.1. Electrical engineering of toroidal-field IBC

Design of the power supplies is one of the critical issues in the engineering of the TF IBC because the IBC approach requires that the electrical and hydraulic systems be physically connected. These connections must be (1) made in the proper location to ensure that the electric current flows in the correct direction through the coils and (2) made away from the remote connect/disconnects of the hydraulics system in order to minimize the number of remote operations during torus replacement. Another operational constraint is that the intermediate heat exchangers (IHxs) and coolant pumps should be grounded (no electric current flowing through them).

Each TF IBC in TITAN-I is a one-turn coil with low voltage and high current (3.85 V, 520 kA per coil). Power supplies rated for such conditions would be expensive and would exhibit high internal-power losses if based on currently available technology. Large homopolar generators would have lower losses but they are a future technology. Connecting all TF IBCs of TITAN-I in series and/or providing more electrical turns in each IBC would raise the voltage of the power supply to a more manageable value. This method had to be ruled out, however, because of thermal-hydraulic considerations.

Figure 10.5-7 illustrates the electrical and hydraulic layout of the TITAN-I IBC system. The TITAN-I FPC comprises three sectors which are connected to each other through the divertor modules. To increase the power-supply voltage, the four IBCs in each sector are electrically connected in series which allows a better match of current and voltage for the power supply (15.4 V, 541 kA). This circuit, however, requires two IHxs per sector for the IBC-cooling circuit.

The TITAN-I TF IBC has the following resistances: each IBC coil, $R_{TF} = 7.40 \mu\Omega$; the cold leg of the coolant, $R_c = 142 \mu\Omega$ (from the coolant pumps to the FPC); and the hot leg of the coolant, $R_h = 366 \mu\Omega$ (from FPC to the IHX). Figure 10.5-7 shows that, because of the series connection of the IBCs and grounding of the pumps and heat exchangers, a leakage current will flow through the cold and hot legs. The leakage current is small in magnitude but causes unequal coil currents, necessitating a small balancing power supply to accompany each main power supply. The load on each balancing power supply (7.7 V, 27 kA) is much smaller load than that of the main power supply (15.4 V, 541 kA) and leaks through the cold legs to ground.

The piping layout (also shown in Figure 10.5-7) between the coils and heat exchangers is consistent with the flow paths and electrical circuit developed to minimize the current leakage. As a result, the electric current and coolant flow opposite to one another in two of the four TF coils in each sector. In the other two coils, the current and coolant

Figure 10.5-7. Schematic of the electrical and hydraulic layout of TITAN-I TF IBCs.

Figure 10.5-8. Electric bussing attachment to lithium duct.

both flow from the cold leg to the hot leg. Figure 10.5-8 shows the electrical bussing attachment of the power-supply leads to the lithium duct. Based on this design, the joule losses in all 12 TF coils are about 24 MW with an extra 1.95 MW of heating in the hot and cold legs.

10.5.3.2. Electrical engineering of divertor IBC

The impurity-control system of the TITAN-I design consists of three toroidal-field divertors (Section 5). Each divertor consists of one nulling coil and two flanking coils to produce the local effects necessary for field nulling. Because of the loss of coverage of TF IBCs in the divertor region, a pair of trim coils is added to each divertor in order to control the toroidal-field ripple and to minimize the toroidal extent of the perturbation. The detailed magnetics design of the IBC divertor coils is given in Section 4. The poloidal cross section of the TITAN-I FPC through one of the divertor modules is shown in Figure 10.5-2, illustrating the divertor IBCs.

The divertor IBCs operate at somewhat higher current densities than the TF coils, thereby requiring much greater voltages. Furthermore, the current in each nulling coil is exactly equal to that of the two flanking coils. The divertor IBCs are connected in

order to take advantage of the symmetric currents and larger voltages. Figure 10.5-9 shows the electrical and hydraulic layout of the divertor IBCs. The resistances of the nulling, flanking, and trim coils are, respectively, 0.47, 0.93, and 0.43 m Ω . The resistance of the hot leg is estimated to be 29 m Ω . Because the resistance of the cold leg is larger than that of the hot leg, the power supplies are grounded at the cold leg location; an arrangement similar to that of Figure 10.5-7 would require a current to flow to ground inside the power supply.

Only two power supplies per divertor module are required because the equalizing power supplies are not needed for the divertor IBCs (Figure 10.5-9). The first power supply, rated at 147.75 V and 169 kA, feeds the null and flanking coils. The second power supply, rated at 11.64 V and 135 kA, feeds the trim coils. The joule losses in the divertor IBCs (three divertors) are 117 MW with an additional 3.5 MW in the hot legs.

10.5.3.3. OFCD power supplies

The TITAN-I design operates at steady state using an oscillating-field current-drive (OFCD) system. Detailed magnetics design of the TITAN-I OFCD system is reported in Section 7. The TF coils of TITAN-I oscillate at 25 Hz with TF-coil currents ranging between 30% to 170% of the mean steady-state value of 7.0 MA-turns (including the current in the divertor trim coils). It is also necessary to oscillate the divertor coils to maintain the plasma separatrix at the proper location.

Inductive coupling of the reactor components (e.g., the TF and PF coils, plasma, plasma liner or first wall, and other conducting material surrounding the FPC) causes time delays in the current rise and fall, and induces voltage drops in addition to that caused by the resistance of the components. Therefore, the following requirements emerge: (1) IBC power supplies should be able to provide adequate steady-state and oscillating voltages and currents, (2) sufficient insulation should be added to standoff induced voltages, (3) suitable time constants for various components are required to permit the coil currents to oscillate at 25 Hz, and (4) the coil busing must be resized to handle the peak load.

Oscillation at 25 Hz requires a peak-to-minimum discharge time of 20 ms. The time constant for the TF IBC is calculated to be ~ 3.2 ms; therefore, more than six IBC time constants will elapse over the discharge period. The skin depth for the OFCD currents in the IBC was taken into account in calculating the performance of the OFCD system (Section 7). Also, the peak induced voltage by the OFCD cycles across a four-coil TF-IBC sector is about 400 V which is easily manageable by the spinel insulator.

Figure 10.5-9. Schematic of the electrical and hydraulic layout of TITAN-I divertor IBCs.

The power supply criteria listed above can be met in principle by using steady-state systems for the nominal values (Figure 10.5-8) and adding an additional oscillating supply to superimpose the required voltage and current variations. However, because of the large impedance of the toroidal-field circuit during the OFCD cycles (about $0.1\text{ m}\Omega$ for each TF coil), the oscillating voltage on each TF coil ($\sim 50\text{ V}$) is much larger than the steady-state value ($\sim 3.8\text{ V}$). If the impedance of the hot and cold legs is dominated by their resistances, the resulting joule heating in the hot and cold legs during the OFCD cycle would be much larger than the power supplied to the TF coils (Figure 10.5-8). Therefore, the design of the OFCD power supplies should take into detailed account the impedances of the hot and cold legs which in turn depend on the piping arrangement.

Detailed analyses of the OFCD power supplies and the leakage currents were not performed because of the complexity of the problem and because the results are very sensitive to the detailed arrangement of the pipings; the TITAN-I design is not in sufficient detail to warrant this level of electrical analyses. Instead, the leakage currents were calculated based on simple estimates of the internal inductances of coolant pipings. The internal inductances of the hot and cold legs of the TITAN-I design, based on low-frequency estimates for a round wire, are calculated to be in the range of 4 to $8\text{ }\mu\text{H}$ (0.6 to $1.2\text{ m}\Omega$) which would reduce the currents and, therefore, the joule losses in the hot and cold legs substantially. Since the direction of the leakage currents in adjacent hot legs or adjacent cold legs are different (Figure 10.5-8), the mutual inductances between these legs could also be exploited by pairing the appropriate legs to further increase the impedances, thereby reducing the leakage currents and the joule losses in the hot and cold legs.

It is necessary to oscillate the divertor coils during the OFCD cycle to maintain the plasma separatrix at the proper location. The OFCD analysis in Section 7 was performed assuming full coverage by TF coils, and the impact of the divertor coils was ignored. This analysis showed that the reversal parameter, F , ranges between -0.032 to -0.173 about the mean value of -0.1 during the OFCD cycle. Since the magnitude of oscillation of the toroidal flux was found to be small, the strength of the toroidal field on the plasma surface would be directly proportional to the reversal parameter, and the magnitude of the current oscillations in the divertor coils would be about $2/3$ of the steady-state value. The voltage oscillations applied to the divertor coils are roughly equal to the steady-state values, in contrast to the TF coils, because the divertor coils operate at much higher current densities and have higher resistances.

The analysis in Section 7 for the performance of the OFCD is scaled to include the impact of the divertor coils. This analysis of full-coverage TF coils predicts 18.23 and

29.15 MW of joule heating in the TF coils, respectively, for oscillating and steady-state portions. Noting that steady-state joule losses in the TF coils in the presence of the divertor coils (Section 4) are 24 MW, the joule losses in the TF coils during the OFCD cycle are estimated to be 25.6 MW for the steady-state portion (24 MW in the coil and 1.6 MW in the hot and cold legs) and 16 MW for the oscillating voltages (15 MW in the coils and 1 MW in the hot and cold legs).

The steady-state joule losses in the divertor coils were found to be 117 MW in the coils (Section 4) and 3.5 MW in the hot and cold legs (Section 10.5.3.2). Assuming that current oscillations in the divertor coils would be 2/3 of the steady-state value, the oscillating losses are estimated at 26 MW in the coils and 0.8 MW in the hot and cold legs. The resulting OFCD power-supply parameters for the divertor and TF-IBC systems are listed in Table 10.5-IV.

During the start-up sequence, particularly during the initial RFP-formation phase, the TF coils must be able to swing from a small positive current (12 kA) to a small negative current (-1.2 kA) within 10 ms. The circuit time constant of the TF IBC is calculated to be ~ 3.2 ms, therefore $\sim 95\%$ of the current swing will occur within the allotted 10 ms. The voltage induced by the start-up transient would peak at ~ 70 V, of the same order as the peak induced voltage by OFCD cycles. A thin layer of spinel insulator can withstand this voltage, even allowing for significant degradation resulting from radiation damage.

10.5.3.4. Oscillating coil forces

Interaction of the TF-IBC current with the reactor magnetic fields produces forces on the coils. These forces are of three kinds: (1) over-turning moments generated by the interaction of the IBC current with the vertical field, (2) centering forces resulting from the radial variation of the toroidal field, and (3) out-of-plane forces caused by the spatial variation of the magnetic fields near the separation of TF coils created by the divertor.

The magnitudes of the forces on the TITAN-I TF IBCs vary over time as the currents oscillate during the OFCD cycle. Structural support, therefore, is designed for the peak loads, as is listed in Table 10.5-V. These forces are much lower than the corresponding forces in tokamaks, since the coil currents are much lower in RFPs.

The OFCD cycles generate oscillating forces on the TF-IBC tubes. If the driving frequency is at or near the resonant frequency of the structure, large deformations and stresses can result, causing fatigue failure or catastrophic rupture. To assess the effect of

Table 10.5-IV.

PARAMETERS OF TITAN-I IBC POWER SUPPLIES

TF-IBC Power Supplies	Main	Balancing
Steady State		
Voltage (V)	15.4	7.7
Current (kA)	541.	27.
Cost (M\$)	2.75	0.25
Oscillatory		
Voltage	200.	100.
Current	420.	21.
Cost (M\$)	5.25	0.25
Busing cost ^(a) (M\$)	7.1	0.35
Total cost per sector (M\$)	15.1	0.85
Divertor-IBC Power Supplies	Null/Flank	Trim
Steady State		
Voltage (V)	148.	113.
Current (kA)	169.	135.
Cost (M\$)	3.0	2.25
Oscillatory		
Voltage	200.	160.
Current	113.	90.
Cost (M\$)	3.0	2.25
Busing cost ^(a) (M\$)	2.2	1.8
Total cost per sector (M\$)	8.2	6.3

(a) Based on a unit cost of 60 \$/kA-m.

Table 10.5-V.
PEAK FORCES ON TOROIDAL-FIELD IBC TUBES

	Average	Peak
Over-turning moment (kN m)	~ 0 .	1.6
Centering force (kN)	-0.1	-0.7
Out-of-plane force (kN)	1.6	-4.0

these oscillating forces on the lifetime of the TF-IBC tubes, a dynamic analysis must be performed to calculate the strain range experienced by the IBC tubes during each cycle and the result then compared to the minimum-strain range that will cause fatigue failure in V-3Ti-1Si.

The OFCD system produces both in-plane and out-of-plane forces which oscillate about some small average value. The cyclic in-plane forces are shown in Figure 10.5-10. The small steady force on the coolant tubes has little effect on the analysis of the cyclic strains induced by the OFCD. A key aspect of dynamic structural analysis concerns the resonant frequency of a cyclically loaded component. If the loading frequency is at or near the natural frequency of a particular deformation mode and the loads excite that mode, then very small loads can cause relatively large strains and deformations. A finite-element model was used to calculate the natural frequencies and mode shapes of the IBC tube. The IBC tube was modeled as a poloidal hoop with 60 two-node beam elements and two free ends that represent the inlet and outlet headers. The natural frequency of the out-of-plane deformation modes can be controlled by tying neighboring TF-IBC tubes together, thus increasing the lateral stiffness of the structure. This method raises the resonant frequency of the out-of-plane modes and is expected to prevent excessive strains caused by over-turning moments on the tubes. Hence, only in-plane forces and vibrations will be considered here.

The presence of the coolant has an impact on the natural frequency of the TF-IBC tubes. The tubes themselves provide all the structural stiffness of the component, but the added weight of the coolant changes their inertia, thereby lowering the natural frequency of the combined structure. This effect was modeled by increasing the density of the

Figure 10.5-10. Cyclic, in-plane forces induced in TF-IBC tubes by the OFCD system.

vanadium tubes from 6100 kg/m^3 to an effective density of 8232 kg/m^3 . This correction was calculated assuming that the total mass of the tube and coolant cross section is contained in the tube wall.

The lowest-frequency in-plane mode is basically a rotation about the attachment points at the ends of the straight portions of each IBC tube. The natural frequency of this mode is 5.4 Hz , which is well below the OFCD frequency. The natural frequencies of the next two in-plane vibration modes are 25 and 44 Hz . Fortunately, the in-plane forces are primarily radial, so no force exists to excite this mode. In addition to these bending modes, there are extensional deformation modes, the most fundamental of which represents a uniform radial deflection of the tube. These modes are always stiffer than the bending modes. For a complete ring, the natural frequency for this mode is about 1100 Hz , so the resonance of extensional deformation modes should not be a problem for TITAN-I. Because the oscillating forces caused by the OFCD are primarily radial, they will tend to excite the extensional modes and resonance should not be a problem.

The TF-IBC cyclic forces (Figure 10.5-10) were examined with a finite-element model to assess the impact of these forces on the blanket design. The peak stress was found to be only 0.3 MPa , corresponding to a cyclic-strain range of $3 \times 10^{-4}\%$. Since endurance

Figure 10.5-11. Fatigue behavior of V-15Cr-5Ti. Arrows indicate the strain value for very large number of cycles.

data were not available for V-3Ti-1Si, data for V-15Cr-5Ti were used. Figure 10.5-11 shows that the OFCD-induced strain range is three orders of magnitude lower than the endurance limit for V-15Cr-5Ti. Therefore, failure is not expected to occur as a result of the cyclic forces induced by OFCD. Compliant layers of metallic fibers are provided between TF-IBC tubes for support. Their resilience, which is not accounted for in this analysis, should dampen the loads even further.

10.6. POWER-CYCLE ANALYSIS

One of the advantages of using liquid lithium as the coolant for TITAN-I is the ability to remove the thermal energy from the reactor at a high thermal potential so that a high power-cycle efficiency can be realized. Both the inlet and exit temperatures of the primary coolant affect the power-cycle efficiency. The inlet temperature affects the pinch point and, hence, the maximum steam pressure in a Rankine power cycle. In a steam generator, the pinch point is the location at which the water temperature reaches the boiling point. At this pinch point, the temperature of the hot fluid must

be higher than the boiling point of water at the desired pressure by some amount for efficient heat transfer. Otherwise, a large heat-transfer area, which would correspond to a large steam generator, is required. The exit temperature determines the maximum steam temperature. High steam pressure as well as high steam temperature are essential for obtaining a high power-cycle efficiency.

The thermal-hydraulic design determines the inlet and exit temperatures of the primary coolant. Details of thermal-hydraulic design of the first wall and blanket are presented in Section 10.4 and the thermal design of the divertor is discussed in Section 11. An important feature of the thermal-hydraulic design of the TITAN-I first wall and blanket is the separation of the coolant circuits for these components in order to handle the high surface heat flux on the first wall. As a result, the first-wall coolant has a much lower exit temperature than that of the blanket and shield coolant. The divertor coolant also has a different exit temperature from those of the first-wall and blanket coolants. In effect, TITAN-I has three separate circuits for primary lithium: the first-wall, the divertor, and the blanket and shield circuits. The inlet temperatures of all three circuits are identical, while the exit temperatures are different. Therefore, several options for the power cycle are available and are discussed in Section 10.6.2. The total thermal power, coolant temperatures, and flow rates in these three circuits are given in Table 10.6-1, and the parameters of the reference power cycle is given in Section 10.6.3.

10.6.1. Secondary-Coolant Loop

The purpose of the secondary-coolant loop is to separate the primary coolant and the containment building from the power-generation cycle (*e.g.*, from water or steam of the steam cycle) for additional safety, even though the secondary loop and the intermediate heat exchanger (IHX) would degrade the thermal potential of the primary coolant. Lithium and sodium were considered for the coolant of the secondary loop. Liquid lithium is preferable to sodium for several reasons: (1) with lithium as the primary as well as secondary coolant, only one type of main coolant would be used in the plant; (2) storage and handling of the coolant would be much easier; (3) the heat capacity of lithium is about twice that of sodium, therefore, the total amount of coolant would be much less; (4) lithium is also less reactive with water than is sodium; and (5) the turbulent-flow heat-transfer capability of lithium is better than that of sodium which could result in smaller surface area in the IHX.

One advantage of sodium is the low solubility of tritium in sodium compared to lithium. Therefore, if sodium is used as the secondary coolant instead of lithium: (1) tri-

Table 10.6-I.
THERMAL-POWER DISTRIBUTION IN TITAN-I

First-wall coolant circuit

Thermal power	736	MWt
Inlet temperature	320	°C
Exit temperature	440	°C
Coolant flow rate	1464	kg/s

Divertor-plate coolant circuit

Thermal power	29	MWt
Inlet temperature	320	°C
Exit temperature	540	°C
Coolant flow rate	31	kg/s

IBC and hot-shield coolant circuit^(a)

Thermal power	2170	MWt
Inlet temperature	320	°C
Exit temperature	700	°C
Coolant flow rate	1363	kg/s

(a) Including divertor-IBC circuit.

tium inventory in the secondary loop would be smaller by about two orders of magnitude and (2) tritium permeation to the steam cycle might be lower depending on the partial pressure of tritium in the primary coolant. If the salt-extraction method is used to remove tritium from the primary coolant, the tritium concentration in the primary coolant could be reduced to less than 1 appm [5], corresponding to a tritium partial pressure of $\sim 10^{-9}$ torr. Since this pressure is much smaller than the obtainable pressure by cold trap in sodium at 115°C [130], the maximum partial pressure of the tritium in the secondary coolant (sodium or lithium) and tritium permeation to the steam side would be set by the tritium pressure in the primary coolant. Therefore, the only benefit in using sodium would be the much lower tritium inventory in the secondary coolant. For the TITAN-I design, it was decided that this advantage is not significant and overall it would be advantageous to use liquid lithium also as the secondary coolant.

10.6.2. Power-Cycle Options

The total thermal power of the TITAN-I reactor is removed by three primary-coolant circuits: the first-wall, divertor, and IBC and shield circuits (Table 10.6-I). The inlet temperature of lithium for all three circuits is 320°C but the exit temperatures are different (respectively, 440, 540, and 700°C for the first-wall, divertor, and IBC and shield coolants). Of the total thermal power, only about 1% is removed by the divertor coolant, about 25% by the first-wall coolant, and 74% by the IBC and shield coolant. Since the thermal power in the divertor circuit is very small, it would be advantageous to mix the divertor coolant with that from one of the other two circuits. If the first-wall and divertor coolants are mixed at the exit, the mixed exit temperature would be 442°C, leading to two separate streams of the primary coolant removing, respectively, 765 and 2170 MWt of power with exit temperatures of 442 and 700°C.

Figure 10.6-1 shows three possible variations of power cycles (A-C) for the TITAN-I design. In (A), the two streams from the first wall and divertor and from the IBC and shield are mixed at the exit (for a mixed exit temperature of 556°C) and then fed to only one power cycle. In (B), the two coolant streams are partially mixed in a special IHX, resulting in a higher steam temperature and pressure than in (A). Option (C) uses two separate power cycles for the two streams of coolant, one for the first-wall and divertor stream and the other for the IBC and shield stream. Each of these two power cycles has its own IHXs, steam generators, and turbine-generator set.

Power cycle (A) is the simplest and least costly, but it has a lower thermal efficiency compared to the other two options because of its lower primary-coolant exit temperature.

Figure 10.6-1. Power-cycle options for the TITAN-I design.

For power cycle (B), a new and complicated design of the IHX and steam generator is necessary, resulting in a higher capital cost compared to (A). The total capital cost of option (C) is likely to be the highest. The dual power cycles of option (C) are as simple as that of (A); the only difference is the duplication of several components and the increased total capital cost as a result of economies-of-scale. The overall thermal efficiency of option (C) is the highest because the primary-coolant streams are not mixed and the high exit temperature of the IBC and hot-shield coolant is efficiently used.

Economic comparison of the three power-cycle options of Figure 10.6-1 for TITAN-I has shown that option (C) would result in the most economical system because its higher thermal efficiency would offset the extra cost. Therefore, this option has been chosen as the reference power cycle for the TITAN-I design.

10.6.3. Reference Power Cycle

A schematic flow diagram of a reheat power cycle is presented in Figure 10.6-2. In this diagram, one high-pressure turbine (HPT), one reheater, one low-pressure turbine (LPT), and two regenerative feed-water heaters are shown explicitly. An intermediate-pressure turbine is also used in each cycle. The actual number of components varies for the two power cycles of TITAN-I.

The TITAN-I FPC consists of three sectors with one IHX and one steam generator per sector for the first-wall and divertor coolant stream. The steam produced in these three steam generators is mixed and fed to a single turbine-generator set. For the IBC and shield stream, two IHXs are used per sector based on electrical engineering requirements of the IBCs (Section 10.5.3). The secondary coolants of each pair of these IHXs are mixed and fed to one steam generator (per sector). As in the first-wall and divertor cycle, the steam from all three steam generators is mixed and fed to one turbine-generator set.

The power-cycle analysis is performed by the computer code PRESTO [131]. The pinch-point temperature difference in the steam generators of each of these power cycles is kept above 20 °C. For both the first-wall and divertor cycle and the IBC and shield cycle, the temperature drop in the IHXs is set at 20 °C.

Figure 10.6-3 shows the results of the analysis of the TITAN-I reference power cycles as temperature-energy diagrams. The first-wall and divertor power cycle is a superheat Rankine cycle with four stages of feed-water heating. No steam reheat after expansion through the high-pressure turbine is used. The throttle steam conditions (temperature and pressure) are, respectively, 396 °C and 10.7 MPa (1550 psia). The condenser back

Figure 10.6-2. Schematic of the flow diagram of a reheat power cycle.

pressure is 6.76×10^3 Pa (2 inches of mercury). The inlet temperature of the feed water is 169°C , and the temperature difference at the pinch point is 21.6°C . The total thermal power in this cycle is 765 MWt and the steam flow rate is 326 kg/s. The gross thermal efficiency of the first-wall and divertor power cycle is 37.0%.

The IBC and shield power cycle is a superheat Rankine cycle with two reheat stages and seven stages of feed-water heating. The temperature and pressure of the throttle steam are, respectively, 565.6°C and 21.4 MPa (3100 psia). The maximum steam temperature is limited to 565.6°C (1050°F), although higher temperatures are reported. The superheater and the reheaters are arranged in series. Figure 10.6-3 also shows the temperature-energy diagram for the IBC and shield power cycle. The condenser back pressure is 6.76×10^3 Pa (2 inches of mercury), similar to that of the first-wall and divertor power cycle. The inlet temperature of the feed water is 258°C and the temperature difference at the pinch point is 34°C . The total thermal power in this cycle is 2170 MWt and the steam flow rate is 703 kg/s. The gross thermal efficiency of this cycle is 46.5%.

The main results and parameters of the first-wall and divertor cycle and the IBC and shield cycle are given in Table 10.6-II. The overall gross thermal efficiency for the TITAN-I design is 44%.

Figure 10.6-3. Temperature-energy diagram for the first-wall and divertor (A) and IBC and shield (B) power cycles.

Table 10.6-II.
PARAMETERS OF THE TITAN-I POWER CYCLE

First-wall and divertor power cycle:		
Total thermal power in the primary coolant	765	MWt
Primary-coolant inlet temperatures	320	°C
Primary-coolant exit temperatures	442	°C
Secondary-coolant inlet temperatures	300	°C
Secondary-coolant exit temperatures	422	°C
Throttle steam temperature	396	°C
Throttle steam pressure	10.7	MPa
Steam flow rate	326	kg/s
Condenser back pressure	6.76×10^3	Pa
Stages of feed-water heating	4	
Feed-water inlet temperature	169	°C
Gross thermal efficiency	37.0%	
IBC and shield power cycle:		
Total thermal power in the primary coolant	2170	MWt
Primary-coolant inlet temperatures	320	°C
Primary-coolant exit temperatures	700	°C
Secondary-coolant inlet temperatures	300	°C
Secondary-coolant exit temperatures	680	°C
Steam temperature after 1st reheat	565.6	°C
Steam temperature after 2nd reheat	550.0	°C
Throttle steam pressure	21.4	MPa
Steam flow rate	703	kg/s
Condenser back pressure	6.76×10^3	Pa
Stages of feed-water heating	7	
Feed-water inlet temperature	258	°C
Gross thermal efficiency	46.5%	
Overall gross thermal efficiency	44.0%	

10.6.4. Summary

The total thermal power of TITAN-I is removed from the reactor core by two different primary-coolant streams: the first-wall and divertor circuit with a mixed exit temperature of 442°C, and the IBC and shield circuit with an exit temperature of 700°C. In order to maximize the overall thermal efficiency, two separate Rankine power cycles are used.

The first-wall and divertor power cycle is a superheat Rankine cycle with no reheat and four stages of feed-water heating with a gross efficiency of 37.0%. The IBC and shield power cycle is a two-stage reheat, superheat Rankine cycle with seven stages of feed-water heating with a gross thermal efficiency of 46.5%. The resulting overall gross thermal-conversion efficiency of TITAN-I is 44%.

10.7. SUMMARY AND CONCLUSIONS

The TITAN-I reactor is a compact, high-neutron-wall-loading (18 MW/m^2) design. A comprehensive list of the operating parameters of TITAN-I can be found in Appendix A. The TITAN-I fusion power core (FPC) is cooled by liquid lithium and uses a vanadium alloy (V-3Ti-1Si) as the structural material. The V-3Ti-1Si is chosen primarily because of its irradiation behavior, its high-temperature capabilities, and its compatibility with liquid lithium. The radiation lifetime of this alloy is estimated to be 15 to 18 MW y/m^2 , with the lower limit resulting in one year of operational life at 75% availability. The neutronics performance of this lithium-vanadium system is good, with a tritium-breeding ratio of 1.18 (from 3-D neutronics calculations), and an energy multiplication of 1.14 using 30% enriched lithium.

High-velocity (20 m/s) liquid lithium flowing in the turbulent regime cools the first wall and divertor plates. A thermal-hydraulics design window has been computed, revealing a maximum heat flux capability of $\sim 5 \text{ MW/m}^2$ under the conditions of TITAN-I. A separate low-velocity lithium circuit cools the IBC and hot shield. Separation of the two coolant circuits leads to the option of dual power cycles with a combined gross efficiency of 44%.

The TITAN-I blanket is arranged as an integrated blanket coil (IBC) with the lithium coolant also acting as the conductor for the toroidal-field and divertor magnets. The IBC eliminates the need for separate coils and neutron shielding for such coils, thereby simplifying the FPC and making the reactor torus assembly more accessible. The IBC system

requires high-current, low-voltage power supplies and appears feasible with current technology. The power-supply cost for the TF IBCs is moderate (~ 50 M\$); the development of large, steady-state homopolar generators could reduce this cost.

The TITAN-I design shows that liquid-metal-cooled high-power-density RFP reactors appear feasible and attractive. Feasibility issues for this concept have been addressed and resolved, given the inadequacy of experimental data in certain areas. As always, further research is needed to validate the technological requirements of TITAN-I design.

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11. TITAN-I DIVERTOR ENGINEERING

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11. TITAN-I DIVERTOR ENGINEERING

11.1. INTRODUCTION

This section describes the engineering design of the divertor of TITAN-I, including the thermal and mechanical design of the divertor components, materials selection, and fabrication issues. Toroidal-field divertors are selected as the impurity-control and particle-exhaust system of TITAN reactors. An account of the magnetics analysis for the divertor is contained in Section 4.4, and the edge-plasma and neutral-particle modeling, which had a strong bearing on the engineering of the divertor, are described in Sections 5.4 and 5.5, respectively.

The design of the impurity-control system poses some of the most severe problems of any component of a DT fusion reactor. For TITAN the divertor design probably represents (perhaps together with the design of the oscillating-field current-drive system, described in Section 7) the most critical engineering and physics issue for the reactor. Even low-power-density devices such as NET [1], operating at a neutron wall loading of about 1 MW/m^2 , encounter major problems in the design of in-vessel components. Although the differences in configuration between poloidal divertors in tokamaks and toroidal divertors in reversed-field-pinch (RFP) reactors make a direct comparison difficult, the particle and heat loadings on the divertor might be expected to scale with the neutron wall loading, implying that the divertor conditions for the TITAN divertor might be a factor of about 20 more severe than for NET, all other things being equal. Therefore, special attention was given to this aspect of the TITAN design. The final divertor design is the result of extensive iteration between the magnetic design, edge-plasma analysis, thermal and mechanical design, and divertor-plate fabrication issues.

The two main design issues for the divertor system are to achieve heat loadings on the divertor collector plate (or target) that do not exceed the maximum acceptable level, while simultaneously ensuring that the sputtering erosion rate does not lead to an early failure of the component. These two aims tend to conflict, because the high heat loadings which inevitably occur on the divertor target require the use of thin structures to minimize temperature differences and thermal stresses, while a thick structure is necessary to give a long life against erosion.

A summary of the results of the edge-plasma modeling is given in Table 11.1-I and are described in detail in Section 5.4. The plasma power balance is controlled by the

Table 11.1-I.

SUMMARY OF TITAN-I EDGE-PLASMA CONDITIONS

Number of divertors(a)	3
Scrape-off layer thickness	6 cm
Peak edge density	$1.7 \times 10^{20} \text{ m}^{-3}$
Peak edge ion temperature	380 eV
Peak edge electron temperature	220 eV
Plasma temperature at first wall	1.7 eV
Peak divertor density	$6 \times 10^{21} \text{ m}^{-3}$
Peak divertor plasma temperature	4.5 eV
Divertor recycling coefficient	0.995

(a) Toroidal divertor (poloidally symmetric) with "open" geometry.

injection of a trace amount of a high atomic number impurity (xenon) into the plasma, causing strong radiation from the core plasma, the scrape-off layer (SOL) plasma, and the divertor plasma. About 95% of the steady-state heating power (alpha particle and ohmic heating) can be radiated to the first wall and divertor plate, with about 70% being radiated from the core plasma (i.e., inside the separatrix). This intense radiation reduces the power deposited on the divertor target by the plasma to an acceptably low level. Preliminary experimental results suggest that beta-limited RFP plasmas can withstand a high fraction of power radiated without seriously affecting the operating point (Section 5.3); this behavior contrasts with that observed in tokamaks, in which a high radiation fraction appears to lead to a plasma disruption. The radiative cooling also reduces the electron temperature at the first wall and divertor target (also assisted by recycling) which, in turn, reduces the sputtering erosion problem.

As discussed in Section 4.4, the TITAN divertor uses an "open" configuration, in which the divertor target is located close to the null point, facing the plasma, rather than in a separate chamber. This positioning takes advantage of the increased separation

between the magnetic field lines (flux expansion) in this region, which tends to reduce the heat loading on the divertor plate because the plasma flowing to the target is "tied" to the field lines. The high plasma density in front of the divertor target ensures that the neutral particles emitted from the surface have a short mean free path; a negligible fraction of these neutral particles enter the core plasma (Section 5.5).

The TITAN-I divertor coils are based on the integrated-blanket-coil (IBC) concept [2], in which an electrical current flows in a liquid-lithium coil, thereby combining the functions of magnetic-field production, tritium breeding, and heat removal in a single system. This approach also reduces the need to shield the divertor coils significantly.

The magnetics design (Section 4.4) focussed on maximizing the achievable degree of flux expansion, in order to minimize the peak heat flux on the divertor target while minimizing the divertor coil currents, since the ohmic losses in the divertor IBC represent a large fraction of the recirculating power in the reactor. The final magnetics design includes three divertor modules which are located 120° apart in toroidal direction. An equatorial-plane cross section of the coils is shown in Figure 11.1-1. The magnetic-field lines are diverted onto the divertor plate using a nulling coil and two flanking coils, with the latter localizing the nulling effect. For the TITAN-I design, the divertor IBC assembly displaces a part of the toroidal-field (TF) IBC tube bank. Therefore, a pair of trim coils is also required to control the toroidal-field ripple; the trim coils are also IBCs.

11.2. MATERIALS

11.2.1. Plasma-Facing Material

In order to reduce the erosion of the divertor armor by the plasma, a high atomic-number (Z) material must be used for the surface of the divertor plate. This conclusion is based on the estimates of sputtering rates of various candidate materials (described in Section 5.5). The threshold energy for sputtering is sufficiently high for high- Z materials, that the erosion rate of the divertor target under the expected conditions for TITAN designs (Table 11.1-I) is acceptable.

Various high- Z materials were considered for the divertor target. Molybdenum has excellent physical properties as well as good resistance to sputtering damage, but suffers from serious activation when exposed to a fusion-neutron energy spectrum. Tantalum also has good thermal and mechanical properties and is relatively easy to fabricate into complex components. However, tantalum absorbs large quantities of hydrogen in the

Figure 11.1-1. Equatorial-plane view of divertor coils and magnetic-field lines for TITAN-I, showing TF and divertor IBCs.

temperature range of interest (~ 300 - 800°C), which causes an embrittlement problem [3, 4]. Tungsten has good thermal and mechanical properties which are retained at high temperatures, and also has a very low sputtering rate, which has led to the specification of a 2-mm-thick armor to ensure a one-year lifetime. One of the inherent problems with unalloyed tungsten is the lack of ductility (brittleness) at room temperature. Addition of rhenium results in a tungsten alloy that combines the high-temperature strength of pure tungsten with useful room-temperature properties in both the stress-relieved and recrystallized conditions [5].

Maykuth *et al.* [6] reviewed the workability and mechanical properties of tungsten alloys containing rhenium. The elevated-temperature properties of the recrystallized W-27Re and W-30Re alloys are shown in Figure 11.2-1. Generally, the maximum effect of rhenium in improving mechanical properties of refractory metals is obtained with the maximum amount of rhenium which can be retained in solid solution. However, rhenium solubility decreases with temperature, and tungsten alloys containing 27% or more rhenium are subject to precipitation of the hard and brittle σ phase at low temperatures [6]. On the other hand, the mechanical properties of tungsten are not significantly influenced for rhenium contents smaller than 3% [7]. For these reasons, the useful rhenium content range in tungsten-rhenium alloys is generally 3-27 at. %.

Another issue is the high ductile-to-brittle transition temperature (DBTT) for pure tungsten, below which tungsten is extremely brittle and cannot be worked. Pure tungsten has a DBTT of about 200 to 400°C depending on the impurity content, thermomechanical history, and thickness [7]. The addition of rhenium reduces the DBTT appreciably. The DBTT of W-26Re alloy is about 90°C [7] which is a great improvement over pure tungsten. Forming, cutting, milling, and drilling of W-26Re alloy must be done at temperatures well above 90°C DBTT. Therefore, if the application requires tungsten with ductility at room temperature and/or ductility after operating above recrystallization temperature, the W-26Re alloy is a logical choice. It should also be selected if the fabrication of the part is unusually difficult. Weldments made with W-26Re are ductile, unlike unalloyed tungsten weldments which always recrystallize the metal and hence are brittle [8].

The properties of the W-26Re alloy are described in Section 11.2.4 and summarized in Table 11.2-1. The primary drawback of W-Re alloys is the cost of rhenium which ranges from \$800 to \$1500 per pound [8]. A further issue is the activation of rhenium and its impact on the rad-waste disposal which is reported in Section 13.7.

The data base of neutron-irradiation effects on tungsten and its alloys is very sparse. Further research on the irradiation behavior of W-Re alloys is needed to establish lifetime-

Figure 11.2-1. Ultimate strength and elongation as functions of temperature for W-Re alloys in the recrystallized condition [6]

Table 11.2-I.

SELECTED PROPERTIES OF W-Re [7], V-15Cr-5Ti, Al_2O_3 , AND MgAl_2O_4

PROPERTY	W-26Re	W-3Re	V-15Cr-5Ti	Al_2O_3	MgAl_2O_4
Melting temperature ($^{\circ}\text{C}$)	3,180	3,180	1,890	2,050	2,135
Linear thermal-expansion coefficient ($10^{-6}/^{\circ}\text{C}$)					
500 $^{\circ}\text{C}$	7.06	4.48	10.3	7.8	8.
1,000 $^{\circ}\text{C}$	7.95	5.14	11.1	—	9.
Thermal conductivity ($\text{W}/\text{m}^{\circ}\text{C}$)					
600 $^{\circ}\text{C}$	—	—	27.5	9.1	8.1
1,000 $^{\circ}\text{C}$	66.72	99. ^(a)	29.	—	6.
2,000 $^{\circ}\text{C}$	62.82	136. ^(a)	—	—	—
Recrystallization temperature ($^{\circ}\text{C}$)					
Incipient after 1 h anneal	1,500	1,400	< 1,100	—	—
Complete after 1 h anneal	1,650	—	—	—	—
DBTT ($^{\circ}\text{C}$)	90	< 150 ^(b)	< 0	—	—
Electrical resistivity ($\mu\Omega\text{m}$)					
20 $^{\circ}\text{C}$	—	0.092	0.39	> 10^{20}	> 10^{20}
500 $^{\circ}\text{C}$	0.35	0.18 ^(a)	0.74	> 10^{16}	> 10^{16}
1,000 $^{\circ}\text{C}$	0.55	0.33 ^(a)	1. ^(c)	> 10^{12}	> 10^{12}
2,000 $^{\circ}\text{C}$	0.85	0.65 ^(a)	—	—	—
Tensile strength (MPa)					
600 $^{\circ}\text{C}$	—	—	300	235	250
1,000 $^{\circ}\text{C}$	450-680	240-320	250 ^(d)	—	—
1,500 $^{\circ}\text{C}$	200-340	110-190	—	—	—
2,000 $^{\circ}\text{C}$	30-110	40-100	—	—	—
Modulus of elasticity (GPa)					
20 $^{\circ}\text{C}$	430	410 ^(a)	127	—	—
1,000 $^{\circ}\text{C}$	380 ^(c)	360 ^(a)	115	—	—
Elongation (%) ^(e)					
650 $^{\circ}\text{C}$	30	—	10-20	—	—
1,000 $^{\circ}\text{C}$	39-45	20-30	—	—	—
1,500 $^{\circ}\text{C}$	60-80	50-70	—	—	—
2,000 $^{\circ}\text{C}$	110-170	70-100	—	—	—

(a) Pure tungsten values.

(b) DBTT for pure tungsten is in the range 100-400 $^{\circ}\text{C}$.

(c) Estimated values.

(d) Value at 900 $^{\circ}\text{C}$.

(e) Data from Reference [6].

limiting factors in a neutron environment. Lastly, the relatively large afterheat in the tungsten armor of the divertor plate should be considered in the safety design of the reactor (Section 13).

11.2.2. Divertor-Target Coolant

The natural choice for the coolant of high heat-flux components is water but, for the lithium-cooled FPC of TITAN-I, fundamental safety considerations prohibit the use of water anywhere within the containment building (Section 13). Gaseous coolants, such as helium, were considered, but removing high heat fluxes is possible only at a low coolant temperature and requires large pumping powers.

Lithium cooling of the divertor target is an attractive option because it enables the divertor cooling circuit to be integrated with that of the first wall, blanket, and shield. Generally, the MHD effects make liquid-metal cooling of high heat-flux components such as divertors and limiters difficult. The magnetic topology of the RFP, however, makes this option viable. Since the toroidal component of the magnetic field near the first wall is small for the RFP (~ 0.4 T for TITAN), only a relatively minor pressure drop is encountered by a poloidally flowing coolant; for the toroidal divertor used in TITAN-I this problem is further eased because of the reduction of the toroidal-field strength in the vicinity of the null point.

For these reasons, lithium was chosen as the divertor-target coolant for the TITAN-I design. The thermal-hydraulic calculations and the integration of the divertor and first-wall coolant circuits are described in Section 11.4.

11.2.3. Substrate Material

There is a strong incentive to design a divertor target using a single material to eliminate the problems of bonding dissimilar materials, differential thermal expansion, and additional stresses at the interface of the different materials. It is desirable, therefore, to use W-Re alloy for both the divertor substrate and coolant tubes since manufacturing W-Re tubes using powder metallurgical techniques is possible using current technology (as proposed for TITAN-II in Section 17). The high electrical conductivity of the W-Re tubes, however, would produce a large MHD pressure drop. For this reason, the same vanadium alloy (V-3Ti-1Si) is used for the divertor substrate as was used for the structural material of the first wall. Relevant physical properties of V-15Cr-5Ti, which are similar

to those of V-3Ti-1Si alloy, are listed in Table 11.2-I. The effects of neutron irradiation on the vanadium-base alloys are also discussed in Section 10.2. Detailed fabrication techniques for the divertor target and joining of the vanadium-alloy tubes to the tungsten-alloy armor are discussed in Section 11.3.

11.2.4. Insulator Material

If a good electrical contact exists between the coolant tubes and the armor, then the higher conductivity of the tungsten-rhenium armor will produce a larger MHD-induced pressure drop than would occur with bare vanadium tubes. Therefore, the coolant tubes of the TITAN-I divertor are electrically insulated from the armor by a thin layer of spinel ($\text{MgO-Al}_2\text{O}_3$), which has a high resistance to radiation damage and a relatively high thermal conductivity ($\sim 8 \text{ W/m K}$). This method of insulation is preferred over the use of an insulator inside the tube, which would lead to the undesirable exposure of the insulator to the high-velocity lithium coolant. Furthermore, the use of laminated tubes would pose fabrication problems for the small tubes employed in the TITAN-I divertor. Selected properties of spinel are also given in Table 11.2-I.

In addition to reducing the MHD pressure drop, the use of spinel as the insulating material introduces another highly desirable characteristic. Experiments on hydrogen permeation through nonmetallic solids have concluded that Al_2O_3 has about two to three orders of magnitude lower hydrogen permeability than the least permeable of the refractory metals [9]. Hydrogen permeability behavior of spinel is expected to be similar to that of pure alumina (Al_2O_3). The low permeation rate of hydrogen through spinel has an important impact on the tritium inventory in the divertor target and on the tritium extraction system because the divertor target is exposed to high particle fluxes.

11.2.5. Compatibility

A schematic cross section through the divertor target, showing the various materials, is given in Figure 11.2-2. The use of three different materials in the TITAN-I divertor plate requires that these materials be compatible with each other. They need to be chemically stable with respect to each other and have matching mechanical properties. The most significant properties are thermal conductivity and thermal-expansion coefficients. Adequate thermal conductivity reduces thermal stresses and similar expansion coefficients reduce mismatch stresses at interfaces. Table 11.2-I lists selected properties

Figure 11.2-2. Schematic cross section through divertor target of TITAN-I, showing the coolant tubes, the spinel insulator layer, and the W-Re divertor target.

of the three candidate materials for the TITAN-I divertor plates. The linear thermal-expansion coefficient of the armor (W-26Re), the substrate (V-15Cr-5Ti or V-3Ti-1Si), and the insulator (spinel) match closely.

Even though the thermal conductivity of spinel is greater than that of most insulator materials, it is still much less than that of the vanadium or tungsten alloys. However, the insulating film between the tungsten-rhenium plate and the vanadium tubes is only a few μm thick and, therefore, only a relatively small temperature drop will appear across the spinel despite its poor thermal conductivity.

The chemical compatibility of spinel with tungsten-base and vanadium-base alloys has been investigated. Tungsten-base alloys are often used in combination with ceramic furnace materials (e.g., Al_2O_3 , MgO , graphite, BeO , ThO_2 , and ZrO_2). The Plansee Corporation [7] has established maximum operating temperatures for tungsten in contact with these furnace materials. Typical maximum operating temperatures for graphite, Al_2O_3 , and MgO are, respectively, 1400, 1500, and 1900 °C. The maximum operating temperature of the tungsten-rhenium plates in contact with spinel will likely be between the two latter limits (i.e., 1500-1900 °C).

The compatibility of spinel and vanadium-base alloys has been studied in detail [10, 11]. The standard free energy of formation for spinel is about one half of that of vanadium oxides in the temperature range of 500 to 1500°C. Therefore, using spinel as an insulating film in contact with vanadium should not pose a compatibility problem.

The effects of neutron irradiation on the spinel insulating material are discussed in Section 10.2. Spinel swells to a few percent of original volume depending on the operating temperature and is shown to be most susceptible in the 150 to 300°C range, which is well below the anticipated operating temperature for the spinel insulating film in the TITAN-I divertor plate.

11.3. TARGET FABRICATION

The TITAN-I divertor plates are cooled with liquid lithium. The high electrical conductivity of tungsten requires electrical insulation of the target plate from the coolant tubes to reduce MHD pressure drops. Several fabrication procedures for the TITAN-I divertor target were investigated. In cooperation with the Plansee research and development group [12], the following four-step fabrication procedure was identified as feasible:

- Manufacture the W-Re plate using powder metallurgy;
- Mill the W-Re plate for coolant-tube grooves;
- Chemical-vapor deposit the electrical insulator;
- Braze the vanadium coolant tubes to the W-Re plate.

Manufacturing the W-Re plate using powder metallurgy (PM). Tungsten powder is usually produced by reduction of tungsten oxides using high temperature hydrogen (1000°C). The tungsten powder is then pressed hydraulically or isostatically into rods, plates, or other shapes. The pressed compact is then sintered in high purity hydrogen at 2500°C into a "pressed and sintered" ingot having a density of about 92%. Alloying elements are introduced into the powder before the high-temperature sintering takes place.

The Plansee manufacturing plant routinely produces tungsten-base-alloy sheets of up to 100 cm and up to 7 mm thick [7,8]. Thus fabrication of a $2 \times 150 \times 5,000$ mm continuous W-Re plate (TITAN-I divertor plate) can be considered a routine procedure.

Milling of the W-Re plate for coolant tube grooves. The next step in the divertor-plate fabrication is milling of coolant grooves along the plate. Tungsten-base alloys can be folded, bent, formed, spun, sheared, stamped, punched, riveted, ground, and machined (i.e., turned, milled, or drilled [8]).

Chemical-vapor deposition (CVD) of the electrical insulator. The CVD process is very versatile in depositing a large variety of elements and compounds at relatively low temperatures (400-800 °C). A unique feature of CVD is the control of stoichiometric composition and layer structures which is difficult or impossible to attain by other techniques. The CVD process involves chemical or thermal decomposition of gaseous species at a hot susceptor surface. Stringent quality requirements can be achieved by controlling temperature, pressure, and gas-flow rates of species and cover gases. Therefore, CVD has become a cornerstone for modern technologies such as solid-state electronics. Routinely applied CVD materials include [13]:

- Insulators (SiO_2 , Al_2O_3 , MgAl_2O_4 , TiO_2 , Nb_2O_5),
- Conductors (W, W-Re, W-Mo-Re, Ta-W, V),
- Superconductors (Nb_3Ge),
- Transparent conductors (VO_2 , V_2O_5 , SnO_2 , In_2O_3),
- Semiconductors (Group II - Group VI).

One of the greatest advantages of the CVD process is that gradual material changes of the deposited compound are possible. For the divertor plate, the CVD process can start with the deposition of W-Re onto the W-26Re plate. By gradually changing the gaseous composition, the deposition of W-Re can be changed into deposition of spinel.

Brazing of vanadium coolant tubes to the spinel-insulated W-Re plate. Joining of refractory alloys by welding is generally not preferred, because embrittlement of the weldment may occur. Although W-26Re does show retention of ductility of weldments, brazing is preferred because the vanadium tubes must be joined to the spinel insulating film. The Plansee Corporation has evaluated and developed a series of brazing materials for joining various refractory metals and alloys. As long as the braze material has a melting point below the recrystallization temperature of the tungsten- or the

vanadium-base alloy, embrittlement can be avoided. Recrystallization of W-26Re sets in at around 1500 °C while for the vanadium alloy, recrystallization can start between 800 to 1100 °C (Section 10.2). Table 11.3-I lists some of the commercially used brazing materials developed by Plansee [7].

Table 11.3-I.

BRAZING MATERIALS FOR REFRACTORY METALS [7]

Material	Brazing Temperature (°C)
Rhodium	1970
PtPd20Au5	1695-1645
Palladium	1550
Nickel	1430
AuPd25	1410-1380
AuPd15	1305-1260
CuNi45	1300
PdNi	1240
CuNi30	1230
AgPd33Mn3	1200-1180
NiMn31Pd21	1120
AgPd20Mn5	1120-1000
CuPd18	1090-1080
AgPd5	1010-970
AgCu20Pd15	900-850

11.3.1. Ceramic-Metal Adhesion

The main concern regarding the divertor plate performance is the integrity of the bond between the tungsten-rhenium armor plate and the vanadium tubes with a thin, ceramic insulating material sandwiched between. The adhesion at ceramic-metal interfaces has always been a concern for industries developing hard, wear-resistant coatings such as carbides and nitrides of tungsten, tantalum, and titanium on soft and ductile substrates. Ceramic-metal interfaces are used extensively by the electronics industry and are also heavily employed in rockets and jet-turbine components. A few examples of latest findings of ceramic-metal interface integrity follow.

Extensive research began in the late 1960s on CVD of insulator and composite structures. Various combinations of metal-ceramic-metal insulating "tri-layer sandwich" structures of W, W-Re, Nb, and Mo were fabricated by the CVD process. Aluminum oxide, hafnium oxide, and zirconium oxide were the primary insulating material. Tubular structures of these sandwich composites were cycled between 1300 °C and room temperature without any apparent damage to the oxide layer or either of the oxide-metal bonds [14]. Extended-length thermocouple wires made of W-Re were routinely coated with hafnium-oxide insulation and used as temperature probes measuring up to 2500 °C [14].

Recently the solid-state bonding of oxide ceramics to steels was studied to examine the bonding strength and the resistance to thermal cycling for alumina-steel joints [15]. A series of metallic interlayers of various thicknesses between the ceramic and the steel were examined. It was found that using a thin (1 mm) metallic interlayer of Nb-Mo produces a superb bond between the oxide and the steel with a strength of 450 to 500 MPa, as measured in a four-point bending test, which corresponds to the strength of the alumina. The Nb-Mo interlayer also proved superior to others in withstanding the effects of thermal expansion mismatches at the joints and in resisting thermal cycling between room temperature and 500 °C.

Research by the electronics industry into improvements of ceramic-metal bonds has recently lead to new discoveries. Adhesion enhancement of a film of nonreactive metal deposited on a ceramic or glass substrate can be produced by irradiating the interface with an ion beam [16]. This method produced thermally stable, adhesion-strength characteristics of chemical bonding for metal films on the surfaces of ceramics. The superior strength achieved was attributed to "interface mixing" of interface atoms. Although the mechanism responsible for the superior bonding strength is not fully understood it is postulated that ion-beam irradiation produces a preferred organization of the atom species that minimizes the interface energy.

Based on the above observations, an alternate technique for the fabrication of the TITAN-I divertor is possible using a gradual CVD mechanism. This CVD process starts with the deposition of W-Re onto the W-26Re plate. By gradually changing the gaseous composition, the deposition of W-Re can be changed into deposition of spinel. After the desired spinel thickness has been achieved, deposition of vanadium can be introduced gradually. This process eliminates sudden changes in material properties resulting in better adherence and improved resistance to film delamination. It is speculated that this gradual compound-deposition mechanism would result in both above-mentioned enhancement processes (i.e., a metallic interlayer and "interface mixing").

11.3.2. Discussion

The feasibility of using tungsten-rhenium for the divertor armor is no longer seen as an issue. High-ductility tungsten-rhenium alloys have been developed since the mid 1950s and show promising properties for fusion applications. Tungsten-rhenium alloys have appreciably lower DBTT when compared to pure tungsten. Because the TITAN-I divertor plates are cooled with liquid lithium, electrically insulated V-3Ti-1Si coolant tubes have to be used. Highly radiation-damage-stable spinel is proposed as the insulating material.

The fabrication process of a divertor plate made from two different materials brazed together with a thin electrical insulating film between them has been discussed. As a second technique, a unique manufacturing process using CVD is proposed to enhance bond strength of the tungsten-spinel-vanadium interfaces. An extensive research and development effort is needed to establish the manufacturing feasibility and performance characteristics of such a system.

11.4. TARGET DESIGN

The materials considerations described in the previous section led to the basic layout of the divertor target shown in the schematic cross section in Figure 11.2-2. The details of the shaping of the surface and the overall thermal analysis are described in this section; a more extensive structural and thermal analysis using finite-element techniques is reported in Section 11.5.

11.4.1. Target Shaping

Despite the intense radiation resulting from the impurities injected into the plasma, careful shaping of the divertor target is required to maintain the heat flux at acceptable levels at all points on the plate. Various factors combine to complicate the shaping problem.

The heat flux associated with the plasma flow on to the target depends nonlinearly on the location in the scrape-off layer and the inclination angle of the target with respect to the field lines. Heating by plasma radiation is taken from the edge-plasma transport analysis performed by the edge-transport code ODESSA (Section 5.4), and is divided into two sources: an upstream zone (the edge plasma, or scrape-off layer) and a downstream zone (the divertor plasma). The former component of radiation is analogous to the surface heating on the first wall and varies with the angle of the target with respect to the centerline of the core plasma. The downstream radiation varies with the local plasma properties and, as the radiating zone is spread over a finite volume, it is assumed that only 25% of this heating is deposited locally on the target; the remainder goes to the first wall.

The heat flux from the plasma and the downstream radiation is directly affected by the local spacing between the magnetic field lines. In order to calculate these heating rates, it is necessary to determine the flux-expansion factor, defined as the ratio of the value of the local magnetic field (toroidal and radial) to the field at a point between two divertors on the same field line (Section 4.4). This factor changes rapidly in the vicinity of the divertor null point.

A further complication arises from the liquid-metal cooling of the target. For a given pressure drop along the coolant path, MHD effects will cause the coolant velocity to adjust according to the local value of the magnetic field. Because the heat-transfer coefficient is a function of the coolant velocity, the allowable heat flux varies with position along the target.

These considerations require an iterative procedure for the divertor design. A guess for the shaping for the target surface is made, and the heat-flux distribution for this shape is calculated to allow a thermal-hydraulics analysis for the target cooling to be performed. If any of the constraints imposed on the thermal design (especially the maximum temperature limits for the various materials) is exceeded, then the shaping is adjusted until an acceptable solution is obtained.

The above iterative procedure has been simplified by writing a computer code to perform the thermal analysis. This code produces an approximation to the profile of the

target in 3-D by specifying its shape in both the inboard and the outboard intersections with the equatorial plane (the two sets of field lines in Figure 11.1-1). It is assumed that the full shape of the target is completed by a smooth curve along the poloidal direction between the two sets of points thus created, and that the values of quantities such as magnetic field strength and heat flux, vary smoothly between their values at these two inboard and outboard locations.

The target-shape profile is described by the coordinates of a number of points where field lines intersect the surface of the target. The input for the code takes the form of this set of coordinates on the target, together with values of heat flux (taken directly from the ODESSA code), magnetic field strength, inclination angle of the target, and flux-expansion factor, at each point.

11.4.2. Target Thermal-Hydraulic Design

The surface heat flux on the target, q''_{sur} , is given by

$$q''_{sur} = q''_1 + q''_2 + q''_3, \quad (11.4-1)$$

where

$$q''_1 = \frac{\sqrt{B_\phi^2 + B_r^2}}{B_\theta} q''_{\parallel} \frac{\sin \alpha}{f_{exp}}, \quad (11.4-2)$$

$$q''_2 = \frac{\sqrt{B_\phi^2 + B_r^2}}{B_\theta} q''_{d,rad} f_{d,rad} \frac{\sin \alpha}{f_{exp}}, \quad (11.4-3)$$

$$q''_3 = \frac{f_{RAD} (P_\alpha + P_\Omega)}{A_{FW}} \sin \beta \frac{r}{r_{FW}}, \quad (11.4-4)$$

are the components of the surface heat flux from the plasma particles, radiation from the divertor plasma, and radiation from the edge plasma, respectively. In these expressions, $q''_{\parallel}$ is the parallel plasma heat flux along the field line, $q''_{d,rad}$ is the radiation from the divertor plasma converted to a parallel plasma heat flux, and $f_{d,rad}$ is the fraction of the divertor plasma radiation which is assumed to be locally deposited on the target. The magnitude of the toroidal, poloidal, and radial magnetic fields are, B_ϕ , B_θ , and B_r , respectively, and $\sqrt{B_\phi^2 + B_r^2}/B_\theta$ represents the pitch of the field line. The inclination angle of the target with respect to the field line is α , f_{exp} is the flux-expansion factor, and f_{RAD} is the radiation fraction from the core and edge plasmas (the fraction of the alpha, P_α , and ohmic, P_Ω , heating powers which is radiated). The area of the first wall

is A_{FW} , β is the angle of the target with respect to the plasma centerline, r is the local value of minor radius, and r_{FW} is the minor radius of the first wall.

The total coolant pressure drop, ΔP , was calculated from the equation

$$\Delta P = \sigma_f v B_{\perp}^2 L \frac{\phi}{1 + \phi} + \sigma_f v B_{\parallel}^2 d_i \sqrt{\phi} + 0.1 \sigma_f v B_{\parallel}^2 d_i \sqrt{\phi} + f L \frac{\rho v^2}{2 d_i}, \quad (11.4-5)$$

with ϕ given by

$$\phi = \frac{\sigma_w t_w}{\sigma_f r_i}, \quad (11.4-6)$$

where σ_f and σ_w are the electrical conductivities of the coolant and tube wall, respectively, L is the length of the coolant tube, d_i and r_i are, respectively, the inner diameter and inner radius of the coolant tube, and t_w is the tube-wall thickness. The parallel and perpendicular magnetic field strengths are, respectively, $B_{\parallel}$ ($= B_{\theta}$) and $B_{\perp}$ ($= \sqrt{B_{\phi}^2 + B_r^2}$). The parameter f is the friction factor, ρ is the coolant density, and v is its velocity. In the pressure drop equation, the first term is the MHD pressure drop resulting from the coolant flow along the length of the tube in the poloidal direction. The second and third terms represent, respectively, the pressure drop caused by the bends and by the gradient of the poloidal field. For these calculations, the magnetic field values are averaged between the inboard and outboard locations. The final term in Equation 11.4-5 gives the usual frictional pressure drop. A detailed justification for the use of these equations is given in Section 10.4.

Since the divertor coolant tubes are all fed from the same circuit, the total pressure drop, ΔP , is specified. It is desirable that this pressure drop be close to that of the first-wall coolant circuit so that both systems can be fed from the same coolant circuit. A constraint on the maximum velocity of coolant in the divertor tubes was also imposed. Given the total pressure drop, Equation 11.4-5 was solved to find the coolant velocity in each tube. For certain tubes, the resultant velocity exceeded the imposed maximum-velocity constraint. For these tubes, the pressure drop should be lowered; the pressure drop between inlet and outlet of these tubes was calculated from Equation 11.4-5 using the maximum-velocity limit. It was assumed that flow orificing would provide the difference between the pressure drop on the circuit and the pressure drop between inlet and outlet of these selected tubes.

A higher surface heat flux can be accommodated by using turbulent coolant flow which is accompanied by a higher Nusselt number compared with that of a laminar flow. The low value of the perpendicular (toroidal and radial) magnetic field in the RFP allows

the possibility of the liquid-metal coolant flow entering the turbulent regime. The flow is turbulent if

$$Re > 60 H_{\parallel} . \quad (11.4-7)$$

Otherwise, the flow is laminar (Section 10.4). In Equation 11.4-7, Re is the Reynolds number ($Re = \rho v d_i / \eta$, and η is the coolant viscosity), and $H_{\parallel}$ is the parallel Hartmann number ($H_{\parallel} = d_i B_{\parallel} \sqrt{\sigma_f / \eta}$).

The Nusselt number, Nu , is given by

$$Nu = \begin{cases} 6 & \text{laminar} \\ 6.5 + \frac{0.005 Pe}{1 + 1890(H_{\parallel}/Re)^{1.7}} & \text{turbulent} \end{cases} , \quad (11.4-8)$$

where Pe is the Peclet number of the coolant ($Pe = Re Pr$, and Pr is the Prandtl number). In determining the heat transfer coefficient, h ($= k_f Nu / d_i$, and k_f is the thermal conductivity of the coolant), the value of the Nusselt number from Equation 11.4-8 is halved to account for the effects of nonuniform heating around the circumference of the tube.

The coolant outlet temperature, T_{ex} , is computed from the energy balance equation

$$T_{ex} = T_{in} + \frac{d_o q''_{sur} + \frac{\pi}{4} (d_o^2 - d_i^2) q'''_w + d_o \left[t_a + d_o \left(\frac{1}{2} - \frac{\pi}{8} \right) \right] q'''_a + \frac{\pi}{4} d_i^2 q'''_f}{\frac{\pi}{4} d_i^2 v \rho C_p} L , \quad (11.4-9)$$

where d_o is the tube outer diameter, C_p is the specific heat capacity of the coolant, and q''_w , q'''_a , and q'''_f are the volumetric heating rates in the tube wall, armor, and coolant, respectively. The surface heat flux in Equation 11.4-9 is averaged between the inboard and outboard locations.

A 1-D thermal analysis is performed at the location of the coolant tube outlet to estimate the peak temperatures in the target. The temperature drops through the boundary layer, ΔT_f , the tube wall, ΔT_w , and the armor, ΔT_a , are given by

$$\Delta T_f = \frac{q''_e}{h} , \quad (11.4-10)$$

$$\Delta T_w = \frac{q'' d_o}{2k_w} \ln \left(\frac{d_o}{d_i} \right) + \frac{q'''_w d_o^2}{8k_w} \ln \left(\frac{d_o}{d_i} \right) - \frac{q'''_w}{16k_w} (d_o^2 - d_i^2) , \quad (11.4-11)$$

$$\Delta T_a = \left(q''_{sur} + \frac{t_a}{2} q'''_a \right) \frac{t_a}{k_a} , \quad (11.4-12)$$

where k_w and k_a are the thermal conductivities of the tube wall and the armor, respectively, and t_a is the minimum thickness of the armor. The heat flux term in the expression for the wall temperature drop, Equation 11.4-11, includes an allowance for the volumetric nuclear heating in the armor, which also must be conducted through the tube wall.

The heat fluxes used in Equations 11.4-10 to 11.4-12 refer to the coolant outlet location. The heat flux at this location is lower than the inboard/outboard average, because the coolant outlet header is 45° above the outboard midplane of the machine and the toroidal geometry leads to a reduction in heat flux on the divertor target with increasing major radius.

The peak temperatures in the vanadium-alloy tube wall, $T_{w,max}$, and in the tungsten-alloy armor, $T_{a,max}$, are determined from

$$T_{w,max} = T_{ex} + \Delta T_f + \Delta T_w \quad (11.4-13)$$

$$T_{a,max} = T_{w,max} + \Delta T_a \quad (11.4-14)$$

The values for the materials properties were given in Table 11.2-I in Section 11.2.5. Design parameters for the thermal analysis are given here in Table 11.4-I.

11.4.3. Design Parameters

For the final design of the target, the following constraints were imposed. A 0.5-mm-thick coolant-tube wall was chosen rather than the 1-mm thickness used for the first-wall tubes because of the higher heat fluxes in the divertor; the reduced thickness is possible because the tube is protected from potential sputtering damage by the tungsten-alloy armor. The diameter of the tubes is reduced by the same factor to ensure that the pressure stress does not increase. The coolant inlet temperature of 320°C is the same as for the first wall, allowing both coolant loops to be fed from the same circuit. Allowance is made in the thermal calculations for the temperature increase associated with the deposition of the pumping power into the coolant.

The maximum allowable coolant velocity was set at 25 m/s for this analysis, based on considerations of physical erosion (Section 10.2). This velocity is only slightly greater than that of the first-wall coolant which is ~ 21 m/s. While, it is desirable that the total pressure drop in the divertor coolant to be about the same as that of the first-wall coolant circuit (10 MPa), this design convenience did not appear possible, and a total pressure drop of 12 MPa was used for the divertor-coolant circuit. Since substantial

Table 11.4-I.
DESIGN PARAMETERS FOR THERMAL ANALYSIS

Thickness of W-Re armor, t_a	2.0 mm
V-alloy tube-wall thickness, t_w	0.5 mm
V-alloy tube inner diameter, d_i	5.0 mm
Nuclear heating rate in armor, q_a'''	100 MW/m ³
Nuclear heating rate in tube wall, q_w'''	100 MW/m ³
Nuclear heating rate in coolant, q_f'''	100 MW/m ³
Coolant inlet temperature, T_{in}	320 °C
Maximum allowable coolant velocity, v_{max}	25 m/s
Total coolant pressure drop, ΔP	12 MPa
Maximum allowable V-alloy temperature, $T_{w,max}$	750 °C

simplification results from cooling two components (first wall and divertor) from the same cooling circuit, it is assumed that a 12-MPa pump would be used for both components, and the excess pressure for the first wall circuit would be removed with an orifice.

The final geometry of the divertor, representing the converged solution to the divertor design problem described above, is shown in Figure 11.4-1. This figure contains views of both the inboard and outboard sections, illustrating the more compressed geometry on the inboard side, which arises because of toroidal effects and the inverse major radius dependence of the toroidal field. The closeness of the null point to the divertor plate surface (~ 1 cm) is clear from the figure, but neutral-atom transport calculations indicate that a negligibly small fraction of the particles released from the target re-enters the core plasma (Section 5).

Figure 11.1-1 shows a large gap between banks of TF IBCs in the divertor region. In Figure 11.4-1 much of this space has been filled with identical, but non-current-carrying, poloidal tubes of liquid lithium. These additional tubes improve the blanket coverage and the tritium breeding ratio (TBR), although 3-D neutronics calculations indicate that this

Figure 11.4-1. Outboard (A) and inboard (B) equatorial-plane views of the divertor region for TITAN-I.

is not necessary to achieve a TBR in excess of unity, nor to provide adequate shielding for the ohmic-heating (OH) coils (Sections 10.3 and 11.7).

Also shown on the outboard view of Figure 11.4-1 is the pumping aperture which leads to the vacuum tank surrounding the torus. This aperture is present for only the outboard 90° in poloidal angle; elsewhere shielding material is provided to protect the OH coils. The positioning of the divertor coils within the envelope defined by the blanket ensures adequate shielding of the inner OH coils.

Figure 11.4-2 shows the distribution of the various components of the surface heat flux along the divertor target for the inboard and outboard locations. The heat flux is plotted for each coolant tube; tube number 0 is located on the apex of the target, which is directly in line between the nulling coil and the plasma centerline.

The heat flux on the inboard target is significantly higher than that on the outboard, because of the toroidal effects noted earlier. The peak heat fluxes are ~ 9.5 and $\sim 6 \text{ MW/m}^2$ on the inboard and outboard targets, respectively. However, the maximum structural temperatures occur at the coolant outlet location (close to the outboard position), because the coolant temperature rise from inlet to outlet is the most significant factor. The maximum value of the inboard/outboard-averaged heat flux is about 7.5 MW/m^2 .

No plasma is incident on the target near its apex, since this region lies between the two points where the separatrix intersects the target. This region is heated only by radiation from the core plasma, and consequently there is a reduction in the surface heat flux at this point.

Corresponding to the increased heat flux on the inboard target, there is a reduction in the size of the heated region on the inboard side. Consequently, many of the coolant tubes are heated only along a portion of their length, since the tubes are of constant cross section. It is noteworthy that about 30% of the total heating rate is associated with the radiation from the core, edge, and divertor plasmas, illustrating the importance of including these components of the heat flux.

Figures 11.4-3 and 11.4-4 show plots of the flux-expansion factor, the inclination angle of the target with respect to the field lines, and the perpendicular (toroidal and radial) magnetic field strength. The structural temperature at certain points through the target is also shown in Figure 11.4-4 as a function of position along the target at the coolant outlet location. These figures show how the inclination angle was adjusted to maintain an acceptable heat flux along the target, given the varying values of the flux expansion factor and the magnetic field. Of particular importance is the strong variation of the magnetic

Figure 11.4-2. Heat flux distribution on outboard (A) and inboard (B) sections of divertor target. Coolant tubes are numbered from the apex or symmetry point of the target between the nulling coil and the core plasma.

Figure 11.4-3. Flux-expansion factor (A) and perpendicular magnetic-field strength (B) as functions of position on inboard and outboard sections of divertor target. Coolant tubes are numbered from the apex or symmetry point of the target between the nulling coil and the core plasma.

Figure 11.4-4. Target inclination angle (A) and coolant and structural temperatures at the coolant outlet (B) as functions of position on inboard and outboard sections of divertor target. Coolant tubes are numbered from the apex or symmetry point of the target between the nulling coil and the core plasma.

field; its low magnitude near the point where the separatrix intersects the target (and where the plasma heat flux is near its peak) allows high coolant velocities to be attained without excessive MHD pressure drops.

One of the most important constraints imposed on the target design is the 750 °C-maximum temperature of the vanadium-alloy tubes. Figure 11.4-4 shows that this condition is satisfied for all points on the target except for two tubes. At this location the curve for the film temperature drop shows a sharp spike, which is caused by a sudden transition from the turbulent to the laminar regime, as is given by the criterion in Equation 11.4-7. It is likely that a gradual transition would be achieved in practice, implying that the temperature estimate is conservative. In any case, the temperature exceeds the limit by only $\sim 10^\circ\text{C}$, which is not considered significant. The maximum temperature of the tungsten-rhenium armor is about 930 °C, at which level the alloy retains its high strength, and the thermal stresses are within allowable levels (Section 11.5).

Flow orificing is used extensively to tailor the coolant velocity distribution. In low-field regions, the large pressure head of 12 MPa would otherwise cause the velocity to exceed the 25 m/s limit. Near the outside of the plate, orificing allows the coolant outlet temperature to be adjusted in order to maintain an approximately constant level across the plate. These features are illustrated in Figures 11.4-5.

A more extensive structural and thermal analysis of the divertor target using finite-element techniques is reported in the next section.

11.5. THERMAL AND STRUCTURAL ANALYSIS

In order to achieve the desired one year lifetime, the TITAN-I divertor must satisfy three major criteria:

1. Low erosion, which is accomplished by using tungsten as the plasma-facing material and ensuring a low-enough plasma temperature at the target;
2. Acceptable stresses, as dictated by the ASME Boiler and Pressure-Vessel Code;
3. Acceptable temperature, and of particular importance, the maximum temperature limit of 750 °C for the vanadium tubes.

Figure 11.4-5. Components of the coolant pressure drop (A) and distribution of coolant velocities (B) for coolant tubes in the divertor target. Coolant tubes are numbered from the apex or symmetry point of the target between the nulling coil and the core plasma.

Figure 11.5-1. Finite-element model for thermostructural analysis of the divertor plate.

Estimates of the erosion rate for TITAN-I divertor target were discussed in Section 5.5, and an approximate thermal analysis was described in Section 11.4. A detailed finite-element analysis of the steady-state temperatures and stresses in the divertor plate is reported in this section.

The steady-state temperatures and stresses in the divertor were calculated using ANSYS [17], a widely used finite-element code. Because of the double curvature of the tungsten plate, a complete finite-element model is difficult and costly to construct, so an approximate 2-D model was used to allow a detailed study of the interface between the tungsten and the vanadium. The critical location for the divertor would be at the coolant outlet, near the outboard side, where the maximum temperatures are expected to occur. The peak heat flux at this location is 6.3 MW/m^2 . This portion of the divertor is modeled by assuming poloidal axisymmetry and using symmetry to model the structural behavior between neighboring tubes. The model, shown in Figure 11.5-1, consists of 135 quadrilateral elements. The material properties used in the analysis are given in Table 11.5-I.

Table 11.5-I.

**MATERIAL PROPERTIES USED IN THERMOSTRUCTURAL
ANALYSIS OF THE TITAN-I DIVERTOR**

	Vanadium	Tungsten
Young's modulus, E (MPa)	114	385
Poisson's ratio, ν	0.36	0.3
Thermal-expansion coefficient, α ($10^{-6}/K$)	10	8
Thermal conductivity, k (W/m K)	29	67
Allowable stress, S_{mt} (MPa)	108 (650°C)	200 (650°C)

11.5.1. Thermal Analysis

The heat flux on the divertor armor was assumed to be constant at 6.3 MW/m^2 . Heat transfer into the coolant was modeled assuming a constant heat-transfer coefficient of $81 \text{ kW/m}^2 \text{ K}$ and all the other surfaces were assumed to be adiabatic. The coolant bulk temperature at this point is $\sim 540^\circ \text{C}$.

The temperature contours for this model are illustrated in Figure 11.5-2, which shows that the temperature distribution in the tungsten armor is approximately one dimensional and little conduction to the rear of the tube is observed. The peak temperatures are 720 and 922°C in the vanadium tubes and the tungsten armor, respectively, in good agreement with the results for an equivalent location in the approximate model (Figure 11.4-4). Each of these values is below the allowable limit.

11.5.2. Pressure Stresses

The equivalent primary stresses, induced by the 12-MPA coolant pressure are shown in Figure 11.5-3. Note that this calculation is conservative because the pressure will be significantly lower than 12 MPa at the coolant outlet location. The stresses in the vanadium are similar to those in a tube without a plate attached and the stresses in

Figure 11.5-2. Temperature contours in TITAN-I divertor target for surface heat flux of 6.3 MW/m^2 .

Figure 11.5-3. Contours of equivalent pressure in TITAN-I divertor target for coolant pressure of 12 MPa.

the tungsten are much lower. The peak stress in the vanadium is 67 MPa, which is well within the allowable value of S_{mt} of about 80 MPa at 700°C.

11.5.3. Thermal Stresses

The equivalent thermal stresses in the divertor plate are induced by two key mechanisms: the temperature gradient and the differential expansion of the two materials. The temperature gradient causes stresses in the direction perpendicular to both the coolant flow and the heat flux. These stresses are similar to those caused by a uniform heat flux on a flat plate. The different thermal-expansion coefficients of the vanadium and tungsten alloys cause stresses in the bulk of the plate, as well as stress concentrations at the points where the interface intersects the free surface. Because of the need to satisfy both the equilibrium and compatibility equations near the traction-free intersection of the interface and component surface, these stress concentrations can be quite high. In fact, a purely elastic analysis would predict a stress singularity at this point, much like a typical stress field near the tip of a sharp crack in a fracture-mechanics analysis. If the singularity is assumed to be of the form $r^{-\delta}$, where r is the distance from the singular point, then δ , the order of the singularity, is usually 0.5 near a crack tip. The order of the singularity, however, is expected to be much smaller at about 0.1 for crack-free interface problems [18]. Because of the weakness of this singularity and the ability of plastic deformation to accommodate the deformations required by the compatibility equations without failure, the existence of a singularity in an elastic analysis cannot be used for design purposes until further testing is done. Hence, the stress concentrations determined from the finite-element analysis will be used to assess the viability of the TITAN-I divertor.

The equivalent thermal stresses in the divertor are shown in Figure 11.5-4. Except for the stress concentrations at the edge of the interface, the peak stresses are 400 MPa in the tungsten and 160 MPa in the vanadium. Each of these is within the allowable thermal stresses in the two materials. Near the edge of the interface, the peak reaches nearly 480 MPa in the tungsten, which is still within the allowable value of 600 MPa.

11.5.4. Global Deformations

One difficulty associated with the divertor target design is that accurate shaping of the plasma-facing surface is required to minimize the peak heat fluxes, as is discussed in Section 11.4. Because the divertor can easily undergo deformations and the null point

Figure 11.5-4. Contours of equivalent thermal stress in TITAN-I divertor target.

Figure 11.5-5. Initial and deformed shapes of the cross section of the TITAN-I divertor target, as compared with the desired deformed shape (dashed line).

is very close to the divertor target (about 1 cm), the initial shape of the tungsten plate must account for α deflections induced during start-up, allowing an optimal shape during operation. An axisymmetric, finite-element model of the divertor cross section, ignoring the coolant tubes, was created to model the divertor deflections from the initial shape; the results are shown in Figure 11.5-5. The dashed line represents the desired deformed shape and the solid lines represent both the initial and final shapes of a particular design. The deformed shape is relatively close to the desired shape, but some alterations of the initial shape could achieve a closer tolerance, if it were required.

11.6. DIVERTOR COIL ENGINEERING

The TITAN-I divertor coils are based on the *integrated-blanket coil (IBC)* concept [2], in which an electrical current flows in a liquid-lithium coil, thereby combining the functions of magnetic field production, tritium breeding, and heat removal in a single system. This approach also eliminates the requirement for a shield in front of the divertor coils. Although the higher resistivity of lithium compared with that of copper causes higher joule heating, this energy is deposited in the primary coolant circuit and is recovered with the thermal efficiency of the energy-conversion system.

The magnetics design of divertor IBCs is presented in Section 4.4. Electrical engineering aspects of the TITAN-I divertor IBC are similar to that of the TF IBCs, therefore both are reported in Section 10.5. Forces on the divertor IBCs are of four types: (1) outward radial forces on each coil caused by the interaction of the coil current with the toroidal field, (2) centering forces resulting from the radial variation of the toroidal field, (3) overturning moments generated by the interaction between the vertical field and the IBC current, and (4) out-of-plane forces resulting from the spatial variation of the magnetic field especially in the divertor region. These electromagnetic forces also vary in time during the cycles of the oscillating-field current-drive (OFCD) system. The magnitude of these forces on divertor IBCs depends on the the reactor magnetic field and, therefore, they have been discussed in Section 10.5 and reproduced in Figure 11.6-1. This section focuses on the support structure needed to carry these loads.

The forces on the divertor coils are delineated in Section 10.5. Maximum loadings from the flank, trim, and nulling coils are presented in Figure 11.6-1 and show the structure design requirements. First, the five elements each have a simple solenoidal radial force which causes respective normal hoop tension, as is indicated. The resulting tensile stress in the coil material has significant effect on the remaining usable stress for the

Figure 11.6-1. Typical forces on TITAN-I divertor coils. Note that forces shown on the coils are reversed forces and the lateral and overturning-moment forces are combined. Design of the beam span is determined by the maximum combination of the radial and lateral-resolved forces.

bending loads as is described below. Second, the entire coil set has a tendency to move away from the center of the machine. These forces vary around each of the coils in the poloidal direction as their centers cross the spatially varying magnetic field and can be described as a single bursting force on the coil but of varying magnitude around it, as shown in the radial forces of Figure 11.6-1. Third, an overturning moment tends to twist the coil out of its plane. This moment is at a maximum at the top and bottom of the coils. Last, a lateral (toroidal) direction force on the coils tends to move them sideways. The null coil, which is on an axis of symmetry, is not subject to this force. It is important to note that these forces are time-varying during the cycles of the OFCD system.

The loads on the five divertor coils in each divertor module are too great to be cantilevered from the header and they should be supported. The support pitch is determined by the strength of the coil tubes and the loads imposed on them. The coil supports are embedded in the shield which is a 10-cm-thick vanadium-alloy shell. These supports transfer forces into the shell by means of ceramic compression pads which electrically insulate the assembly.

Figure 11.6-2. The restraint structure for the TITAN-I divertor IBCs.

The information on long-term or fatigue strength of irradiated vanadium alloys at high temperatures is inadequate at present. For the TITAN-I design, a value of 69 MPa (10 ksi) is assumed for a working stress. This stress, which is 10% of the room-temperature ultimate and two-thirds of the creep-rupture stress at operating temperature for one full-power year (FPY) of operation, allows 10^9 stress cycles. Figure 11.6-2 shows the adopted configuration for the support system. To support the hoop stress, an insulated tie is used to complete the coil near the header. The toroidal-direction support beams are not equally spaced. The loads at top and bottom are most intense and require a somewhat closer pitch.

Details of the coupling beam are shown in Figures 11.6-3 and 11.6-4. The beam is electrically connected only to the trim coils, having insulators at the null and flank coils and at the bearing pads which must transmit the loads on to the shield. Cooling of the coupling beam is accomplished by baffling some of the trim coil flow through the beam tube. Figure 11.6-4 shows additional baffling at the insulated connections to the null and flank coils to ensure cooling of the attachment point. Realization of this concept will undoubtedly require further development and experimental verification.

11.7. NEUTRONICS

Nuclear heating and irradiation damage to the divertor IBCs are not different from those of the TF IBCs. The remaining issue is the radiation damage to the OH coils in the region behind the divertor, where the limited available space may lead to a more stringent requirement for shielding, particularly in the inboard region.

A 50-cm-thick reference shield design was adopted for the TITAN-I divertor region. The remaining 25-cm space between the first wall and this shield would then be available for the divertor IBCs and the collector plates; any shielding function provided by the material in this region was ignored for the purposes of this calculation. The composition of this divertor shield is 90% vanadium alloy and 10% lithium (30% enrichment in ^6Li). In this case, the fast-neutron fluence at the OH coil does not exceed $9.6 \times 10^{26} \text{ n/m}^2$, for 30 FPY of operation. This fluence is about a factor of 3 lower than the limit specified in Section 10.2 for the spinel insulator of the TITAN-I OH coils. When the divertor coils and the collector plates are included in the neutronics calculation, the fast-neutron fluence at the OH coil should be much lower than the value given above.

The lifetime of the shield was estimated assuming a limit of 200 dpa as the maximum damage allowed for the vanadium structural alloy. Using this criterion, the first 0.3 m of the shield nearest to the plasma must be replaced after every full-power year of operation, while the next 0.15 m will have a lifetime of 5 FPY. The nuclear heating rate at the OH coil is less than 2.8 MW/m^3 , which is similar to the rate in regions remote from the divertor, behind the blanket and hot shield.

After this reference design was completed, it became clear that it would be possible to incorporate some non-IBC blanket tubes in the space allocated for the divertor coils and collector plates, as shown in Figure 11.4-1. This design improves both the tritium breeding ratio and the energy recovered from the blanket. The non-IBC region is 0.28 m thick (as is the rest of the blanket and hot-shield design), although a larger void fraction exists, resulting in the composition of 10.8% vanadium, 43.2% lithium, and 46% void. The final divertor-shield design consists of two zones, located behind the non-IBC blanket tubes behind the divertor target region. The first zone is composed of 30% vanadium and 70% lithium, and a second zone consists of 90% vanadium and 10% lithium. Optimum thicknesses for these two shield zones were found to be 0.3 and 0.15 m, respectively, in order to limit the maximum fast-neutron fluence at the OH coil to about $9 \times 10^{26} \text{ n/m}^2$ after 30 FPY of operation. The lifetime for the non-IBC blankets, divertor coils, collector plates, and first shield zone is estimated to be 1 FPY, while the last 0.15 m of the shielding component, similar to that in the other design option, will last for 5 FPY.

Table 11.8-I.
ASSUMPTIONS FOR He AND DT REMOVAL DURING BURN

Throughput of DT	6.7×10^{21}	s^{-1}
Throughput of He	8.2×10^{20}	s^{-1}
Pressure in vacuum tank	20	mtorr
Temperature of neutral gas	700	K

11.8. VACUUM SYSTEMS

The vacuum system for TITAN-I is based upon a large vacuum tank surrounding the entire torus, OH coils, and the equilibrium-field (EF) coils. This approach is attractive because of the compact FPC of TITAN-I, which allows a relatively small tank (~ 16 m in diameter and 10 m high) to be used. The tank is connected to the plasma chamber via a pumping duct on the outboard side of the torus at each of the three divertor locations, as shown in Figure 11.4-1. The high vacuum pumps are situated outside the vacuum tank to allow easy access for maintenance. Pumping is performed through 12 ducts (one between each pair of EF-coil supports) which penetrate the tank. A general elevation view of the design is given in Figure 11.8-1.

11.8.1. Design Requirements

The requirements for the vacuum system design were assessed, considering both the removal of DT and He ash during the burn and initial pump-down of the plasma chamber. The configuration of the vacuum tank, pumping ducts, and vacuum pumps is shown in Figure 11.8-2. The main assumptions made in the calculations relating to the burn conditions are given in Table 11.8-I.

The particle throughputs are based on the results of plasma simulations described in Section 5.5. The vacuum-tank pressure is chosen to be compatible with the neutral-transport calculations for the divertor region. The pressure is estimated to be about

Figure 11.8-1. Elevation view of the TITAN-I FPC design, illustrating the geometry of vacuum tank.

Figure 11.8-2. Configuration of vacuum tank, pumping duct, and vacuum pumps for the TITAN I vacuum system.

20 mtorr; this pressure is sufficiently high that pumping speed requirements are not excessive. At these pressures, the gas flow is in the transition range between the regimes of molecular and viscous flow. Using standard prescriptions for vacuum-pumping calculations, incorporating Santeler's correction for molecular flow in short ducts [19], the net conductance of each duct is estimated to be $215 \text{ m}^3/\text{s}$ for DT and $224 \text{ m}^3/\text{s}$ for He. The resulting vacuum pumping speed required at each duct location is $21.8 \text{ m}^3/\text{s}$, or about $260 \text{ m}^3/\text{s}$ for the entire reactor.

The level of purity of the plasma chamber will be determined by the ultimate pressure than can be achieved with the vacuum-pumping equipment. An assessment of this issue was made by assuming outgassing to be the only source of impurities (*i.e.*, the vacuum tank is leak tight). An outgassing surface area equal to five times the inner plasma chamber surface and vacuum tank surfaces was used to account for the EF-coil case, blanket and shield, and various other exposed surfaces present inside the tank.

Outgassing rates of 10^{-7} to 10^{-8} torr liter/s cm^2 were considered, representing the range of values expected for baked stainless-steel surfaces. The minimum pumping speed required to achieve a base pressure of 10^{-8} torr in the plasma chamber ranges from 0.7 to $12 \text{ m}^3/\text{s}$ at each duct location. As this is less than that required for DT and He removal during the burn, pump-down to a reasonable base pressure appears not to be a serious concern for the TITAN-I design.

11.8.2. Vacuum Pumps

It is proposed to use magnetic-suspension-bearing turbo-molecular pumps for the high-vacuum pumps in TITAN-I. These pumps do not require oil lubricants, which can become contaminated with tritium or, if fluorocarbon oils are used, can cause fluoride corrosion of the pump. However, the largest model currently available has a pumping speed of only $2 \text{ m}^3/\text{s}$; in general, turbo-molecular pumps are limited to lower speeds than cryo-pumps. Manufacturers consider that higher speed pumps can be easily developed, and it is estimated that a turbo-molecular pump with a speed of $20 \text{ m}^3/\text{s}$ will only cost about a factor of 3.5 more (including development costs) than the largest which is currently available ($5 \text{ m}^3/\text{s}$) [20]. It is expected that pumps with a sufficiently high speed, such that the required number of pumps is not excessively large, will be available on the time scale envisioned for TITAN-I.

The magnetic field in the vicinity of turbo-molecular pumps must be less than a few hundred Gauss. To achieve this in a fusion facility which has relatively high magnetic

fields and requires close coupling of the pumps and vacuum tank, some magnetic shielding may be required. This shielding can be included in the pump housing outside the vacuum tank.

The other option for the high-vacuum pumps is to use compound cryo-pumps. Such pumps remove most gases by cryo-condensation and contain a sorption stage to remove helium. These pumps are already available at sizes sufficient for TITAN. The greatest drawback to their use is the high tritium inventory that accumulates within the pump between regenerations, which raises significant safety concerns and was the reason for preferring turbo-molecular pumps for TITAN.

Scroll pumps can be used to provide backing of the turbo-molecular pumps. These pumps, which employ a wobble rather than a rotary drive, are a sealed unit with no bearing lubricant exposed to tritium. Four 600 m³/h scroll pumps (one for every three duct locations), which can operate between atmospheric pressure and 50 mtorr, should provide adequate backing for this application.

11.9. SUMMARY AND CONCLUSIONS

The design of the impurity-control system poses some of the most severe problems of any component of a DT fusion reactor; for a compact or high-power-density device, these problems can be particularly challenging. For TITAN-I the impurity-control system is based on the use of toroidal-field divertors to minimize the perturbation to the global magnetic configuration, and to minimize the coil currents and stresses. Three such divertors are used as a compromise between the conflicting desires of minimizing the total ohmic losses in the divertor coils and maximizing the total area of divertor plates.

In order to maintain the heat flux on the divertor target plate at acceptable levels ($\leq 10 \text{ MW/m}^2$), the TITAN plasma is required to operate in a high-radiation regime, such that a total of about 95% of the steady-state heating power is radiated in the core, edge, and divertor plasmas. An "open" configuration, in which the divertor target is located close to the null point in the magnetic field, is used, rather than a "closed" configuration, which tends to produce large peaking factors in the heat-flux distribution. These features, together with careful shaping of the divertor target surface, allow the maximum heat flux at the inboard location to be restricted to 9.5 MW/m^2 , with a peak outboard value of 6.0 MW/m^2 .

To satisfy the requirement for a high- Z material for the plasma-facing surface of the divertor target, a tungsten-rhenium alloy (W-26Re) is proposed. The high rhenium

content provides the high ductility and high strength necessary for the severe loading conditions. A bank of lithium-cooled vanadium-alloy coolant tubes removes the heat deposited on the target; these tubes are separated from the tungsten-alloy armor by a thin, electrically insulating layer of spinel, to avoid an excessive MHD pressure drop. Fabrication of the divertor target is based on brazing of the tungsten-alloy plate (which is produced by powder-metallurgy techniques) to the bank of coolant tubes, with the spinel layer deposited by CVD process.

The low value of the toroidal field in the RFP allows high coolant velocities to be achieved without prohibitive MHD pressure drops, thus permitting operation in the turbulent flow regime, with the associated high heat-transfer coefficients. The maximum structural temperatures are $\leq 750^{\circ}\text{C}$ in the vanadium-alloy coolant tubes, and about 930°C in the tungsten-alloy armor. A 2-D finite-element structural analysis indicated that stress concentrations will occur at the edge of the interface between the different materials of the target; this aspect requires further analysis and experimental investigation to ensure the viability of the design.

The vacuum system is based on the use of a large vacuum tank encompassing the entire torus, and connected to the divertor region by a duct located at each of the three divertor locations. Lubricant-free magnetic-suspension-bearing turbo-molecular pumps for the high-vacuum pumps are proposed to avoid the possibility of tritium contamination of oil lubricants. The pumps of the required size need to be developed.

In conclusion, at the present level of analysis, the toroidal-field divertor design for TITAN-I appears to represent a feasible design approach for the impurity-control and particle-removal system for a high-power-density RFP reactor. A number of areas require further analysis and experimental investigation to confirm their potential as described in this report. Demonstration of good RFP operation with a toroidal-field divertor is clearly necessary to justify the TITAN design. The use of a radiation-dominated plasma is central to the divertor design, and also requires further experimental work. The stress concentration which occurs at the boundary of the different materials needs further examination to ensure that the structural design is adequate, and the data base for the irradiated properties of the divertor target materials, especially the tungsten alloy, requires considerable expansion.

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12. TITAN-I TRITIUM SYSTEMS

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12. TITAN-I TRITIUM SYSTEMS

12.1. INTRODUCTION

The major units in the tritium system of a fusion reactor are: (1) the the blanket tritium-recovery system, (2) plasma-exhaust processing system, (3) the fuel-processing and safety systems. The complete tritium system has to be designed under the constraints of tritium inventory, system cost, and tritium leakage rate. Significant relaxation of any one of the constraints will have a major impact on the overall design of the complete system.

In the TITAN design, plasma fueling is performed by injection of pellets. Because the separation of the D and T of the plasma exhaust is not required, the cryogenic distillation system needs only sufficient capacity to separate protium generated by the DD reaction. Since only about 1% of the plasma exhaust will be processed, the cost of the plasma-exhaust processing is much reduced and a redundant unit is affordable. A double plasma-exhaust processing system can significantly improve the reliability of the system, and the reactor tritium-fuel storage can be reduced.

The molten-salt-recovery process [1] is selected for tritium recovery from the lithium blanket. This process has been demonstrated on the laboratory scale [1] to recover tritium from lithium down to 1 wppm. Therefore, the tritium inventory in the blanket will be moderate. A unique advantage of using lithium as the primary coolant and the breeder is associated with the high tritium solubility in the lithium. For a tritium concentration of 1 wppm, the tritium partial pressure is only 10^{-7} Pa. With such low tritium partial pressure, tritium containment is usually not a severe problem. This reduces the required capacity of the room air-detritiation system and the secondary containment systems.

Sodium has been selected as the intermediate coolant for most fusion reactor designs [2]. Using the molten-salt extraction method to remove tritium from the primary coolant, the tritium partial pressure will be about $\sim 10^{-9}$ torr which is much smaller than the obtainable pressure by cold trap in sodium at 115°C [3]. Therefore, the maximum partial pressure of the tritium in the secondary coolant (sodium or lithium) and tritium permeation to the steam side would be set by the tritium pressure in the primary coolant. One advantage of sodium is the low solubility of tritium in sodium compared to that of lithium resulting in a smaller tritium inventory in the secondary loop by about

two orders of magnitude. For the TITAN-I design, it was decided that this advantage is not significant and to also use liquid lithium as the secondary coolant since this choice avoids the requirement of two separate liquid-metal systems (sodium and lithium) and the related technologies. Since tritium solubility is much higher in lithium than sodium, the TITAN-I design has a moderate amount of tritium inventory in the secondary loop.

In Section 12.2, the blanket tritium systems will be discussed, including estimates of tritium diffusion and inventory in the fusion power core (FPC) and the description of the molten-salt recovery system chosen for the TITAN-I design. Fuel-processing and tritium-cleanup and safety-related systems are discussed in Sections 12.3. Results and conclusions are summarized in Section 12.4.

12.2. BLANKET TRITIUM SYSTEM

12.2.1. Plasma-Driven Permeation

The TITAN-I plasma operates in a highly radiative regime with a high-recycling, open divertor configuration to reduce the heat flux on the divertor plates to acceptable levels. This configuration also decreases the plasma temperature at the divertor plate and at the first wall to minimize erosion. As a result, most particles impinging on the first wall have very low temperatures (5 eV or less). Vanadium alloys, such as those used in the first-wall and blanket structure (V-3Ti-1Si), are known to be susceptible to tritium permeation [4]. The potential, therefore, exists for a large plasma-driven permeation (PDP) into the first-wall coolant. At low particle energies, a phenomenon of major concern called "super-permeation" [5] has been observed. This phenomenon can be best explained by Figure 12.2-1. With a high particle energy, PDP reaches a peak about 250 s after initial implantation, but then decreases to a much lower, steady-state value. As the plasma-side surface is cleaned by the high energy particles, surface diatomic recombination and release of tritons becomes much easier, enhancing diffusion of the implanted tritons toward the plasma side. However, low energy particles will not clean the surface, hindering plasma-side re-emission and giving rise to super-permeation. In Figure 12.2-1, the SS-304 structural material has PDP by 20 eV ions 750 times higher than that by 320 eV ions. Energetic plasma ions of energies 40 eV or more are known to clean the surface and prevent super-permeation [6]. Although a small fraction of the neutral particles in TITAN-I edge-plasma have energies of this magnitude, their ability to clean the surface adequately is unclear. The energy spectrum of these neutrals, estimated by the neutral transport calculations (Section 5.5), is given in Table 12.2-I.

Figure 12.2-1. Typical data showing a transient permeation rate during high-energy ion implantation of stainless steel, as opposed to monotonic approach to steady state for low-energy implantation [6].

In the TITAN-I design, 95% of the tritons (both charged and neutral) have energies below 5 eV at the first wall. Two major uncertainties arise in evaluating the resulting PDP: (1) the surface cleanliness and the rate of plasma-side re-emission after implantation, as mentioned above and (2) the fraction of ions reflected from the first wall at energies below 10 eV. The fraction of hydrogen reflection off vanadium and vanadium alloys at these low energies is uncertain and implantation at this low energy range is poorly modeled. One analysis [7] shows the fraction of hydrogen, reflected off nickel, remains at 90% down to particle energies of about 3 eV. Below this energy, surface sticking and implantation of the ions increases drastically.

For evaluation of typical values of PDP, 90% reflection of the first-wall tritium flux (10% implantation) and a clean first wall (recombination sticking coefficient, $\alpha = 1$) are assumed. Incident tritium flux on the first wall is estimated to be $1.5 \times 10^{21} \text{ m}^{-2} \text{ s}^{-1}$. Depth of implantation is evaluated using the TRIPOS code [8]. The PDP is evaluated using the DIFFUSE code [9] with boundary conditions of diatomic recombination and $\alpha = 1$ on both sides [2]. The first wall of TITAN-I is made of V-3Ti-1Si alloy and consists of a poloidal bank of 0.8-cm-diameter tubes with initial (beginning-of-life) wall thickness of 1.25 mm and a 0.25-cm erosion allowance for one year of operation. The

Table 12.2-I.
ENERGY SPECTRUM OF NEUTRAL DT FLUX
AT THE TITAN-I FIRST WALL

Energy (eV)	Percent of Flux
< 5	85
5 - 25	10
25 - 100	4
100 - 500	1

DIFFUSE code indicates a PDP of about 100 g/d of tritium into the first-wall coolant, increasing daily tritium extraction requirements by a manageable 25% over that required for tritium-breeding extraction.

If the plasma-side surface is dirty ($\alpha \sim 10^{-3}$ to 10^{-5}), PDP of many kg/d of tritium is possible. For example, a first-wall surface with a sticking coefficient decreased by two orders of magnitude ($\alpha = 0.01$) would increase the PDP to about 1 kg/d, requiring a larger and more costly extraction system. If in actuality such large PDP occurs and poses a design problem, a permeation barrier such as several microns of tungsten deposited by chemical-vapor deposition on the inside or outside of the first-wall tubes would reduce this permeation to acceptable levels.

In the TITAN-I design, a tungsten-rhenium alloy (W-26Re) is chosen for the divertor plates. Tungsten is very resistant to permeation and, therefore, PDP through the divertor plate is not a concern. The DIFFUSE code indicates permeation through the divertor of about 0.1 g/d of tritium, which is insignificant relative to the first-wall permeation.

The problem of PDP which is described here is not unique to compact RFP reactors. Any fusion reactor design with a combination of a low edge-plasma temperature and a vanadium-alloy first wall must consider the severity of this problem. Experiments are needed to determine the extent of plasma-driven permeation and the sputtering rate of the first-wall structure at low edge-plasma temperatures.

12.2.2. Molten-Salt Recovery Process

The molten-salt extraction process has shown considerable promise for removing hydrogen isotopes down to the ppm level for liquid lithium [1]. In this method, liquid lithium and a molten salt are in contact, and LiH is preferentially distributed to the salt phase. The salt is then electrolyzed to yield H_2 at an emf of about 0.9 V; this voltage is significantly below that of the decomposition potential of the salt (> 2 V). The hydrogen is recovered from the molten salt by sweeping the porous stainless-steel hydrogen electrode with a circulating stream of inert gas. The tritium is subsequently recovered from the inert gas with a getter.

The LiH is preferentially dissolved in the salt. The volumetric distribution coefficient,

$$D_v = \frac{(\text{wppm T in salt}) \times (\text{density of salt})}{(\text{wppm T in Li}) \times (\text{density of Li})}, \quad (12.2-1)$$

is 3.1 for protium and 3.9 for deuterium in liquid lithium. This favorable distribution coefficient is the key reason that molten-salt recovery is effective for tritium recovery from lithium.

The flow rate of the lithium which is processed per unit time, X , is given by

$$X = \left(\frac{T_b}{I_{ss}} \cdot \frac{\epsilon D_v \eta + 1}{\epsilon D_v \eta} \right) M_{Li}, \quad (12.2-2)$$

where T_b is the tritium breeding rate, ϵ is the efficiency of tritium recovery from the salt, η is the efficiency factor which accounts for nonequilibrium tritium distribution during contacting, I_{ss} is the steady-state tritium inventory, and M_{Li} is the lithium inventory.

It has generally been assumed that η is equal to unity, yielding $D_v \eta \simeq 4$. An important trade-off [10] occurs between the recovery efficiency, which determines the number of contacting units, and the average current density, which dictates the volume of the electrolysis tank. With all other parameters fixed, the efficiency of the salt processing, ϵ , determines the number of contacting units required to maintain a given tritium steady-state inventory. On the other hand, the average current density decreases as the recovery efficiency increases, since the average current density is proportional to the tritium concentration. Therefore, the total electrode area and the size of the electrolysis tank must be large to extract the tritium at the lower current density and still recover tritium at a rate equal to the breeding rate. The salt processing step can be operated at high recovery efficiency and low current density or the recovery efficiency can be reduced to increase the average current density. It is believed that maximizing the recovery efficiency will be

the preferred mode of operation, since the contacting units will be more complex devices than the salt processing tanks and the contacting operation will require substantially more electrical power.

As can be seen in Table 12.2-II, the processing requirements are small for 1 wppm of tritium in lithium. These requirements increase linearly as the tritium concentration decreases so that the processing requirements at 0.1 wppm are a factor of 10 higher than those given in Table 12.2-II. Furthermore, the molten-salt extraction process lends itself to a modular design in which relatively small units are connected in parallel with the liquid-lithium blanket. This design allows additional flexibility for accommodating changes in the allowable tritium inventory of the blanket. If future safety regulations, economic constraints or design difficulties require a reduction in the tritium inventory, additional processing units could be attached easily to meet the new requirements.

The lithium that is separated from the molten-salt contactor will be saturated with the molten salt. The corrosion characteristics of the lithium saturated with the salt may be different from that of the pure lithium. In the molten-salt extraction experiment at Argonne National Laboratory, a stirrer was exposed to the Li/salt mixture for 2000 h at 500°C [10]. The stirrer was in motion for approximately 1000 h and for 6 h, the temperature was 600°C. Upon inspection of the stirrer, it was discovered that: (1) the corrosion rate is nearer that expected for lithium corrosion than that for molten salt corrosion; (2) nickel is preferentially depleted from grain boundaries, as would be expected for lithium penetration; and (3) chromium appears to accumulate at the grain boundaries, again suggesting lithium penetration rather than salt penetration, since molten salts would more than likely cause chromium depletion. Therefore, it is concluded that salt has a small impact on the corrosion characteristics of lithium. However, potential effects of MHD and radiation need further investigation.

12.2.3. Tritium Diffusion

Calculations of tritium permeation from the lithium coolant through the vanadium-alloy structure were made using DIFFUSE [9], with boundary conditions of diatomic recombination on any outer surfaces (recombination coefficient $\alpha = 0.1$) and Sievert's law on the coolant side of the pipes. Sievert's law corresponds to solubility of gas phase species in an adjacent solid [9]:

$$c(x,t) = S[T(x,t), P(t)] X(t), \quad (12.2-3)$$

with c the concentration of the species in the solid at position x and time t , S the solubility of the species which is a function of temperature and pressure, and X the

Table 12.2-II.
ANALYSIS OF MOLTEN-SALT EXTRACTION SCHEME
FOR A LIQUID-LITHIUM BLANKET SYSTEM

Breeding rate	420 g/d ^(a)
Recovery rate	520 g/d ^(a)
Lithium exit temperature	556 °C ^(b)
Extraction system temperature	556 °C ^(b)
Estimated blanket inventories	
Lithium	2.12×10^6 g
Tritium	212 g (1 wppm)
Tritium recovery efficiency, ϵ	90%
Capacity per extractor unit	23 m ³ /h
Electrical power per unit	3.7 kW

Tritium Concentration (wppm)	Effective Distribution Coefficient ($D_v\eta$)	Lithium Processed per Hour (kg/h)	Number of Units	Required Electrical Power (kW)
1	4	22,000	7	26
1	1	88,000	28	104 ^(c)

(a) Based on a tritium-breeding ratio of 1.2 and 100 g/d of PDP.

(b) Parameters of the Scoping Phase design,
the blanket and first-wall coolant were mixed in the outlet.

(c) Reference case.

mole fraction of the species in the gas phase. To evaluate the ambient tritium pressure corresponding to some concentration of tritium in liquid lithium, the following expression was employed [1]:

$$C = 17.9 P_{T_2}^{1/2} \exp(12,284/RT), \quad (12.2-4)$$

where C is the concentration of tritium in the lithium (wppm), P_{T_2} is the tritium partial pressure (torr), R is the ideal gas constant (1.987 cal/mole K), and T is the temperature (K). Given the temperature of the lithium within different regions of the blanket and piping and using a typical tritium concentration of 1 wppm in the lithium, the tritium partial pressures were calculated and input into DIFFUSE.

Tritium permeation rates through the intermediate heat exchangers into the steam-generator system is a major concern with respect to the release of the radionuclides into the environment under normal operating conditions. This permeation was estimated using an expression from Reference [2] for permeation through HT-9 tubing and an assumed oxide barrier factor of 100 on the water side of the pipes. The steam-generator piping consists of 1-mm-thick HT-9 tubing with surface area 2.7×10^4 m². The permeation rate into the water for ultimate release to the atmosphere is estimated to be 6.8 Ci/d, which is less than the target release rate of 10 Ci/d to the environment. The tritium concentration within the steam system is negligible.

Out-of-blanket piping structure in the primary and secondary loops is a stabilized ferritic steel with a 15-cm inner diameter and a 3-mm wall thickness. The DIFFUSE calculations using the boundary conditions mentioned above indicate permeation through the bare pipes into containment would total about 2 Ci/d for both loops. However, insulation on the pipes acts as a barrier and reduces permeation to a negligible level. The inventory within this out-of-blanket piping structure is estimated to be less than a milligram of tritium.

The tritium inventories within the first-wall structure are evaluated by DIFFUSE, using the first-wall boundary conditions described in Section 12.2.1. The typical case of a clean first wall gives an inventory of 0.72 g, while a dirty first wall ($\alpha = 0.01$) increases the inventory to 4.53 g. The structural inventories within the balance of the fusion reactor core are evaluated using the boundary conditions mentioned above. These inventories are given in Table 12.2-III.

Table 12.2-III.
TRITIUM INVENTORIES IN THE TITAN-I REACTOR

Unit	Tritium Inventory (g)
Storage	1,100
Primary-coolant loop	212
Secondary-coolant loop	300
Molten-salt extraction	10
Fuel processing	20
First wall:	
typical case	0.72
excessive PDP	4.53
Integrated blanket coil	2.20
Hot shield, zone 1	0.14
Hot shield, zone 2	0.25
Divertor shield	0.08
Divertor	0.01
Out-of-blanket piping	0.01
TOTAL TITAN-I inventory	1,650

12.3. FUEL-PROCESSING AND SAFETY SYSTEMS

The fuel-processing loop will process the reactor exhaust gas to remove and decompose all impurities so that all of the deuterium and tritium are recovered; will perform isotopic separation of the hydrogen isotopes to provide any or all of the species D_2 , DT , and T_2 as required by the various fueling systems; and will provide safe, long-term storage of the tritium inventory during shutdown periods. The fuel-processing system will also process tritium recovered from the blanket-extraction system and from the blanket module experiments and prepare this tritium supply for use in the fueling system. These interactions are shown in the tritium flowchart, given in Figure 12.3-1.

The tritium-cleanup and safety system is necessary to provide personnel, public, and environmental protection. This system includes secondary-containment-cleanup, tritium-contaminated-waste-cleanup, and room-air-detritiation subsystems. The latter subsystem will be used to recover tritium which may be released into the confinement building and/or the reactor hall and to process the air in the hot cell and the decontamination area. Other safety and environmental protection systems are included as part of the tritium-cleanup system (e.g., tritium-monitoring equipment and modification to the ventilation system to accommodate maintenance operations in the tritium system).

Tritium-related subsystems for accomplishing the fuel processing and tritium cleanup and safety include the following:

- Tritium receiving and storage
- Fuel cleanup
- Isotope separation
- Fuel preparation
- Transfer and circulation pumps
- DT fueling
- Glovebox-atmosphere detritiation
- Room-air detritiation
- Tritium-waste treatment
- Tritium monitoring

Figure 12.3-1. Flowchart of the TITAN-I tritium system.

- Breathing air
- Liquid-waste disposal
- House vacuum
- Flexible-ventilation duct
- Tritium data acquisition and control

The tritium subsystems must maintain the concentrations of the airborne tritium below $20 \mu\text{Ci}/\text{m}^3$ in spaces occupied by unprotected operating personnel and below $4000 \mu\text{Ci}/\text{m}^3$ for protected (in bubble suit) operating personnel. This assumes a protection factor of 200 for the bubble suit.

12.3.1. Fuel-Processing System

The fuel-processing system will process all gases exhausted from the plasma chamber, tritium recovered from the blanket and coolant streams, and the fueling-system gas-recovery system. The impurities which are expected to be in the exhaust gas and which must be removed are shown in Table 12.3-I. The fuel-processing system must not only remove these impurities, but also decompose them to recover the contained deuterium and tritium.

Key parameters of the fuel-processing system are summarized in Table 12.3-II and the capital costs for fuel-processing and associated safety systems are given in Table 12.3-III. The subsystems of the fuel-processing system are discussed below.

12.3.1.1. Fuel-cleanup system

A schematic flow diagram of the proposed fuel-cleanup (FCU) system for the TITAN reactor is shown in Figure 12.3-2. The exhaust gas from the plasma chamber first enters a palladium-alloy membrane diffuser. The hydrogen isotopes rapidly permeate through the alloy while impurities are unable to pass through. This gives an extremely pure hydrogen isotope stream to feed the isotope separation system (stream 3). A bleed stream containing the impurities is continuously removed from the diffuser (stream 4).

If these impurities contain hydrogen isotopes (off-normal condition), this stream passes over a heated catalytic-oxidation bed where the reactive impurities are oxidized

Table 12.3-I.

**ESTIMATED CONTAMINANT-GAS CONCENTRATION
IN THE VACUUM-PUMP EXHAUST OF TITAN REACTOR**

Gaseous Component	Atomic %	Probable Chemical Species
A. Normal Operation		
D	~ 45	DT
T	~ 45	DT
He	5-10	He
H	1-2	HD, HT
Xe	≪ 1	Xe
B. Off-Normal Operation (Air Contamination)		
D	~ 45	DT
T	~ 45	DT
He	5-10	He
H	1-2	HD, HT
Xe	≪ 1	Xe
N	2	N(D,T) ₃
O	< 1	DTO
Ar	≪ 1	Ar

Table 12.3-II.

PARAMETERS OF THE FUEL-PROCESSING SYSTEM

Tritium flow rate to fuel processing	5,920 g/d
Deuterium flow rate to fuel processing	3,950 g/d
Tritium inventory in processing	20 g
Volume of fuel-processing equipment	1,509 m ³
Power required for processing (maximum)	830 kW

Table 12.3-III.

CAPITAL COSTS OF THE FUEL-PROCESSING SYSTEM

Subsystem	Cost, k\$ (1986)
Fuel cleanup, isotope separation	3,225
Glovebox detritiation	1,525
Data acquisition and control	2,315
Emergency power	1,180
Transfer pumps, storage	690
Tritium monitors, gas analysis	940
Secondary containment, waste disposal, inventory control	475
TOTAL	10,350

Figure 12.3-2. Flow diagram of the fuel-cleanup system.

and all hydrogen isotopes end up as water. This water is collected in the molecular-sieve moisture traps while the nontritiated impurities (He, CO₂, N₂, Ar) pass through the moisture trap to the tritium-waste-treatment system. Periodically the moisture traps are heated and the water released is passed to a ceramic electrolysis cell (stream 11) where the water is electrolyzed. The released oxygen permeates through the ceramic (stabilized zirconia) and is fed to the tritium-waste-treatment system (stream 14). The hydrogen isotopes released in the electrolysis cell (stream 12) are fed back to the inlet of the palladium membrane and ultimately to the isotope-separation system (stream 3). This FCU is based on components developed by the Japan Atomic Energy Research Institute [11] and tested with tritium at the Tritium System Test Assembly (TSTA) [12].

Tritium recovered from the blanket is fed into the main inlet stream to the FCU subsystem (stream 1). Some additional preprocessing of this tritium may be required, depending the nature of the breeding material in the blanket modules. For instance, if the tritium released from the breeder is in the form of HTO, it may be profitable to decompose the HTO before it enters the main fuel-cleanup unit.

The FCU is designed to recover > 99.9% of all hydrogen isotopes in the plasma exhaust gas. The impurity level in the FCU output stream is of the order of 1 part per million.

12.3.1.2. Isotope-separation system

A stream of the purified hydrogen isotopes from the fuel-cleanup system is sent to the isotope-separation system (ISS). The ISS is based on fractional distillation of the hydrogen species at cryogenic temperatures, a well developed technology [12]. The distillation columns are packed with commercial, stainless-steel packing material and are operated at ~ 20 K.

The ISS is designed basically in accordance with the TSTA [12] design concept and equipment. Each column is a few centimeters in diameter and 3 to 4 m in height. The refrigeration unit required to provide helium gas coolant at 20 K is contained within the isotope-separation package.

12.3.1.3. Fuel-preparation system

The fuel preparation system consists of a series of transfer pumps, calibrated volumes, and small holding tanks. Feed gas for this system comes from the tritium storage beds, deuterium storage tanks, and the isotope-separation unit.

12.3.1.4. Transfer and circulation pumps

A number of circulation and transfer pumps are required in the fuel processing system. These pumps are used to provide the driving force and pressure differentials to move the gas stream through the fuel-cleanup, isotope-separation, and fuel-preparation systems. These pumps must be all-metal, dry, tritium-compatible pumps. The pumps selected are manufactured by the Metal Bellows Corp., Sharon, Massachusetts [13] and have been in tritium service at various laboratories for several years. The Metal Bellows pump is a completely tritium-compatible alternative for the low-vacuum region. The vacuum chamber of this pump is carefully sealed from the atmosphere and from all moving mechanical parts. The Metal Bellows pump is limited by low throughput when a high compression ratio is required, hence, using more pumps with lower compression ratios in series is preferred.

The second transfer pump is a tritium-compatible, orbiting scroll pump manufactured by Normetex (France) [14]. Standard sizes for these pumps range from 15 to 1300 m³/h. Operation of these pumps relies on the relative motion of two spiral vanes: one is stationary, while the other moves in a circular orbit. The chambers are sealed from both the atmosphere and from all lubricated parts by a bellows seal. The two spiral vanes are noncontacting so no lubricant is required. As a result, the pump does not contaminate the pumped gases and there is no tritium-contaminated pump oil to contend with. The Normetex scroll pumps can be operated from a few Pa to atmospheric pressure. Although the scroll pumps are more efficient in the low vacuum region than in the intermediate region, they are most useful in the intermediate region because of the lack of other tritium-compatible pumps for this pressure regime.

12.3.1.5. Fuel-storage system

The fuel in the fuel-storage system consists of unburned fuel and bred tritium recovered by the ISS, as well as new fuel received from off-site sources. The isotopic components of the fuel (D₂, DT, and T₂) are fed to the fueling systems during operation of the reactor. The storage of the DT and T₂ fuel components is on a metal matrix. The metal storage beds consist of double-jacketed vessels containing a metallic getter in the form of sponge, powder, or turning. The gettering material must have good thermal stability and hydrogen-release properties. There are several potentially suitable gettering materials such as uranium, zirconium-cobalt alloy, or LaNi_{5-x}Al_x alloys. These metals all have a low dissociation pressure at room temperature. The temperature required to

reach a dissociation pressure of one atmosphere is in all cases 400°C or less. The metal storage beds cannot help in the removal of decay helium from the fuel. Because helium is not adsorbed on a metal getter bed, tritium can be transferred from one bed to a second bed. Any helium in the one atmosphere of the gas will remain gaseous and, therefore, can be pumped away by a small vacuum pump. About 5 kg of $\text{LaNi}_{5-x}\text{Al}_x$, 37 kg of ZrCo , or 115 kg of uranium metal are needed to store 1 kg of deuterium or 1.5 kg of tritium.

12.3.1.6. Redundancy of fuel-processing subsystems

To ensure a highly reliable fuel supply at a minimum cost, certain key fuel-processing subsystems should be duplicated. These key systems are the fuel-cleanup system (FCU), the isotope-separation system (ISS), the transfer pumps, the fuel-storage system (FSS), the associated gloveboxes, tritium monitors, and controls. The cost for these redundant systems is \$4.5 million. This cost is very small compared to the cost of storing even one day's throughput of standby tritium, which would amount to over \$60 million.

12.3.2. Tritium-Cleanup and Safety System

The tritium-cleanup system includes the safety subsystems for radiation and tritium monitoring, protection of the public and employees against exposure to radiation, and preparation of waste tritium for shipment off-site for reprocessing or safe disposal. Systems for both normal operations and emergency situations are discussed below.

12.3.2.1. Glovebox- and room-atmosphere-detritionation systems

Tritium may be accidentally released into gloveboxes or the room atmosphere. In either case, the tritium will be purged from the atmosphere and solidified on a matrix as an oxide for eventual recovery or disposal. Two detritionation subsystems are planned: one with a capacity of 50 cfm (cubic feet per minute) for continuously processing glovebox atmospheres, and another larger system for processing room atmospheres.

The smaller glovebox-atmosphere-detritionation system (GBADS) will be a batch system consisting of redundant blowers, catalysts with preheaters and heat exchangers, and desiccant beds. This redundancy may be achieved by a single subsystem with parallel components or by two separate and independent units, each with a capacity of 25 cfm. The cost difference between the two configurations is not expected to be significant. In

Table 12.3-IV.
ROOM-AIR-DETRITIATION SYSTEM (RADS)
DESIGN ASSUMPTIONS

Ideal cleanup time ^(a)	3 d
Tritium spill size	50 g
Tritium concentration after cleanup ^(b)	20 $\mu\text{Ci}/\text{m}^3$

(a) No absorption on walls and surfaces. Surface absorption will extend cleanup time.

(b) Perfect mixing in room air. Imperfect mixing may allow shorter cleanup time by selective use of portable ventilation ducting.

either case, the maximum capacity of 50 cfm can be achieved with both blowers in operation. Common to the systems will be two large receiving tanks, normally kept below atmospheric pressure. Four desiccant beds are to be used, two for each of the units if the separate unit configuration is adopted. A separate regeneration unit will be available for drying the desiccant beds as needed. A possible alternative to the above-described method for removing tritium from inert glovebox atmospheres is the adsorption through chemical bonding of elemental tritium by metal "getters." This technology is currently under development and is not available at the present time.

The large room-air-detritiation system (RADS) will consist of three separate and independent 5000-cfm units that can operate individually or in parallel, depending on the size and the number of the rooms to be cleaned up (Figure 12.3-3). The rooms to be ducted to the RADS include the test cell, hot cell, warm cell, tritium facility, and GBADS and RADS rooms. The RADS unit capacity was determined by the requirement (Table 12.3-IV) that it be capable of reducing the tritium concentration in the TITAN test cell ($170,000 \text{ m}^3$) following a release of 50 g to the derived-air concentration (for HTO in air) of $20 \mu\text{Ci}/\text{m}^3$ in 72 h, which is satisfactory for worker exposure with no protection.

The relationship of system cost to the detritiation time is shown in Figure 12.3-4. Each unit (Figure 12.3-5) comprises a blower, a catalyst with heater and heat exchanger, and two redundant desiccant beds. A cooler/refrigerator dryer may be incorporated to

Figure 12.3-3. Room-air-detritionation system (RADS).

Figure 12.3-4. Cost of the detritiation system as a function of room detritiation time.

reduce the size of the desiccant beds. Alternatively, the components of the units may be linked together so as to provide component redundancy as opposed to unit redundancy, at little increase in cost. Regeneration of the desiccant beds will be done by unloading and replacing with dry desiccant. Once removed, regeneration of the moist desiccant may be accomplished by the GBADS regeneration unit, or alternately, it may be treated as waste. Table 12.3-V summarizes the parameters of the RADS equipment.

Table 12.3-V.

**PARAMETERS OF
 THE ROOM-AIR-DETRITIATION SYSTEM (RADS)**

Equipment capital cost	\$4,350,000 (1986\$)
Equipment volume	3,000 m^3
RADS power requirement (maximum)	1,200 kW

Figure 12.3-5. Layout of a room-air-detritiation system.

12.3.2.2. Tritium-waste-treatment system

The primary purpose of this system is to receive and process the effluent gas from gloveboxes. In addition, other gaseous effluents generated throughout the facility (such as vacuum-pump exhausts) that could possibly contain tritium will be processed by this system. Tritium removal is based on the catalytic oxidation of tritium-containing compounds and subsequent removal of the tritiated water by adsorption on molecular sieve beds. The decontamination factor (ratio of the tritium input to tritium exhausted to environment) will be better than 10^5 . The system includes holding tanks, blowers, catalyst bed, and molecular sieve beds. The tritiated water is recovered from the molecular-sieve beds by regeneration with a heater-purge gas. The water is collected and packaged for shipment to an external disposal or reprocessing facility.

12.3.2.3. Radiation monitoring system

The radiation-monitoring system involves monitoring for safety as well as for the purpose of diagnostics and process control. The instruments to be used include gamma and neutron monitors as well as tritium monitors. Many are environmental (i.e., room, stack, or duct) monitors whereas others are for monitoring glovebox atmospheres or processes for tritium concentrations.

Approximately 90 to 100 fixed, active tritium monitors are projected for the TITAN facility. Tritium instruments are of the flow-through ionization-chamber type and are capable of measuring tritium concentrations from a few $\mu\text{Ci}/\text{m}^3$ to about one Ci/m^3 , the actual range depending on the application. Some selected monitors will be capable of discriminating between activated-air products, tritium gas, and tritium oxide. This type monitor is somewhat more expensive, but the safety implication of knowing the form (gaseous or oxide) is quite significant, because the oxide is about 25,000 times more hazardous.

Monitors will be needed in all gloveboxes, handling rooms for activated and/or contaminated equipment (e.g., hot cell, decontamination room) and for the rooms containing the tritium-cleanup subsystems (glovebox-air detritiation, tritium-waste treatment, and emergency room-air detritiation). All of these proposed instruments are fixed or transportable and do not include portable instruments and passive monitoring devices such as thermoluminescent dosimeters or neutron- or gamma-sensitive film.

12.3.2.4. Breathing-air system

The test cell and tritium-handling areas will be equipped with a supplied breathing-air system. Personnel will use breathing air for emergency access to areas where tritium has been accidentally released and for maintenance operations where there is the possibility that significant tritium may be released into the room.

The major components of the system are supplied air suits and connecting air hoses, breathing-air stations located throughout the facility, a pressure regulating and distribution system, and a source of breathing air. As a source for breathing air, either a compressor and a holding tank or a tube trailer filled with breathing air (approximate capacity 45,000 scf) will be used. One or more portable breathing air stations (on carts) will also be available, either as an alternative or a supplement to the fixed system.

12.3.2.5. Liquid-radioactive-waste system

This system includes all the piping, drains, pumps, and storage vessels necessary to collect liquid radioactive wastes, and to prepare for shipment off site for disposal. These wastes may be generated as a result of normal operations (washing, decontamination, etc.) or accident conditions which may, for example, produce water used for extinguishing a fire. The waste may be tritiated or may contain other radioactive materials. The system will normally handle only water. Other radioactive liquids will be packaged separately.

12.3.2.6. House-vacuum system

This system will provide a service vacuum to evacuate tritium-contaminated or potentially contaminated lines located throughout the tritium areas. Components will include an oil-free vacuum pump ($\sim 200 \text{ m}^3/\text{h}$ in size), and piping and outlets distributed in areas in which tritium will be handled. The pump will discharge to the tritium-waste-treatment system.

12.3.2.7. Flexible-ventilation-duct system

The flexible-ventilation-duct system will be used to provide local ventilation to areas where tritium has been or may be released into the room air because of an accident or a planned maintenance operation. Examples of uses are maintenance on the plasma

chamber through an open port or removal of a glovebox window. The exhaust of the flexible ventilation duct can be either routed to the stack, or if the tritium concentration in the air is higher than allowable release levels, to one of the tritium cleanup systems.

12.3.2.8. Local instrumentation and control system

The tritium system will have its own control system that will interact with the main control system in a supervisory manner. Several options are available for controlling the tritium subsystems. Either a central tritium-control system can monitor and control the separate subsystems, or distributed processors can control the individual subsystems with a central tritium-control computer having supervisory monitoring and control functions. The latter method is preferable. Manual (computer independent) control of each of the tritium subsystems may be included. The cost and space requirements for these options are similar.

12.4. SUMMARY AND CONCLUSIONS

The parameters of TITAN-I which are important to the design of the tritium systems are summarized in Table 12.4-I. A flowchart of the TITAN-I tritium systems is shown in Figure 12.4-1. The tritium throughput and inventory of each component has been calculated, as can be seen in Figure 12.4-1 and Table 12.2-III. The cost of the TITAN-I tritium system is summarized in Table 12.4-III. The cost estimates are based on the Tritium System Test Assembly scaling [15] and Blanket Comparison and Selection Study scaling [2]. The tritium inventory (1650 g), the leakage rate (7 Ci/d), and the tritium-system cost (\$35M) are all very reasonable.

A potential critical issue facing TITAN-I is the plasma-driven permeation of low energy tritons through the permeable vanadium-alloy first wall. The ability of the small fraction of high-energy plasma ions to adequately clean the first-wall surface is uncertain. Any fusion reactor design with low edge-plasma temperature may face similar problems.

An important feature of the TITAN-I design is the cryogenic distillation of only 1% of the plasma exhaust. Because of the associated cost reductions, redundant units are used in the plasma-exhaust-processing system to reduce significantly the tritium fuel storage (and cost) requirements.

Table 12.4-I.
TITAN-I TRITIUM PARAMETERS

Fusion power	2,301	MW
Thermal power	2,935	MW
Net electric output	970	MWe
Plasma tritium burnup fraction	5	%
Tritium-breeding ratio	1.2	
Tritium production rate	420	g/d
First-wall plasma-driven permeation	100 - 1000	g/d
Tritium extraction rate	520 - 1500	g/d
Tritium concentration in lithium	1	wppm

Figure 12.4-1. Flowchart of the TITAN-I tritium systems.

Table 12.4-II.
TITAN-I TRITIUM SYSTEM CAPITAL COSTS

Subsystem	Cost (k\$, 1986)
Fuel processing:	
Fuel cleanup, isotope separation	3,225
Glovebox detritiation	1,525
Data acquisition and control	2,315
Emergency power	1,180
Transfer pumps and storage	690
Tritium monitors and gas analysis	940
Secondary containment, waste disposal, and inventory control	475
Backup unit for fuel processing	4,500
TOTAL	14,850
Room-air detritiation	4,350
Molten-salt extraction (typical case)	5,000
Molten-salt extraction (excessive PDP)	15,000
 TOTAL cost for TITAN-I (typical case)	 24,200
TOTAL cost for TITAN-I (excessive PDP)	34,200

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13. TITAN-I SAFETY DESIGN AND RADIOACTIVE-WASTE DISPOSAL

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13. TITAN-I SAFETY DESIGN AND RADIOACTIVE-WASTE DISPOSAL

13.1. INTRODUCTION

Strong emphasis has been given to safety engineering in the TITAN study. Instead of an add-on safety design and analysis task, the safety activity was incorporated into the process of design selection and integration at the beginning of the study. The safety-design objectives of the TITAN-I design are: (1) to satisfy all safety-design criteria as specified by the U. S. Nuclear Regulatory Commission on accidental releases, occupational doses, and routine effluents and (2) to aim for the best possible level of passive safety assurance.

This section presents the safety design and evaluations of the TITAN-I reactor. The safety design goals for the TITAN reactors are discussed in Section 13.2 and the safety-design features are reviewed in Section 13.3. Safety analyses for the TITAN-I design are reported in Section 13.4 for loss-of-flow and loss-of-coolant accidents in the fusion power core, in Section 13.5 for lithium fires, and in Section 13.6 for plasma-related accidents. Section 13.7 describes the radioactive-waste-disposal issues and ratings for the TITAN-I design. A summary of the TITAN-I safety design and analysis is given in Section 13.8.

13.2. SAFETY-DESIGN GOALS

Two main objectives have guided the TITAN safety design: (1) to satisfy all safety-design criteria as specified by the U. S. Nuclear Regulatory Commission (U. S. - NRC) on accidental releases, occupational doses, and routine effluents; and (2) to aim for the best possible level of safety assurance.

Although the accident scenarios and classification systems developed by the U. S. fission industry may not apply directly to fusion reactors, the dose guidelines used by the fission industry will probably either be directly applicable or serve as useful references in defining the radiological safety requirements for fusion-reactor designs. The U. S. - NRC regulations covering fission reactors are described in the Code of Federal Regulations in Sections:

- 10CFR20 – Standards for Protection Against Radiation [1],
- 10CFR50 – Domestic Licensing of Production and Utilization Facilities [2],
- 10CFR100 – Reactor Site Criteria [3],
- 10CFR61 – Licensing Requirements for Land Disposal of Radioactive Waste [1].

Table 13.2-I describes the present industry guidelines [4] for satisfying the regulations and the dose-limit values are shown in Table 13.2-II. These NRC regulations define the maximum dose limits and releases of radioactivity during routine and anticipated

Table 13.2-I.

PRESENT INDUSTRY GUIDELINES ON DOSE EXPOSURE [4]

Frequency of Occurrence per Reactor Year, F ^(a)	Off-Site Dose
Planned operations	10CFR50, Appendix I ^(b)
$F > 10^{-1}$	10CFR50, Appendix I ^(b)
$10^{-1} > F > 10^{-2}$	10% of 10CFR100 ^(c)
$10^{-2} > F > 10^{-4}$	25% of 10CFR100 ^(c)
$10^{-4} > F > 10^{-6}$	100% of 10CFR100 ^(c)

(a) Data compiled from fission-reactor license applications reviewed by the NRC.

(b) Dose objective of 10CFR50, Appendix I must be met for the summation of radioactive releases due to planned operations and the annual average of events with $F \sim 0.1$. Individual radionuclide concentration limits are given by 10CFR20, Appendix B [1].

(c) Dose limits during preliminary design and review are 80% of the maximum allowable whole body limit and 50% of the maximum allowable limits for all other doses [3].

Table 13.2-II.
REGULATORY DOSE LIMITS

10CFR20 - Routine Release [1]

- Maximum permissible concentrations of radionuclides in air and water:
 - specified by individual isotopes for occupational and public areas
- Occupational exposure:

- whole body or individual organs	5 rem/y
- extremities	75 rem/y
- skin	30 rem/y

10CFR50, Appendix I (Public)^(a) - Routine Release [2]

- Gases:

- total body	5 mrem/y
- skin	15 mrem/y
- Liquids:

- total body or any organ	5 mrem/y
- total release per reactor (except tritium & dissolved gases)	5 Ci/y
- Particulates:

- any organ	15 mrem/y
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- As low as is reasonably achievable (ALARA):

- cost/benefit guideline expenditure to reduce exposure	1000 \$/man-rem
---	-----------------

10CFR100 - Accidental Release [3]

- Two-hour dose at site boundary, or dose during entire event at low population zone:

- whole body	25 rem
- thyroid	300 rem
- bone, lung, and other organs ^(b)	150 rem

(a) The Department of Energy (DOE) recently proposed a limit of 100 mrem/y annual dose from all routine operations to the public near DOE facilities [5].

(b) Inferred from 10CFR100 intent [6].

operations (10CFR20 [1] and 10CFR50 [2]) and during severe hypothetical accidents (10CFR100 [3]). In general, these guidelines were based on the biological effect of a radioactive release and thus are independent of the source of the release. Therefore, even though the regulations frequently reference nuclides of importance to nuclear fission which will have no significance in fusion (e.g., thyroid dose from ^{131}I), the intent of the guidelines remains valid.

Recently, four levels of safety assurance were proposed to facilitate the preliminary evaluation of different designs [7,8]. While these levels are neither precisely defined licensing criteria nor rules for formal safety evaluation, they do provide a relatively simple guide for designers who can use these definitions of different levels of safety to evaluate their designs or to improve on their safety features when appropriate. The following summarize the interpretation of these four levels of safety assurance as suggested by Piet [7] (also see Reference [8]).

Level 1 - "Inherent safety." Safety is assured by inherent mechanisms of release limitation no matter what the accident sequence is. The radioactive inventories and material properties in such a reactor preclude a violation of release limits regardless of the reactor condition.

Level 2 - "Large-scale passive-safety assurance." Safety is assured by passive mechanisms of release limitation as long as severe reconfiguration of large-scale geometry is avoided, and escalation to fatality-producing reconfigurations from less severe initiating events can plausibly be precluded by passive design features. In such a reactor, natural heat-transfer mechanisms suffice to keep temperatures below those needed, given the radioactivity inventory and material properties, to produce a violation of release limits unless the large-scale geometry is badly distorted.

Level 3 - "Small-scale passive-safety assurance." Safety is assured by passive mechanisms of release limitation as long as severe violations of small-scale geometry, such as a large break in a major coolant pipe, are avoided, and escalation to fatality-capable violations from less severe initiating events can plausibly be precluded by passive design features. In such a reactor, sufficiency of natural heat-transfer mechanisms to keep temperatures low enough, given its radioactivity inventories and materials properties, to avoid a violation of release limits can only be assured while the coolant boundary is substantially intact.

Level 4 - "Active safety assurance." There are credible initiating events that can only be prevented from escalating to site-boundary-release limit violations or reconfigurations by means of active safety systems. This is the conventional approach of add-on safety.

The public is adequately protected by all four levels of safety assurance. To understand the meaning of adequate protection of the public, the concept of safety assurance can be further strengthened in the context of probabilistic risk assessment. The risk-based safety goal for TITAN is that fusion accidents would not increase the individual cancer risk of the public by more than 0.1% of the prevailing risk. As a consequence of this goal, a site-boundary whole-body dose limit of 25 rem for accidental release for fission reactors (10CFR100 [3]) has been adopted.

13.3. SAFETY-DESIGN FEATURES

13.3.1. Design Guidelines

The TITAN-I fusion power core (FPC) is cooled by liquid lithium. Four basic safety-design approaches were incorporated into the design:

1. Physical separation of potentially reactive materials, such as lithium, water, concrete, and air (*e.g.*, by using multiple physical barriers);
2. Minimization of the amount of induced radioactivities (*e.g.*, by using reduced-activation materials);
3. Reducing the maximum material temperature caused by decay afterheat (*e.g.*, by using low-afterheat materials and by providing thermally conducting paths from the front to the back of the blanket);
4. Minimization of the amount of vulnerable lithium coolant (*e.g.*, by using lithium drain tanks).

Based on these design approaches, the following guidelines were derived.

- Low-activation and low-afterheat materials and designs should be used wherever possible to minimize the amount of releasable radioactivity.
- Passive afterheat-removal designs should be emphasized (*e.g.*, use of conducting paths leading from the first wall to the back of the blanket, and natural circulation for transferring the afterheat energy to a heat sink).
- Passive safety features should be incorporated in the plasma-engineering design to reduce the impact of plasma accidents.

- The FPC should be located below grade to reduce the impact from earthquakes and above-ground accidental events (*e.g.*, aircraft crash).
- In order to avoid a complete loss-of-coolant accident from accidental drainage, primary-piping connections should be located above the FPC.
- The entire primary-coolant system should be enclosed within the confinement building (or buildings, depending on the detailed reactor layout), and the confinement building(s) should be filled with argon cover gas in order to reduce the probability of a lithium fire.
- Water should be excluded from the confinement building or vacuum vessel to prevent the probability of lithium-water reaction.
- The confinement-building floor should be covered with steel liner and the walls and ceiling should be sealed with epoxy liner to minimize the probability of lithium-concrete reaction and the absorption of tritium and other radioactive materials on the walls.
- Lithium-fire-retarding agents (*e.g.*, graphite powder) should be designed for effective lithium-fire control and located conveniently inside the confinement building.
- Drain tanks with a flame-retarding drain-pipe design are recommended, to reduce the impact of a lithium fire.
- The first wall and blanket must be drained to prevent a lithium fire when air is allowed into the confinement building.
- Because of the vanadium-alloy structural material of TITAN-I, a cool-down period of two to seven days will be needed for the afterheat to decay before draining the reactor torus. The exact waiting period will be determined by details of mechanical and passive heat-removal designs.

The impact of these guidelines and the safety design of the TITAN-I reactor are presented in the remaining part of this section.

13.3.2. Material Selection

One of the major concerns in the safety design is the post-shutdown afterheat induced in the structure because of the high neutron wall loading of the TITAN-I design

Figure 13.3-1. The first-wall afterheat for V-3Ti-1Si, V-15Cr-5Ti, and HT-9 as a function of time after shutdown for 1 FPY of operation at 18 MW/m^2 of neutron wall loading.

(18 MW/m^2). The afterheat levels at the first wall of TITAN-I were evaluated for three candidate structural alloys (V-3Ti-1Si, V-15Cr-5Ti, and HT-9). Figure 13.3-1 shows the afterheat power density at the first wall for these three alloys as a function of time after one full-power year (FPY) of operation at 18 MW/m^2 of neutron wall loading. As shown, HT-9 has the highest level of afterheat among these three alloys for times to about six hours after shutdown, and for times after two days. The maximum power density in HT-9 is about 5.5 W/cm^3 at shutdown. The afterheat is primarily from ^{56}Mn (half-life of 2.6 h) which is induced in the major alloying element of the ferritic steel (iron) by the $^{56}\text{Fe}(n,p)$ reaction. The ^{56}Mn will decay away within one day, after which the afterheat is dominated by ^{54}Mn (half-life of 313 d), a product of $^{54}\text{Fe}(n,p)$ reaction.

The afterheat power densities for the two vanadium-based alloys are almost identical for about two days after shutdown, and are about a factor of two lower than HT-9 within the first hour after shutdown (Figure 13.3-1). The dominating radionuclides for the afterheat for both alloys are ^{48}Sc (half-life of 43.7 h) produced by the $^{51}\text{V}(n,\alpha)$ reaction, ^{51}Ti (half-life of 5.76 min) by the $^{51}\text{V}(n,p)$ reaction, and ^{52}V (half-life of 3.75 min) by the $^{51}\text{V}(n,\gamma)$ reaction, within about one week after shutdown.

For the first five minutes after shutdown, the silicon in V-3Ti-1Si contributes ~ 3% of the total afterheat, primarily from ^{28}Al (half-life of 2.24 min) produced by $^{28}\text{Si}(n,p)$ reaction. After one week, the dominant radionuclide in V-3Ti-1Si is ^{46}Sc (half-life of 83.8 d), a product of $^{46}\text{Ti}(n,p)$ reaction. Because of a higher Ti content, the afterheat values (from ^{46}Sc) in V-15Cr-5Ti will be slightly higher than V-3Ti-1Si. In addition, V-15Cr-5Ti contains significant amounts of chromium and the decay heat from ^{51}Cr (half-life of 27.7 d), produced by $^{52}\text{Cr}(n,2n)$ and $^{50}\text{Cr}(n,\gamma)$ reactions, should be considered.

These results indicate the significant advantage of the vanadium-base alloys in terms of lower afterheat power density within a few hours after shutdown and have influenced the selection of V-3Ti-1Si as the TITAN-I structural material.

13.3.3. Configuration and Lithium-Drain-Tank Design

The configuration of the TITAN-I FPC is illustrated in Figure 13.3-2, which shows the impact of the safety guidelines of Section 13.3.1. Three physical barriers (*i.e.*, blanket tubes, vacuum vessel, and confinement building) are used to separate the primary lithium from air and water. As illustrated, the primary-coolant connections are all located on top of the FPC to prevent complete drainage of the coolant. The worst accident for a lithium self-cooled design is perceived to be an uncontrolled lithium fire. To reduce the impact from this potential accident scenario, passive drain tanks were incorporated into the TITAN-I design.

Figure 13.3-3 shows the geometric model which was used in the design of the drain tanks and drain pipes in order to evaluate the feasibility of the proposed design approach. Two sets of drain tanks will be needed: one connected to the vacuum vessel and one to the confinement building. Also shown are passive fire-retarding valves to prevent fire propagation into the drain tanks. These valves will allow the heavier liquid lithium into the drain tanks and minimize the amount of air entering the system by closing the spring valve, as is illustrated in Figure 13.3-3. Based on the tank geometry and the drain-system dimensions (Figure 13.3-3), it was found that the complete lithium inventory can be drained in less than 30 seconds.

Design characteristics of the lithium-drain system are given in Table 13.3-1. The diameter of the drain pipes was selected to be 15 cm in order to avoid flow blockage caused by the high viscosity of the liquid lithium. Four and six drain tanks would be needed, respectively, for the vacuum vessel and confinement building zones; these tanks are sufficiently large to hold the potential lithium inventories from the primary and

Figure 13.3-2. Elevation view of the TITAN-I fusion power core.

Figure 13.3-3. Elevation view of the TITAN-I reactor showing the geometry of the vacuum vessel, confinement building, drain tanks and pipes, and passive fire-retarding valves.

secondary loops. These cylindrical drain tanks are all 4 m in diameter and 9 m long and are spaced underneath the FPC such that they will not interfere with the structural supports of the FPC. The above observations indicate that the design of a fast lithium-drain system for TITAN-I is feasible.

13.3.4. Confinement-Building Cover Gas

The vacuum vessel and all of the primary-coolant circuit of the TITAN-I design are enclosed in an inert cover gas. Different cover-gas options (helium, argon, and nitrogen) were evaluated and the results are summarized in Table 13.3-II.

Helium has the advantage of being chemically inert and will not produce radioactive isotopes. The disadvantages of helium are that it is relatively expensive and it is lighter than air so, unless it is contained, it will not function as a cover gas when it is mixed with air.

The most commonly used cover gas is nitrogen and it is the least expensive of the three. Nitrogen, however, reacts with lithium coolant and forms Li_3N which is a very corrosive compound. For investment protection under minor accident conditions, nitrogen would not be a suitable choice for the lithium self-cooled designs.

Argon is cheaper than helium and heavier than air, yet it has the key disadvantage of being activated by neutrons. The main argon activation-dose contributors are ^{37}S (65%), ^{40}Cl (14%), and ^{41}Ar (12%). Smaller contributors are ^{39}Cl and ^{38}Cl . Neutron activation analysis and release-dose calculations were performed under accident and normal-release conditions for Ar, taking into consideration the TITAN-I configuration (Figure 13.3-2). It was found that the equilibrium concentrations of induced radioactivity in the argon cover gas were too high to satisfy the criteria for accidental and routine releases.

However, the amount of the induced radioactivity in the argon cover gas can be substantially reduced by additional shielding outside the vacuum vessel. For the TITAN-I design, a 1.2-m-thick concrete radiation shield was used. Dose-boundary calculations were performed for accidental release of the entire confinement building atmosphere ($26,000\text{ m}^3$ of argon) at ground level by a catastrophic building failure, assuming a short-term atmospheric-dilution factor of 10^{-6} m^{-3} at 1-km exclusion-area boundary (EAB) based on realistic site meteorological conditions. For these assumptions, the off-site dose is less than 5-mrem whole-body gamma dose, which is small compared to the 10CFR100 limit of 25 rem.

Table 13.3-I.

CHARACTERISTICS OF THE TITAN-I LITHIUM-DRAIN SYSTEM

	Vacuum Vessel	Confinement Building
Lithium Coolant Loops:		
L (m)	22.	22.
d (m)	0.5	0.5
D (m)	2.4	2.4
V_0 (m/s)	20.8	20.8
Li mass (tonne)	212.	278.
No. of heat exchangers	6	6
Discharge time (s)	22.5	35.0
Drain System:		
L (m)	6.	6.
d (m)	0.15	0.15
D (m)	15.	26.
V_0 (m/s)	10.8	10.8
Li mass (tonne)	212.	278.
No. of drain pipes	100	90
Pipe separation (m)	0.47	1.0
Drain time (s)	22.2	32.3
Drain Tank:		
Diameter (m)	4.	4.
Length (m)	9.	9.
No. of tanks	4	6

Table 13.3-II.
CONFINEMENT-BUILDING COVER GASES

Option	Cost (1987 k\$) ^(a)	Comments
Helium	69.0	<ul style="list-style-type: none"> • Lighter than air • Relatively expensive • Nonreactive
Argon	27.5	<ul style="list-style-type: none"> • Heavier than air (Major advantage under Li Fire) • Cheaper than He
Nitrogen	2.8	<ul style="list-style-type: none"> • Forms Li_3N • Cheapest • Li-N reaction ($T_{\text{max}} \sim 830^\circ\text{C}$)

(a) Cost for confinement building volume of 26,000 m³.

For normal operating conditions, the off-site dose is determined by the activity concentrations in the confinement building and the volumetric leakage rate. Assuming a volumetric leakage rate of 1% per day, and a typical average atmospheric dilution at 1 km of $7.6 \times 10^{-7} \text{ m}^3$, the annual whole-body gamma dose can be kept below the 5-rem limit of 10CFR50. Based on these observations and the key advantage of being heavier than air, argon was selected as the confinement-building cover gas for the TITAN-I design.

13.3.5. Tritium Issues

Other safety results from this study concern tritium. At a tritium concentration of 1 appm, the total tritium inventories in the primary coolant loop is 275 g and a similar

amount is in the secondary loop. Since passive drain tanks are used to drain a large fraction of the tritiated lithium, under the worst accidental conditions the amount of possible tritium release will be < 200 g. The tritium inventory in the blanket structure was estimated to be < 10 g, which is acceptable. The tritium leakage rate from the primary loop was estimated to be 7 Ci/d , which is within the 10 Ci/d limit [9].

13.4. LOSS-OF-FLOW & LOSS-OF-COOLANT ACCIDENTS

Two of the major accidents postulated for the FPC are the loss-of-flow accident (LOFA) and the loss-of-coolant accident (LOCA). Demonstration of the level of safety attainable by the power plant requires in-depth analyses of the response of the FPC to these accidents. Thermal responses of the first wall, blanket, and shield of the TITAN-I design to LOFA and LOCA are modeled using a finite-element heat-conduction code. The results of these analyses helped guide the engineering design of the reactor so that the maximum level of safety can be achieved.

The principal concern of these analyses is to estimate the temperature history and peak temperature of the FPC during the accident and to predict the behavior of the structural material at these temperatures. Several temperature limits have been identified for the V-3Ti-1Si alloy and are summarized in Table 13.4-1. The first important limit ($\sim 1300^\circ\text{C}$) corresponds to the onset of volatilization of radioactive products (CaO, SrO) in the vanadium alloy. Although not an immediate safety hazard, simultaneous breach of the vacuum tank and the confinement building would release these products into the biosphere. A more stringent temperature limit is the recrystallization temperature of the vanadium alloy ($\sim 1100^\circ\text{C}$). If the temperature of the structure exceeds this value, then the strength of the material is severely degraded and the torus assembly must be replaced. Details of the recrystallization phenomenon are found in Section 10.2. For the TITAN-I design, the worst-case accident scenario should not result in peak temperatures in excess of 1100°C .

In addition to the above temperature limits, the impact of the thermal transient on the structural material should be considered. During such thermal transients, creep rupture is the dominant mechanism of structural failure. The time-to-failure of the creeping structural material of the FPC should be sufficiently long to ensure that structural integrity of the FPC is maintained and that a small-scale accident would not lead to more serious accident (Section 13.4.3).

Table 13.4-I.
TEMPERATURE LIMITS FOR TITAN-I FPC^(a)

Melting point of the vanadium alloy	1890 °C
Onset of activation product release	~ 1300 °C
Recrystallization of the vanadium alloy	~ 1100 °C
Maximum vanadium temperature during normal operation	750 °C
Boiling point of lithium	1347 °C
Solidification temperature of lithium	181 °C

(a) Time-to-failure by thermal creep rupture is decreased at elevated temperatures.

13.4.1. Accident Models

An elevation view of TITAN-I is shown in Figure 13.4-1. A mid-plane cross section of the first wall, integrated blanket coil (IBC), and hot shield (Figure 13.4-2) shows the repetitive nature of the toroidal and radial cross sections. Therefore, a 2-D geometric model can be used to represent the radial build of the blanket, as is shown in Figure 13.4-3. The coarseness of this finite-element mesh may first appear to be inconsistent with transient-problem analysis. The problem under study, however, is primarily one of heat capacity and heat flow between components which is mainly governed by the radiation between surfaces and, therefore, should be insensitive to the size of the computational elements within the materials.

An alternate 2-D model of the blanket would provide a poloidal/radial build similar to Figure 13.4-1. This model would incorporate the heat conduction along the poloidal flow paths but would not accurately account for the tube-to-tube radiation seen in Figures 13.4-2 and 13.4-3. Since the poloidal conduction path along the first-wall tubes is long (2.09 m) and the initial temperatures are relatively high (700 °C), the radial heat flow during accidents is dominated by thermal radiation.

In order to determine the maximum temperature in the system, the initial and boundary conditions and spatial and temporal distribution of loads are needed. The applied

Figure 13.4-1. An elevation view of the TITAN-I fusion power core.

Figure 13.4-2. Mid-plane cross section of the TITAN-I fusion power core.

Figure 13.4-3. Finite-element model of the TITAN-I fusion power core used in LOFA and LOCA analyses.

boundary conditions include radiation to a constant-temperature sink (behind the shield), as well as internal radiation between various surfaces in the FPC. In LOFA analyses, internal radiation conditions are only applied to the outer surfaces of the tubes, while in the LOCA model, radiation conditions are applied to the internal cavities of the piping as well as to the outer surfaces. Table 13.4-II lists the emissivities and radiation view factors for various locations in the blanket. The view factors are calculated numerically by the code.

The initial temperature of the blanket differs for the LOCA and LOFA. Following the initiation of a LOFA, pump coast down will take place. During the coast-down period, which could last several minutes if the pump has enough inertia, the primary coolant continues to circulate while the plasma is shut down. The continued circulation is assumed to allow the first-wall and blanket streams to mix and equilibrate thermally. Therefore, at the onset of the LOFA analysis, the blanket-coolant temperature is set equal to the mixed bulk temperature of the lithium.

The initial temperature of the blanket for the LOCA analysis depends on several assumptions about the accident scenario. Because of the finite drain time required to empty the primary loop following a pipe break, an instantaneous loss of coolant is unlikely. However, an instantaneous plasma shutdown at the instant of a pipe break is also unlikely. Relatively rapid plasma-shutdown mechanisms have been proposed (Sections 6 and 13.6).

Typical plasma-shutdown times are on the order of a second while the primary-loop drain time is less than 30 s. The longer duration of the coolant drain will allow the removal of heat added by the plasma prior to its shutdown. The temperature of the blanket at the end of the coolant drain period is conservatively set equal to the full-power operating conditions. In reality, coolant draining that continues after the termination of

Table 13.4-II.

EMISSIVITY AND VIEW FACTORS OF TITAN-I FPC

Participating Surfaces	Emissivity	View Factor ^(a)
First-wall tubes to first row of IBC	0.50	0.70
Inside of IBC tubes: front half to back	0.25	0.67
Outside of IBC tubes: between adjacent rows	0.50	0.75
Last IBC tube to hot shield	0.50	0.75
Inside of hot-shield coolant channels (zone 1)		
Front to back	0.25	0.15
Front to one side	0.25	0.43
Zone 1 of hot shield to zone 2 ^(b)	N/A	N/A
Inside of hot-shield coolant channels (zone 2)		
Front to back	0.25	0.20
Front to one side	0.25	0.40
Back of hot shield to sink (OH coils and vacuum tank)	0.50	1.00

(a) Tabulated view factors are based on the average of the view factors for the individual finite-element surfaces associated with the participating surfaces.

(b) The interface between the two zones of the hot shield is a 1-cm metallic felt to provide support for the torus. It is thus treated as a part of the thermal-conduction solution.

the plasma will continue to cool the structure. A more realistic initial LOCA temperature is somewhere between the full-power operating conditions and the bulk temperature of the coolant. Uncertainties in the exact dynamics of the LOCA initiation suggest the use of the conservative initial temperatures (full-power conditions).

The spatial variation of the afterheat at shutdown is shown in Figure 13.4-4. The time dependance of the decay heat in the first wall is shown in Figure 13.4-5. The exclusive use of V-3Ti-1Si throughout the torus produces low levels of decay afterheat. The initial heating rate in the first wall of 2.1 W/cm^3 drops rapidly to less than 1 W/cm^3 in about three hours. The static lithium of the LOFA analysis has no internal heat generation and acts as a heat sink.

Several heat conduction codes are available to model the thermal response of the system during LOCA and LOFA scenarios described above. Finite-element codes such as ANSYS [10], TACO2D [11], and TOPAZ [12] have sufficient flexibility to solve this time-dependant, nonlinear problem. For the safety analysis of the TITAN-I design, TACO2D is used and analytical checks are provided, when possible, to verify the results.

13.4.2. Thermal Response to LOCA and LOFA

The first case studied is that of a complete LOCA. If the results from this case indicate that the blanket will survive a LOCA, then the blanket should also survive a LOFA. The thermal response of the first wall, IBC, and shield are shown in Figure 13.4-6. The peak temperature attained is 1500°C indicating that the TITAN-I blanket will not melt during a LOCA (Table 13.4-1). The temperature, however, exceeds the recrystallization temperature of the vanadium alloy ($\sim 1100^\circ\text{C}$). Although exceeding this temperature does not pose a direct safety hazard, it does require that the torus assembly be replaced subsequent to the accident. The peak temperature of 1500°C , also exceeds the temperature limit of 1300°C for the onset of the volatilization and release of the radioactive products in the vanadium alloy such as CaO and SrO (Table 13.4-1). These products will become mobile within the vacuum tank and, although not an immediate safety hazard, simultaneous breach of the vacuum tank and the containment building will release these products to the biosphere.

The above results do not include the effects of a lithium fire during the LOCA. The lithium-fire calculations are discussed in detail in Section 13.5 and the results then incorporated in the form of prescribed temperature histories during the lithium-burn period. The primary effect of the lithium fire is an increase in the temperature of the

Figure 13.4-4. The level of decay afterheat in the TITAN-I FPC at shutdown.

Figure 13.4-5. The level of decay afterheat in the TITAN-I first wall and divertor plate as a function of time after shutdown.

Figure 13.4-6. The thermal response of the TITAN-I FPC to a complete LOCA as a function of time after the initiation of the accident.

vacuum tank and inner ohmic-heating coils, both of which are the ultimate heat sink for the thermal radiation from the torus assembly. Because of the short duration of the lithium fire (~ 30 s), the corresponding temperature rise in the surrounding structures is small (5°C). The effect of the fire on the first-wall peak temperature is minimal. The first wall is well separated from the back of the torus where the fire is burning, and, therefore, it is fairly insensitive to short-duration events such as the 30-s fire. For these short-duration events, the first wall can be treated as a simple heat-capacity problem, sensitive only to events occurring in the immediate vicinity.

The temperature histories during a LOFA are plotted in Figure 13.4-7. The peak temperature is 990°C and occurs at the first wall, 12.8 hours after accident initiation. This peak temperature satisfies all of the constraints listed in Table 13.4-I. The heat capacity of the static lithium accounts for the moderate temperature excursion. The heat capacity of the lithium in the primary loop, away from the blanket, was not included. No natural convection of the coolant is assumed in the analysis even though the emergency plasma shutdown procedure of Section 13.6 is accompanied by the discharge of all magnets and no MHD retarding force is expected on the coolant. If natural convection develops, the temperature excursions will be considerably smaller than those predicted by Figure 13.4-7.

Material and configuration differences between the divertor and the blanket necessitate additional LOFA analysis for the divertor region. The high initial level of decay afterheat and its longevity relative to the vanadium are a concern. Although the tungsten neutralizer plate is capable of high-temperature operation, the attached vanadium cooling tubes are subject to the same limits as the first wall and blanket. Excessive heating in the divertor structure has the potential to create vanadium-tube failures, hence a local LOCA.

The finite-element mesh used to model the divertor region is shown in Figure 13.4-8. The initial level of afterheat in the tungsten alloy is 10 W/cm^3 and remains relatively constant for 10^4 s . The solid angle for thermal radiation from the divertor plate, across the plasma chamber, and onto the opposite side of the divertor is small; hence, the view factor is set to 1.0, and the temperature boundary condition of the sink is the time-varying first-wall temperature, as is calculated by the first-wall and blanket LOFA analysis. The heating rates in the vanadium structure are the same as those in the first-wall and blanket analysis.

Figure 13.4-7. The thermal response of the TITAN-I FPC to a complete LOFA as a function of time after the initiation of the accident.

Figure 13.4-8. Finite-element model of the TITAN-I fusion power core used in divertor LOFA analysis.

Figure 13.4-9. The thermal response of the TITAN-I FPC to a complete divertor LOFA as a function of time after the initiation of the accident.

The temperature histories of the divertor tungsten armor, divertor-IBC structure, and divertor shield is plotted in Figure 13.4-9. The peak temperature in the vanadium is 1117°C and occurs in the cooling tube behind the front of the divertor plate. The peak temperature is slightly higher than the recrystallization temperature of V-3Ti-1Si ($\sim 1100^{\circ}\text{C}$). Even though the failure of the divertor is unlikely, the strength of the vanadium will be reduced significantly and the divertor module may not be able to operate at full-power conditions after LOFA.

In the TITAN-I design, all of the primary-coolant routings and connections are above the FPC. In case of a major pipe break outside the FPC, the coolant remains in the reactor torus. Even if several coolant tubes inside the FPC (at the lowest point) rupture, the drain time for the coolant would be very long because of the large lithium inventory in the primary loop. Therefore, the possibility of a credible complete LOCA is eliminated. The worst local LOCA scenario envisioned for the TITAN-I design is a complete LOCA in the first-wall circuit and a LOFA in the blanket circuit. The thermal response of the FPC is found to be similar to that of a complete LOFA. Therefore, partial LOCA in the TITAN-I design will not result in any safety concerns.

13.4.3. Elevated-Temperature Creep Rupture

During a LOFA, lithium remains inside the torus assembly and, because of the static head of lithium in the primary loop, a hydrostatic pressure load exists inside the piping (typically 0.1 to 0.2 MPa). The hydrostatic pressure and the elevated temperature of the blanket could lead to failures through thermal-creep rupture. Should this occur, a LOCA situation would develop. An estimate of the time to rupture of the TITAN-I first wall and blanket during a LOFA is provided here.

For a component of a given material, the time to rupture depends on the temperature and stress. The failure analysis of creeping structural materials is made difficult by data shortages for specific materials (*e.g.*, accounting for impurities and heat treatments) in specific stress and temperature ranges, and the process is further complicated by the difficulty of the inelastic stress analysis. Because of the complexity of creep deformation, designers rely heavily on empirical equations to predict creep deformation. For vanadium-base alloys, the expected service life is much longer than the duration for which experimental creep data are available. Extrapolation of the creep data, in particular the creep-rupture data, to longer times have been studied extensively over the past decades [13 - 15].

Widely used extrapolation techniques for creep-rupture data include the Larson-Miller, White-LeMay, and Orr-Sherby-Dorn methods [13]. These methods predict the value of the rupture time, t_r , as a function of the material temperature and for different applied stresses. The choice and success of these methods depends on the behavior of the creep-rupture data and assumed pattern for the data in each method. As a result, it is possible for these methods to predict different values of stresses appropriate to long-life conditions [13]. To overcome this problem, the minimum-commitment method (MCM) was developed at NASA [14] to extrapolate creep-rupture data without forcing the creep data set to any specific pattern. Ghoniem *et al.* [15] developed a modified-minimum-commitment method (MMCM) in which the stress, the time to rupture, and the temperature are related by the following functional form:

$$\ln(\sigma_r) = A(T) + B(T) \ln(t_r), \quad (13.4-1)$$

where σ_r is the stress to rupture in MPa, t_r is the time to rupture in hours, and $A(T)$ and $B(T)$ are temperature-dependent parameters given by

$$A(T) = a_0 + a_1 T \quad \text{and} \quad B(T) = b_0 + b_1 T. \quad (13.4-2)$$

Here, T is the temperature (K) and a_0 , a_1 , b_0 , and b_1 are constants determined by fitting creep-rupture data. The coefficients of Equation 13.4-2 for V-3Ti-1Si and V-15Cr-5Ti are given in Table 13.4-III.

Creep-rupture stress data for vanadium alloys, in particular for V-3Ti-1Si, are limited. Some V-3Ti-1Si creep-rupture stress data [16] are available at 750 and 850 °C. At 650 °C data were found only for a V-3Ti alloy containing unspecified amounts of silicon. The creep-rupture data at these three temperatures are used to develop a phenomenological stress-rupture equation (similar to Equation 13.4-1). A similar equation was also developed for V-15Cr-5Ti using more recent creep-rupture data [17].

Figure 13.4-10 shows the experimental data points [16] and estimates from MMCM for the creep-rupture stress of V-3Ti-1Si at several temperatures as well as the expected stress range in the first wall of TITAN-I during normal and off-normal operation. Based on the results of the MMCM extrapolation equation, operation of the first wall at a pressure close to 100 MPa and at temperatures below 700 °C will not lead to creep rupture within one year of normal operation. During off-normal conditions, coolant pressure is lost and creep rupture would not occur even if the structure is kept at elevated temperatures (1000 °C) for a prolonged period of time. However, high-temperature (> 850 °C) creep-rupture data are necessary to gain more confidence in the creep-rupture behavior at these

Figure 13.4-10. Creep-rupture stresses of V-3Ti-1Si at various temperatures. Symbols are data [16] and solid lines are estimated by MMCM. The expected stress ranges during normal and off-normal operations of the TITAN-I design are also shown.

Table 13.4-III.
COEFFICIENTS FOR THE CREEP-RUPTURE STRESS EQUATION

Coefficient	V-3Ti-1Si	V-15Cr-5Ti
a_0	9.507209	7.239522
a_1	-3.50498×10^{-3}	-1.04844×10^{-3}
b_0	4.85748×10^{-1}	2.17853×10^{-2}
b_1	-6.07858×10^{-4}	-7.38258×10^{-5}

higher temperatures. A more detailed discussion of the creep behavior of the vanadium alloys is given in Section 10.2.

The life-fraction rule (LFR) [18] is used to determine creep-rupture times under transient stress conditions. Several examples of the use of LFR method are given in Reference [19]. The LFR divides the component life into small time steps, dt , and assumes that the fraction of damage incurred by the material during any increment is determined by the ratio of the length of the time step to the rupture time, t_r , measured for the instantaneous temperature and stress during that time step, dt/t_r . When the accumulated damage fraction reaches unity, failure is assumed to occur. This rule, of course, assumes that the damage incurred during a time step is independent of the previously accumulated damage. The integral form of the LFR can be stated as follows:

$$\int_0^{t_f} \frac{dt}{t_r} = 1, \quad (13.4-3)$$

where t_f is the failure time for a transient-stress situation. To compute the failure time, Equation 13.4-1 is solved for the rupture time as a function of temperature and stress and substituted into the integral in Equation 13.4-3. Then, for a given stress history, the failure time, t_f , can be determined.

During normal operation, the thermal gradient in the first wall creates thermal stresses, while the primary stress results from the coolant pressure. After only a few displacements per atom (dpa), irradiation creep will relax the thermal component of the

stress and only the primary stress of the pressure will remain. During a LOFA, the primary stress is significantly reduced because the coolant pressure is lost. On the other hand, the thermal gradient is removed and a residual stress of opposite sign to the initial thermal stresses will develop. Hence, the stress field during a LOFA consists of a small primary stress, caused by the hydrostatic pressure load inside the piping, and an initial thermal stress of the same order as the operating thermal stress. If the LOFA occurs before the operating thermal stresses are completely relaxed, the stresses during the accident will be lower and failure will occur later.

During the thermal transient of an accident, the stress fields in both the first wall and blanket feature a maximum stress of the form

$$\sigma = \sigma_p + f(\sigma_o, t), \quad (13.4-4)$$

where σ_p is the primary stress and $f(\sigma_o, t)$ is a decay function representing the time dependence of the initial thermal stress, σ_o . In the TITAN-1 first wall, the pressure stress is about 1 to 2 MPa during LOFA and the initial thermal stress is about 200 MPa. In the blanket, the pressure stresses are of the same order, the thermal stresses are much smaller, and the temperatures are lower; the blanket, therefore, is expected to have a longer lifetime than after the first wall.

The decay function, $f(\sigma_o, t)$, depends on the creep-rate equation exhibited by a particular material. Typically, creep-rate equations are given in the form

$$\dot{\epsilon}_c = C' \sigma^n, \quad (13.4-5)$$

where $\dot{\epsilon}_c$ is the creep rate, C' is a constant, and n is the creep exponent (both C' and n are temperature dependent). When $n = 1$, the creep rate is proportional to the stress, σ , and the decay function is given by:

$$f(\sigma_o, t) = \sigma_o e^{-E C' t}, \quad (13.4-6)$$

while for $n > 1$,

$$f(\sigma_o, t) = \left[\sigma_o^{1-n} - (1-n) E C' t \right]^{1/(1-n)}, \quad (13.4-7)$$

where σ_o is the initial stress and E is the modulus of elasticity. For V-Ti alloys, the creep exponent, n , drops below 2 above $\sim 800^\circ\text{C}$ [16], so a reasonable approximation would be to use Equation 13.4-6 for the decay function. Substituting Equation 13.4-6 in Equation 13.4-4, and applying the resultant stress history to Equation 13.4-1 will result

in the rupture time, t_r , as a function of the instantaneous temperature and stress. Then, Equation 13.4-3 can be used to find the time to failure, t_f .

For pure thermal stresses ($\sigma_p = 0$), the stress, σ , decays exponentially from the initial thermal stress and the time to failure would be:

$$t_f = \frac{B}{EC} \ln \left[1 + \frac{EC}{B} \left(\frac{\sigma_o}{\epsilon^A} \right)^{1/B} \right], \quad (13.4-8)$$

where A and B are given by Equation 13.4-2. Because B is negative in the temperature range of interest, the right-hand side of Equation 13.4-8 is positive only if

$$C > -\frac{B}{E} \left(\frac{\epsilon^A}{\sigma_o} \right)^{1/B}, \quad (13.4-9)$$

which implies that rupture will not occur if the creep rate is sufficiently high to relax the stress before rupture occurs. For the TITAN-I first wall, pure thermal stresses will not cause failure at the expected LOFA temperature of $\sim 1000^\circ\text{C}$ if the creep coefficient C is greater than $10^{-5}/\text{h-MPa}$. Since the creep rate of V-Ti alloys at 1000°C is, conservatively, $1\%/h$ at stresses above 100 MPa [16], the creep coefficient of these alloys at 1000°C should be greater than $0.005/\text{h-MPa}$, much larger than the limiting value from Equation 13.4-9. Therefore, the TITAN-I first wall would not fail under pure thermal stresses. This result is apparent if one considers the creep strain which develops during total relaxation of a 200-MPa thermal stress, as compared to the rupture strain at 1000°C . For unirradiated V-Ti alloys with Ti contents of 3% to 5%, the creep-rupture strain is well above 10% [16], whereas the total creep strain caused by relaxation of a 200-MPa thermal stress is only 0.17%. Therefore, the creep ductility is well above the strain produced and failure will not occur unless significant primary stresses are also present.

Because primary stresses are not relaxed by creep, they are much more likely to cause creep rupture. Considering creep rupture caused by only the primary stresses (the stress and temperature are constant in time), Equation 13.4-1 can be used for rupture time directly. The predicted rupture time for several stresses is given in Table 13.4-IV.

When combined pressure and thermal stresses are considered, Equation 13.4-3 must be solved numerically. For the materials and loadings expected in the TITAN-I first wall during a LOFA, we have found that the thermal stresses have a negligible influence on the rupture time, relative to the pressure stresses, so the results of Table 13.4-IV for pure pressure stresses is used to estimate the rupture and failure times. Table 13.4-IV shows that expected primary stress in the TITAN-I design during a LOFA (2 MPa) would not result in any failure in the first wall.

Table 13.4-IV.
CREEP-RUPTURE TIME FOR TITAN-I FIRST WALL

Primary Stress, σ_p (MPa)	Rupture Time, t_r (h)
10	3200
20	360
30	101
40	41
50	20

In some cases, depending on the fracture mode, irradiation can reduce the creep-rupture strain (ductility). In 316 SS for instance, intergranular fracture which occurs at high temperature (811-922 °C), and transgranular fracture which occurs at lower temperatures, are affected differently by neutron damage. Irradiation produces helium which migrates to the grain boundaries and reduces the intergranular fracture strength. Therefore, irradiation of 316 SS decreases the strain to failure in the high temperature (intergranular fracture) regime from $> 8\%$ to $\sim 2\%$, but irradiation does not affect the transgranular fracture ductility [20]. In vanadium, intergranular fracture has not been observed [16], even for long rupture times, so irradiation may reduce the creep ductility in all temperature regimes to a few percent. Without specific data it is impossible to estimate the rupture time of an irradiated first wall, but if the ductility is not reduced below 1% to 2%, the thermal stresses will still be incapable of producing rupture, according to the arguments put forth previously.

The decrease in the rupture time for primary stresses is more difficult to estimate. In 316 SS, the rupture time, for a given stress and temperature, is reduced by about an order of magnitude after irradiation of 9.1×10^{22} n/cm² [21], which corresponds to about 40 dpa. A similar reduction in vanadium would still not be a problem for primary stresses of 2 MPa expected during a LOFA. Hence, creep rupture is still not expected to occur in the TITAN-I first wall or blanket during a LOFA, even after significant irradiation.

13.4.4. Conclusions

The thermal analysis indicates that the TITAN-I first wall will exceed the prescribed temperature limits during a complete LOCA. As a result, the primary-lithium piping arrangement has been chosen so that a complete LOCA is improbable. A complete LOCA is one in which the entire primary loop, including the torus, is drained of coolant. The worst-case accident scenario is then a double-ended pipe break which spills all of the lithium in the primary loop that is above the height of the break. The 30-s residence time of the lithium on the floor and the ensuing lithium fire compound the consequences. The reactor torus, being the lowest point in the primary loop, remains filled with lithium. Therefore, LOFA conditions within the torus are the governing heat-transfer mechanisms.

During the LOFA (and a lithium fire) the first-wall temperature rises to 990°C, about 300°C above the normal, full-power condition. This moderate temperature excursion occurs because of the low level of afterheat in the vanadium alloy (V-3Ti-1Si), the exclusive use of this alloy throughout the torus assembly, and the heat capacity of the static lithium. The peak temperature is well within the limits for both structural integrity and safety. The hydrostatic pressure of the static lithium during a pure LOFA is not sufficient to overstress the blanket and lead to thermal creep-induced rupture and, hence, LOCA conditions in the reactor torus.

In the TITAN-I design, all of the primary-coolant routings and connections are above the FPC, eliminating the possibility of a credible complete LOCA. The worst local LOCA scenario envisioned for the TITAN-I design is a complete LOCA in the first-wall circuit and a LOFA in the blanket circuit. The thermal response of the FPC is found to be very similar to that of a complete LOFA. Therefore, a partial LOCA in the TITAN-I design would not result in safety concerns.

Higher afterheat is expected in the tungsten plate of the divertor. During a LOFA, the peak temperature in the divertor vanadium cooling tube is 1117°C. This may result in shortening the lifetime of the divertor modules, but failure that would lead to a LOCA is unlikely.

13.5. LITHIUM-FIRE ACCIDENTS

The use of high-temperature liquid-lithium as the primary coolant of TITAN-I results in LOFA and LOCA scenarios that are somewhat different from those of water- and gas-cooled systems. The principle difference is the possibility of a lithium spill leading to a

lithium fire. Unless safety features are implemented, the energy content of the lithium fire will contribute significantly to the peak temperature of the torus assembly. The reactor has to be designed to (1) ensure that a lithium fire would be a low probability event, and (2) minimize the consequences of lithium fire should it occur.

The consequences of a lithium fire are slight for the TITAN-I design because of the use of floor drains and drain tanks in the confinement building and vacuum tank. Following the design guidelines of Section 13.3.1, three barriers exist between the primary lithium coolant and air. These barriers are the primary-coolant pipes, the vacuum tank, and the confinement building, as shown in Figure 13.3-1. To further reduce the probability of a lithium fire, argon cover gas is used in the confinement building as presented in Section 13.3.4. In addition, in order to reduce the consequences of a lithium spill, two sets of lithium-drain tanks are used: one set connected to the vacuum tank and one set to the confinement building. These drain tanks are designed such that the complete inventory of lithium can be drained to either tank system in < 30 s (Section 13.3.3). This fast draining design has significant impact on minimizing the consequences of major lithium-fire accidents.

Based on the TITAN-I configuration illustrated in Figure 13.3-2, the consequences of three different lithium-fire scenarios were evaluated. These scenarios differ by their initial conditions. Scenario 1: the confinement building is filled with air and the vacuum tank with argon, and air will enter and argon will leave the vacuum-tank through an 0.4-m^2 opening (corresponding to the total cross-sectional area of two primary-coolant pipes). This scenario is a credible major accident when a double-ended primary-pipe break occurs on the vacuum tank and the argon cover gas flows in and fills the vacuum tank. Also, it is assumed that the confinement building has failed and the air will enter into the confinement building and then diffuse into the vacuum tank.

Scenario 2: the confinement building is filled with air and the vacuum tank is in vacuum, with air flowing in through a 0.4-m^2 hole into the vacuum tank. This scenario is possible, but is less likely than the first scenario. This scenario corresponds to a case where the confinement building cover gas is completely replaced by air because of a major explosion or projectile penetration (e.g., aircraft penetration). Scenario 3: the confinement building and the vacuum tank both are filled with air (worst case). This scenario is not considered a credible accident, since both the confinement building and the vacuum tank would need to be penetrated at the same time and the argon cover gas to be blown away.

The above three lithium-fire accidents were analyzed with the LITFIRE code [22]. A two-cell model, which includes the vacuum tank and the confinement building, was used

in the calculations. It is assumed that the entire primary-loop inventory of 212 tonnes is spilled into the vacuum tank for our worst-case calculations. Other assumptions made in the calculations are: a 5-cm-thick vacuum-tank wall, a 1-mm-thick stainless-steel liner on the floor of the confinement building, and an initial lithium temperature of 495 °C (mixed-lithium operating temperature).

Figure 13.5-1 shows the temperature response of the combustion zone and the mass-flow rate of the opening in the vacuum tank as a function of time, for the three scenarios. For scenario 1 ("Argon"), the vacuum tank is initially filled with argon gas, but the gradual influx of air into the vacuum tank (Figure 13.5-1) initiates the lithium fire and the temperature increases with time. For scenario 2 ("Vacuum"), the vacuum tank is initially in vacuum and air would flow into the tank at the beginning and start the lithium fire. As the lithium fire intensifies, the mass flow through the vessel opening actually reverses and becomes an outward flow (Figure 13.5-1). For scenario 3 ("Air"), the vacuum tank is initially filled with air. The temperature of the combustion zone rises with time until the burning rate is so high that more gas (air and combustion products) is driven out of the vessel than there is air coming in. At this point, the lithium combustion-rate decreases (not enough air) resulting in a decrease in the combustion-zone temperature (Figure 13.5-1). The maximum combustion-zone temperatures are found to be less than 500, 750, and 1000 °C, respectively, for the cases of argon-, vacuum-, and air-filled vacuum tank initial conditions.

Figure 13.5-2 shows the combustion-zone temperature for accident scenario 2 (vacuum tank in vacuum) as a function of time, for different tank-opening sizes and different masses of spilled lithium. It is evident that the combustion-zone temperature is not very sensitive to the opening sizes considered. Also, as the quantity of lithium dumped instantaneously into the vacuum tank is reduced, the maximum combustion-zone temperature is much reduced.

For the three scenarios, it was found that the maximum amount of burned lithium would be 1.6 kg. At a tritium concentration of 1 appm in the lithium coolant of the primary circuit, the maximum release of tritium in this accident is 6.1×10^{-3} g, which is well below the accidental release limit of 200 g [23] to cause a site-boundary dose of less than 25 rem. It should be also be noted that when air is in contact with the hot vanadium structure, V_2O_5 is formed which melts at 670 °C [24]. This can lead to the release of radioactivity from the first-wall and blanket structure. The significance of this reaction and subsequent release of radionuclides should be evaluated.

It can be concluded that under the perceived worst-accident condition of having the vacuum tank initially filled with vacuum, the maximum lithium combustion-zone

Figure 13.5-1. The combustion-zone temperature (A) and the mass-flow rate through the opening in the vacuum tank (B) of TITAN-I as a function of time after the lithium spill for the three different accident scenario: the vacuum tank is filled with argon, in vacuum, and filled with air. The confinement building is assumed to be filled with air.

Figure 13.5-2. The combustion-zone temperature of a lithium spill in TITAN-I as a function of time after the spill for different opening sizes in the vacuum tank (A) and different masses of spilled lithium in the vacuum tank (B). The tank is initially in vacuum.

temperature will be $< 1000^{\circ}\text{C}$. Considering the built-in safety feature of the drain tank (drainage of all lithium within 30 s), the maximum temperature can actually be controlled to $< 750^{\circ}\text{C}$, which is within the reuse temperature limits of the structural materials of vanadium, stainless steel, or ferritic steel.

13.6. PLASMA ACCIDENTS

In RFPs and tokamaks, in addition to the plasma thermal energy, a significant amount of energy is stored in the poloidal magnetic field. At full operational conditions, the stored energy in TITAN-I plasma includes 0.1 GJ of kinetic (thermal) energy and $W_M \sim 4.3$ GJ of magnetic energy (~ 5.2 GJ for ohmic-heating coils in full forward bias). The magnetic energies internal to the plasma are 0.3 MJ in the toroidal field and 0.4 GJ in the poloidal field. The magnetic energies outside the plasma are < 2 MJ in the toroidal field and 3.6 GJ in the poloidal field. Any rapid release of these stored energies (e.g., similar to disruptions in tokamaks) may lead to severe consequences.

Operating RFP experiments usually end with a "current termination" phase where the plasma current is rapidly reduced to approximately zero. Current termination is characterized by the loss of toroidal-field reversal and is accompanied by a positive voltage spike (as opposed to a negative voltage spike for tokamak disruptions) and large density and magnetic-field fluctuations. A number of variables, such as plasma density, toroidal-field reversal, magnetic-field errors, and impurities appear to affect RFP terminations. A complete and satisfactory explanation of RFP current terminations is not yet available. Evidence, however, suggests that the onset of termination may be related to a loss of density possibly leading to a streaming parameter that exceeds a critical value for runaway electrons. Since the value of streaming parameter for TITAN plasma is only a percent of the critical value for runaway electrons, a current termination is not expected during normal, steady-state operation of the TITAN reactor, rather only failure of plasma support technologies leading to an uncontrolled ramp-down of the plasma current will result in a current termination.

A method of controlled current ramp-down has been tested on the HBTX1B experiment in which the toroidal-field-coil circuit is controlled so that the pinch parameter (and the field reversal) is maintained at a given value as the current is decreased to a relatively low level [25]. Maintaining the field reversal in this way is found to delay termination, and the current can be reduced to between 10% to 20% of the maximum value (and the stored magnetic field energy reduced to 1% to 4% of the maximum value) before the

termination occurs. Controlled ramp-downs of this kind forestall the loss of toroidal-field reversal as long as possible and are required for the reactor.

During normal, steady-state operation of the TITAN reactors, the following plasma support technologies are operational: (1) fueling, (2) current-drive, (3) toroidal-field, (4) divertor-field, and (5) equilibrium-field systems. Consequences of failure of each of these systems are discussed below.

Fueling system. The effective particle confinement time in the TITAN plasma is very long (a few seconds) because of the operation with high-recycling divertors. The standard shutdown scenario envisioned for the TITAN plasma starts with the termination of plasma fueling and current divertor operations. Therefore, the loss of fueling system (and vacuum pumping) will not initiate an accident and the standard plasma shutdown scenario will be implemented.

Current-drive system. Once the current drive fails, the plasma current will resistively decay with time scales of $L_p/R_p \simeq 200$ to 400 s. As for the case of the failure of the fueling system, the standard plasma shutdown scenario can be implemented here which will prevent any accidents.

Toroidal-field system. The toroidal-field (TF) coils provide the reversed toroidal field at the plasma edge. If the TF-coil system (power supplies) fail, the RFP dynamo activity would increase to generate the necessary toroidal flux, resulting in a decay of the plasma current. An emergency plasma shutdown is recommended in this case even though the decay time for the plasma current is probably long enough that a standard shutdown would be possible.

Divertor-field system. The TITAN reactors operate with three toroidal-field divertors. If any of the divertor coils fail, the reactor can still be operated at reduced power levels. Failure of all three divertors will result in the plasma "riding" on the first wall in a limiter mode. Since the first wall is designed to handle about 95% of the plasma heat flux (if distributed uniformly on the first wall), a standard shutdown process probably suffices to avert any accidents.

Equilibrium-field system. The TITAN reactors use two sets of equilibrium-field (EF) coils: (1) a pair of superconducting magnets provide the main equilibrium field, and (2) a pair of small normal-conducting "trim" coils provide the exact equilibrium (for feedback control of plasma position and for OFCD cycles). With a loss of control on plasma equilibrium and position, not only can the plasma energy be deposited locally, but also the plasma current will decrease rapidly, usually leading to a disruption or current termination. The failure of the equilibrium-field system, therefore, appears to be the most severe plasma-related accident for the TITAN reactor (and for any current-carrying toroidal system).

13.6.1. Shutdown Procedures

The TITAN plasma shutdown procedures are guided by the above observations to ensure (1) plasma current is reduced through a controlled ramp-down in order to forestall current termination, (2) plasma equilibrium is maintained during current ramp-down, (3) failure of the equilibrium-field system (*i.e.*, quench of the superconducting EF coils) will automatically lead to an emergency shutdown, and (4) most of the magnetic energy stored in the plasma is removed during the shutdown.

The plasma shutdown scenarios envisioned for the TITAN plasma, therefore, start with termination of fueling and current-drive operations and simultaneous discharge of the EF coils. For the standard shutdown procedure, the duration of the EF-coil discharge can be of the order of a few to tens of seconds. During the emergency shutdown procedure, however, the EF coils are discharged rapidly (~ 0.1 s) through a resistor which can be combined with the quench protection system for the EF coils. Therefore, failure of the equilibrium-field system will automatically initiate the emergency shutdown procedure.

Because of the strong magnetic coupling between the plasma and EF and OH coils in TITAN, a fast discharge of the EF coils results in a rapid decrease in the plasma current. The parameters of EF and OH circuits, however, are chosen such that the plasma equilibrium is approximately maintained during this discharge without any need for an equilibrium-control system. The large time constant of the IBC TF coils for field penetration is also utilized to ensure maintenance of the field-reversal during the shutdown similar to the controlled current ramp-down technique. Preliminary simulations of the TITAN emergency shutdown procedure (Section 6) indicate that most of the stored magnetic energy (4 to 5 GJ) is removed from the system and dumped through the discharge resistor. Only about 200 MJ of energy is transferred to the first wall in a time scale of

50 to 100 ms, resulting in an average temperature rise in the first wall of about 300 °C; failure of the first wall is not expected.

Despite these favorable results, the RFP theoretical and experimental data base is not very extensive. In particular, no experimental data on high-current, high-temperature, diverted RFP plasmas exist. Furthermore, a complete and satisfactory explanation of current termination in RFPs is not yet available. The safety impact of plasma accidents, therefore, should be further investigated and the shutdown procedures, such as those envisioned for the TITAN plasma, should be experimentally explored.

13.7. RADIOACTIVE-WASTE DISPOSAL

13.7.1. Radioactive-Waste-Disposal Issues

The classification of nuclear waste for disposal is given under the Code of Federal Regulations, specifically 10CFR61 [1]. Four waste classes have been defined: Class A (segregated waste), Class B (stable waste), Class C (intruder waste), and geologic waste. The first three classes of waste are eligible for near-surface burial, while the last class needs deep geologic burial. Radionuclides with half-lives less than five years will decay by at least six orders of magnitude in 100 years after disposal. These radionuclides can be reasonably managed to meet either Class-A or Class-B disposal requirements. For long-lived radionuclides with half-lives greater than 100 years, however, it will be difficult to meet either Class-A or Class-B disposal requirements solely by radioactive decay to reduce the activity level. To qualify as Class-C or better nuclear waste, the nuclear components in a fusion reactor should minimize the quantity of their alloying and/or impurity elements that would produce long-lived radionuclides.

The limiting-specific activities for near-surface (Class A, B, and C) disposal of nuclear waste were also specified in 10CFR61 regulations for the following radionuclides: ^{59}Ni , ^{94}Nb , ^{99}Tc , ^{129}I , ^{90}Sr , ^{137}Cs , and alpha-emitting transuranic nuclides with half-lives greater than five years. These limiting-specific activities were given primarily for radionuclides relevant to present-day applications such as fission-based nuclear reactors. Many radionuclides with half-lives greater than five years, such as ^{42}Ar (half-life 33 y), $^{108\text{m}}\text{Ag}$ (127 y), and $^{186\text{m}}\text{Re}$ (2×10^5 y), may be produced by fusion reactors in the elements constituting the structural alloy (e.g., vanadium), the divertor collecting plates (e.g., tungsten), and impurities in the structural alloys. However, the limiting-specific activities for near-surface disposal of nuclear waste containing these nuclides are not available in 10CFR61.

Evaluations of limiting-specific activities for fusion-relevant nuclides were made based on 10CFR61 values. A complete work was recently performed by Fetter [26] providing limiting-specific activities for near-surface disposal of all radionuclides with atomic numbers less than 84. These evaluations, consistent in methodology with the 10CFR61 regulations, were used in the waste-disposal analysis of the TITAN-I reactor. Note that discrepancies may exist between Fetter's evaluations and 10CFR61 or other estimates. The major discrepancy occurs with the nuclide ^{63}Ni for which Fetter's evaluation of the allowable limiting-specific activity is greater than the 10CFR61 limit by more than two orders of magnitude. Also, several nuclides are not given by 10CFR61 evaluations or other estimates: ^{39}Ar , ^{152}Eu , $^{192\text{m}}\text{Ir}$, ^{137}La , $^{186\text{m}}\text{Re}$, $^{121\text{m}}\text{Sn}$, and ^{158}Tb . These nuclides are now covered by Fetter's evaluations. Comparison of these evaluations and resolution of any discrepancies should be carried out as the development of fusion-energy technologies progresses.

13.7.2. Radioactive-Waste-Disposal Ratings of TITAN-I Reactor

The neutron spectrum at the first wall, IBC, shield, and ohmic-heating (OH) coils of the TITAN-I design are given in Table 13.7-1. The fast-neutron fraction of the spectrum decreases in the shield and magnets because of enhanced neutron moderation. The increase of the relative low-energy neutron population in these components will result in a higher production rate of long-lived radionuclides such as ^{94}Nb which depend on (n,γ) reactions.

The neutron fluxes calculated for the reference TITAN-I reactor were used as the input to the activation calculation code, REAC [27]. These results were analyzed to obtain the allowable concentrations of alloying and impurity elements in the vanadium-alloy structural material of the TITAN-I FPC (with a number density of 7.22×10^{22} atoms/cm³), and in the OH magnets. At any locations in the TITAN-I FPC, the reaction products of the two main alloying elements in the V-3Ti-1Si alloy (vanadium and titanium) would have no limits on their concentrations to qualify as Class-C waste. The other alloying element, silicon, also poses no problem for the Class-C disposal of the vanadium alloy since its allowable concentration (23%) is much more than needed in this alloy (1%).

The allowable concentrations of various impurities in the vanadium structural material of the TITAN-I reactor are listed in Table 13.7-II. The impurity elements and their levels in V-3Ti-1Si alloy are not specifically given in the literature. The information compiled in Reference [9] for V-15Cr-5Ti was used for comparison in this study and is also noted in Table 13.7-II which shows that Nb (4-ppm nominal concentration [9]) is probably the

Table 13.7-I.
NEUTRON SPECTRUM IN THE TITAN-I FPC

Component ^(a)	Neutron Flux (n/cm ²)	Flux-Reduction Factor ^(b)	Fast-Neutron Fraction ^(c)
First wall & IBC	4.4×10^{15}	—	82%
Hot shield (zone 1)	1.5×10^{15}	2.9	70%
Hot shield (zone 2)	4.6×10^{14}	3.3	52%
OH coils	1.7×10^{14}	2.7	43%

(a) Flux values at the front side of each component.

(b) Ratio of flux in the front of the component to that in the back.

(c) For $E_n \geq 0.1$ MeV.

only impurity element that has to be controlled for the shield to qualify as Class-C waste. Also, the allowable impurity concentrations of Nb in the shield (~ 1 appm) and in the first wall and blanket (~ 5 appm) are lower than the Nb impurity levels cited in Reference [9]. Certain elements that are not listed in Reference [9] but require strict concentration limits (1 appm or less) for Class-C disposal are Ag, Ir, and Tb. Table 13.7-II indicates that with the nominal impurity levels, the Tb and Ir concentrations in the first wall, blanket, and shield of the TITAN-I reactor may exceed the limit for Class-C disposal. Therefore, special attention should be given to controlling the concentration of these impurity elements.

Because of the low-sputtering and high-temperature properties, tungsten divertor plates are used. The allowable concentration of tungsten in the divertor plates, located mostly close to the plasma, is about 5% (Table 13.7-II). If the divertor plates of the TITAN-I design are disposed of as a unit (mixing the tungsten armor with the vanadium-alloy cooling tubes of the divertor plate), the tungsten concentration in the divertor plates is about 50%, thus giving a factor of 10 higher than the limit for Class-C waste disposal. However, the quantity in this category (non-Class C or geologic) to be disposed annually is small (~ 0.35 tonne or ~ 0.03 m³ by volume). Except for the divertor plates, all other

TITAN-I components are classified as Class-C or better nuclear waste if the impurity elements (mainly niobium) are controlled below the allowable limits.

Table 13.7-III summarizes the TITAN materials and related quantities for Class-C disposal. As shown in Table 13.7-III, the total weight of TITAN-I FPC is ~ 1363 tonne. The weight of the magnet system (OH and equilibrium-field (EF) coils, and EF shield) is $\sim 73\%$ of the total weight of the FPC. The magnet system has a 30-FPY lifetime and disposal (or recycling) is needed only at decommissioning of the power plant. The

Table 13.7-II.

**MAXIMUM CONCENTRATION LEVELS^(a) OF IMPURITIES IN
TITAN-I REACTOR COMPONENTS TO QUALIFY AS CLASS-C
WASTE**

Element	Major Nuclide (Activity Limit) ^(b)	Components			Nominal Level
		FW & Blanket (1 FPY) ^(c)	Hot Shield (5 FPY) ^(c)	OH Coils (30 FPY) ^(c)	
Nb	⁹⁴ Nb (0.2 Ci/m ³)	5.	1.4	0.5	0.1
Mo	⁹⁹ Tc (0.2 Ci/m ³)	65.	100.	90.	1.0
	⁹⁴ Nb (0.2 Ci/m ³)				
Ag	^{108m} Ag (3 Ci/m ³)	1.3	1.5	0.7	1.0
Tb	¹⁵⁸ Tb (4 Ci/m ³)	0.4	0.6	7.0	5.0
Ir	^{192m} Ir (2 Ci/m ³)	0.1	0.1	0.02	5.0
W	^{186m} Re (9 Ci/m ³)	5%	9%	100%	0.89%

(a) Concentrations in appm except as noted.

(b) From Reference [27].

(c) Based on operation at 18 MW/m² of neutron wall loading.

Table 13.7-III.

SUMMARY OF TITAN-I REACTOR MATERIALS AND RELATED
WASTE QUANTITIES FOR CLASS-C WASTE DISPOSAL

Component	Material	Lifetime (FPY) ^(a)	Volume (m ³)	Weight (tonne)	Annual
					Replacement Mass (tonne/FPY)
First wall	V-3Ti-1Si	1	0.4	2.5	2.5
Blanket (IBC)	V-3Ti-1Si	1	6.4	39.2	39.2
Shield (zone 1)	V-3Ti-1Si	5	15.5	95.6	19.1
Shield (zone 2)	V-3Ti-1Si	5	28.0	172.0	34.4
OH coils	Modified steel	30	3.8	34.0	1.1
	Copper		26.6	239.0	8.0
	Spinel		3.8	15.2	0.5
	TOTAL		34.2	289.2	9.6
EF coils	Modified steel	30	43.0	315.0	10.5
EF shield	Modified steel	30	43.9	347.0	11.6
	B ₄ C		18.8	47.0	1.6
	TOTAL		62.7	394.0	13.2
Divertor shield					
zone 1	V-3Ti-1Si	1	2.3	14.2	14.2
zone 2	V-3Ti-1Si	5	6.7	41.2	8.2
TOTAL CLASS-C WASTE			199.	1363.	151.

(a) Based on operation at 18 MW/m² of neutron wall loading.

components to be replaced annually are first wall, blanket, and front part of the divertor shield, and constitute only 4% of the total weight of the FPC. The balance of the weight is contributed by the 5-FPY-lifetime components (*i.e.*, hot shield and the rear part of the divertor shield). The average annual-replacement mass of the FPC is about 150 tonne/FPY (Table 13.7-III).

13.7.3. Conclusions

The waste-disposal analysis has shown that the compact, high-power-density TITAN-I reactor can be designed to meet the criteria for Class-C waste disposal. The key features for achieving Class-C waste in the TITAN-I reactor are attributed to: (1) materials selection, and (2) control of impurity elements. The materials selected for the TITAN-I reactor are vanadium alloy (V-3Ti-1Si) and lithium for the FPC. The main alloying elements of these materials do not produce long-lived radionuclides with activity levels exceeding the limits for Class-C disposal. The impurity elements, mainly niobium and possibly silver, terbium, and iridium, need to be controlled in the vanadium alloy below appm levels.

The average replacement mass was estimated to be 150 tonne/FPY ($\sim 11\%$ of total FPC). The divertor plates are fabricated with a tungsten armor because of its sputtering properties. The waste-disposal rating of the divertor plates is estimated to be a factor of 10 higher than for Class-C disposal after one year operation. The annual disposal mass of this non-Class-C waste is 0.35 tonne/FPY ($\sim 0.23\%$ of the FPC replacement mass).

The conclusions derived from the TITAN-I reactor study are general, and provide strong indications that Class-C waste disposal can be achieved for other high-power-density approaches to fusion. These conclusions also depend on the acceptance of recent evaluations of limiting-specific activities carried out under 10CFR61 methodologies.

13.8. SUMMARY AND CONCLUSIONS

Strong emphasis has been given to safety engineering in the TITAN study. Instead of an add-on safety design and analysis task, the safety activity was incorporated into the process of design selection and integration at the beginning of the study. The safety-design objectives of the TITAN-I design are: (1) to satisfy all safety-design criteria as specified by the U. S. Nuclear Regulatory Commission on accidental releases, occupational doses, and routine effluents; and (2) to aim for the best possible level of passive safety assurance.

The key safety features of the lithium self-cooled TITAN safety design are:

- The selection of a low-afterheat structural material, V-3Ti-1Si;
- The selection of a relatively high ^6Li enrichment (30%) to aid in further reducing afterheat and radioactive wastes;
- The use of three enclosures separating the lithium and air: the blanket tubes, vacuum vessel, and confinement building which is filled with argon cover gas;
- Locating all coolant piping connections at the top of the torus to prevent a complete loss of coolant in the FPC in case of a pipe break;
- The use of lithium-drain tanks to reduce the vulnerable lithium inventory should a pipe break occur;
- The use of steel liner to cover the confinement-building floor to minimize the probability of lithium-concrete reaction;
- The exclusion of water from the confinement building and vacuum vessel to prevent the possibility of lithium-water reaction.

With the above design features, we found that during LOFA, the first wall temperature rises to a maximum of 990°C , which is well below the recrystallization temperature of the V-3Ti-1Si alloy. We also found that the hydrostatic pressure of the lithium is not sufficient to over-stress the first wall and blanket and lead to thermal creep-induced rupture. Therefore, it is not expected that a LOFA will lead to a LOCA. Also, a complete LOCA is not credible because all the piping connections are located above the reactor torus.

In the event of major primary-pipe breaks and failure of the confinement building and vacuum vessel, air could enter the vacuum chamber and start a lithium-fire. Drain-tank systems are provided to drain the maximum amount of lithium in less than 30 s, and we found that during the perceived worst-accident condition of a lithium fire, the maximum combustion-zone temperature is less than 1000°C . The tritium release in this case would be about 60 Ci which is quite acceptable under this worst-accident scenario. A critical concern under the lithium-fire scenario is the formation and release of vanadium oxide, V_2O_5 . Further measurement of vanadium-oxide formation and its vapor pressure with temperature, and the calculation of potential releases to the public based on the TITAN-I configuration and accidental scenarios should be performed.

The total tritium inventories in the lithium primary loop and the secondary loop are 344 and 300 g, respectively. These inventories are acceptable when passive drain tanks are used to control the amount of possible tritium releases. The tritium inventory in the blanket structure is less than 10 g, which is also acceptable. The tritium-leakage rate from the primary loop was estimated to be 7 Ci/d which is within the 10 Ci/d design goal.

Plasma-accident scenarios need to be further evaluated as the physics behavior of RFPs becomes better understood. Preliminary results indicate that passive safety features can be incorporated into the design so that the accidental release of plasma and magnetic energies can be distributed without leading to major releases of radioactivity. Activities in this area need to be continued, especially for high-power-density devices.

Based on the analyses summarized above, TITAN-I does not need to rely on any active safety systems to protect the public. A LOFA will result in no radioactive release and will not lead to a more serious LOCA. A complete LOCA from credible events is not possible. Only the assurance of coolant-piping and vacuum-vessel integrity is necessary to protect the public. The TITAN-I design, therefore, meets the definition of level 3 of safety assurance, "small-scale passive safety assurance." Pending information on the vanadium-oxide formation and releases from the TITAN-I vacuum chamber under the lithium-fire accident scenario, the qualification of TITAN-I as a level-2 of safety assurance design, "large-scale passive safety assurance," may also be possible.

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14. TITAN-J MAINTENANCE PROCEDURES

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14. TITAN-I MAINTENANCE PROCEDURES

14.1. INTRODUCTION

The TITAN reactors are compact, high-power-density designs. The small physical size of these reactors permits each design to be made of only a few pieces, allowing a single-piece maintenance approach [1,2]. Single-piece maintenance refers to a procedure in which all of components that must be changed during the scheduled maintenance are replaced as a single unit, although the actual maintenance procedure may involve the movement, storage, and reinstallation of some other reactor components. In TITAN designs, the entire reactor torus is replaced as a single unit during scheduled maintenance. Furthermore, because of the small physical size and mass of the TITAN-I FPC, the maintenance procedures can be carried out through vertical lifts, allowing a much smaller reactor vault. The advantage of using fully toroidal units with vertical lifts for maintenance has been verified in some fusion experiments [3].

The single-piece maintenance procedure is expected to result in the shortest period of downtime during the scheduled maintenance period because: (1) the number of connects and disconnects needed to replace the components will be minimized and (2) the installation time is much shorter because the replaced components are pretested and aligned as a single unit before commitment to service. Furthermore, recovery from unscheduled events will be more standard and rapid because complete components are replaced and the reactor is brought back on line. The repair work will then be performed outside the reactor vault.

A single-piece maintenance of the entire reactor torus (including the first wall, blanket, and divertor modules) will have the additional benefits of: (1) no adverse effects resulting from the interaction of new materials operating in parallel to radiation-damaged material; (2) complete and extensive testing of the entire torus assembly can be performed before commitment to service, which is expected to result in increased reliability; and (3) it will be possible to continually modify the torus assembly as may be indicated by the reactor performance and technological developments and to fully exploit the learning curves.

In this section, the layout of the main power-plant buildings (Section 14.2) and the proposed maintenance procedures for the TITAN-I reactor (Section 14.3) are presented. A comparison of the TITAN-I single-piece maintenance procedure with a modular approach is difficult because: (1) the TITAN-I fusion power core (FPC) is designed so that

the advantages of a single-piece approach are fully utilized and a different design should have been produced to compare and quantify the benefits of single-piece maintenance procedures, (2) little data is available on times that would be required for each step during the maintenance procedure, and (3) data are needed on "mean time-to-failure" and "mean time-to-repair" of various components in order to quantify the impact of the maintenance procedure on the overall plant availability. Therefore, only those steps that are likely to be different between single-piece and modular approaches have been identified. Pretesting of the reactor torus to full operating condition is one of the potential advantages of the TITAN-I single-piece approach and is discussed in Section 14.4.

14.2. TITAN-I PLANT LAYOUT

The elevation view of the TITAN-I design is shown in Figures 14.2-1 and 14.2-2. All of the TITAN-I maintenance procedures are performed with vertical lifts. As a result, the reactor vault and reactor building are smaller. The vertical lift of various components is performed by a moveable bridge crane. The heaviest components are the moveable upper ohmic-heating (OH) coil set and the upper hot shield, each weighing about 150 tonnes, which can be easily lifted by existing cranes (conventional bridge cranes have a lift limit of about 500 tonnes and special-order cranes are available with lift limits exceeding 1000 tonnes).

The lifetime of the TITAN-I reactor torus (including the first wall, blanket, and divertor modules) is estimated to be in the range of 15 to 18 MW y/m², and the more conservative value of 15 MW y/m² will require the change-out of the reactor torus on a yearly basis for operation at 18 MW/m² of neutron wall loading at 76% availability. The lifetime of the hot shield is estimated to be five years. To reduce the quantity of the rad-waste, the TITAN-I hot shield is made of two pieces, with the upper hot shield removed during the maintenance procedures and then reused following replacement of the reactor torus. It should be noted that because of the smaller mass of the FPC of a compact reactor, the total replaced mass during the life of the plant is comparable to that of a low-neutron-wall-loading design.

One of the unique features of the TITAN-I design is that the entire FPC operates inside a vacuum tank, again made possible because of the small physical size of the reactor. The vacuum tank concept moves the vacuum boundary well away from the harsh radiation and thermal environment, allowing for a more robust and reliable design. During maintenance of the FPC, the weld at the lid of the vacuum tank must be cut

Figure 14.2-1. Elevation view of the TITAN-I reactor building through the reactor centerline showing the reactor vault and the maintenance crane.

and then rewelded after the maintenance is complete. Although a design with individual vacuum ducts leading to each of the three divertor chambers is possible, remote cutting and welding of that complex geometry is expected to be much more difficult. The vacuum tank of TITAN-I provides an additional safety barrier in the event of an off-normal incident (Section 13). Drain tanks are provided below the FPC to recover and contain any spilled lithium in the vacuum tank or the reactor building. The drain tank system connected to the vacuum tank is also evacuated during the normal operation.

The major plant facilities and the plant layout of the TITAN-I reactor are illustrated in Figure 14.2-3. The entire reactor building has an argon atmosphere to prevent and contain lithium-fire accidents in the event of a lithium spill. All of the systems containing the tritiated, primary-lithium coolant are surrounded by this argon atmosphere. New, pretested torus assemblies are also stored in inert argon. In addition, all maintenance operations are performed in this argon atmosphere. This approach reduces the out-gassing and pump-down time when the reactor torus is installed into the vacuum tank.

Figure 14.2-2. Elevation view of the TITAN-I reactor showing the vacuum tank and the FPC.

Figure 14.2-3. Proposed layout of the TITAN-I major plant facilities.

A plan-view of the confinement building is given in Figure 14.2-4 and illustrates the lay-down areas for the components which have to be moved during maintenance, as well as the storage area for the new reactor torus assemblies.

14.3. MAINTENANCE PROCEDURES

A key assumption for the TITAN maintenance program, as in other fusion reactor studies, is that a high degree of automation is available. In the TITAN-I design, powered joints are used extensively for hydraulic and electrical connect/disconnects. The use of powered joints allows many tasks to be done quickly and in parallel. Together with the single-piece maintenance scheme, which reduces the number of joints to a minimum, this approach is expected to result in a dramatic reduction in the required time to perform the maintenance operations and to increase the overall reliability. The powered joints for the coolant circuits are located on the hot and cold legs of the lithium supplies, above the torus, as is shown in Figure 14.2-2. Additional connect and disconnect powered joints are also provided for the upper OH-coil-set electrical and cooling circuits. Examples of powered joints [2] are shown in Figure 14.3-1.

Seventeen principal tasks must be accomplished for the annual, scheduled maintenance of the TITAN-I FPC. These steps are listed in Table 14.3-I. Tasks that will require a longer time to complete in a modular design are also identified in Table 14.3-I (assuming the same configuration for the modular design as that of TITAN-I). Another potential benefit of the single-piece maintenance approach is that the recovery from any unscheduled event will be standard and similar to the procedures of Table 14.3-I for the scheduled maintenance. It should be noted that the economic impact of a disabled FPC is dominated by the downtime of the plant and not by the capital cost of a new FPC.

Vertical lifts have been chosen for the component movements during maintenance. Vertical lifts allow a more compact reactor building, consistent with the TITAN-I design goal. Vertical lifts of the components are performed by a moveable bridge crane as shown in Figures 14.2-1 through 14.2-4. Lift limits for conventional bridge cranes is around 500 tonnes, with special-order crane capacities in excess of 1000 tonnes. The most massive components lifted during TITAN-I maintenance are the upper OH-coil set (OH coils 2 through 5) and the upper hot shield, each weighing about 150 tonnes. Vertical lift of these components is easily manageable by conventional cranes. The four major component lifts are illustrated in Figure 14.3-2. Temporary storage for the vacuum lid, upper OH-coil set, and upper hot shield are provided, as shown in Figure 14.2-4. These

Figure 14.2-4. Plan-view of the TITAN-I reactor building.

Clamp for remotely disconnecting
main PbLi headers.

Clamp for remotely disconnecting
PbLi drain plug.

1. Helium coolant circuits
(2 for redundancy)
2. Seal-verification and
leak-monitoring channel
3. Frozen PbLi zones

Figure 14.3-1. Examples of remote connect/disconnect, powered joints [2].

Table 14.3-I.
PRINCIPAL TASKS
DURING THE TITAN-I MAINTENANCE PROCEDURE

-
1. Orderly shutdown of the plasma and discharge of the magnets;
 2. Continue cooling the FPC at a reduced level until the decay heat is sufficiently low to allow cooling by natural convection in the argon atmosphere;
 3. During the cool-down period:
 - a. Continue vacuum pumping until sufficient tritium is removed from the FPC;
 - b. Break vacuum (valve-off vacuum pumps and cut weld at vacuum tank lid),^(a)
 - c. Remove vacuum-tank lid to the lay-down area;
 - d. Disconnect electrical and coolant supplies from the upper OH-coil set;
 4. Drain lithium from the FPC;
 5. Lift OH-coil set and store in the lay-down area;
 6. Disconnect lithium-coolant supplies;^(a)
 7. Lift upper shield and store in the lay-down area;
 8. Lift the reactor torus and move to the hot cell;^(a)
 9. Inspect FPC area;
 10. Install the new, pretested torus assembly;^(a)
 11. Connect lithium supplies;^(a)
 12. Replace upper shield and connect shield-coolant supplies;
 13. Replace the upper OH-coil set and connect electrical and coolant supplies;
 14. Hot test the FPC;^(b)
 15. Replace vacuum-tank lid and seal the vacuum tank;^(a)
 16. Pump-down the system;^(c)
 17. Initiate plasma operations.
-

(a) The time required to complete these tasks is likely to be longer for a modular system than for a single-piece system, assuming similar configuration.

(b) The new torus assembly is pretested and aligned before commitment to service. Only minimal hot testing would be required.

(c) The TITAN-I reactor building is filled with argon gas and the replacement torus is also stored in argon atmosphere. Therefore, the pump-down time would be short.

Figure 14.3-2. Four major crane lifts required for TITAN-I maintenance.

three components are reinstalled following the installation of the new torus assembly. Once the new torus is lowered into position, horizontally oriented remote connects attach the torus to the stationary primary-lithium supplies.

A simple comparison of modular and single-piece maintenance approaches can be made using Table 14.3-I by assuming a modular design for TITAN-I with the same dimensions and wall loading but with toroidal segmentation which separates the reactor torus into three or more units for maintenance purposes. Examination of the maintenance steps listed in Table 14.3-I indicates that 5 of the 17 tasks (6, 8, 10, 11, and 14) would likely be more time consuming for a modular reactor. Some of the differences are associated with those steps that involve interfaces between modules and lifting of individual modules. Since the lifting of individual modules is done in series rather than in parallel, the total number of module transfers requires more time, even though the lighter, modular unit may be transported somewhat faster than the complete reactor torus.

One of the critical steps in Table 14.3-I is the installation of the new reactor torus in the reactor vault (step 10). A modular design will require additional time in order to align the modules into a full torus (depending on the required degree of the precision). Another important difference between the modular and single-piece approaches is the degree of pretesting that can be performed outside of the reactor vault. A comprehensive set of pretests are envisioned for the TITAN-I design and are discussed in Section 14.4. For a modular design, those pretests that require a fully assembled torus should be performed after the installation of the modules into the reactor vault as a complete torus, which will increase the maintenance period and the downtime.

A self-consistent comparison of the TITAN-I single-piece maintenance procedure with a modular approach is difficult because very little information is available on the time needed to perform each of the maintenance tasks listed in Table 14.3-I. Furthermore, the TITAN-I FPC is designed such that the advantages of a single-piece approach are fully exploited and a different modular-type design should have been produced for a self-consistent comparison.

The comparison between the single-piece and modular maintenance procedures is even more difficult for unscheduled events because such a comparison would require an extensive data base on the mean time-to-failure and the mean time-to-repair of various components of the reactor. Recovery from a major event will be shorter with the single-piece maintenance approach. It is possible that for minor events, the mean time-to-repair for a modular approach will be shorter. However, for a modular approach, recovery from unscheduled events requires additional equipment, each designed to handle and repair

certain failure modes. In a single-piece approach, recovery from unscheduled events will be, in principle, standard and similar to a scheduled maintenance procedure. One should also note that the sector-to-sector interfaces in a modular design add to the number of possible fault areas, hence, possibly reducing overall reliability.

14.4. FUSION POWER CORE PRETESTING

An important feature of the TITAN design is the pretest facility. This facility allows the plant personnel to test fully the new torus assemblies in a non-nuclear environment prior to committing it to full-power nuclear operation in the reactor vault. Any faults discovered during pretesting can be quickly repaired using inexpensive hands-on maintenance. Furthermore, additional testing can be used as a shakedown period to reduce the "infant-mortality" rate of the new assemblies. A comprehensive pretest program can greatly increase the reliability of the FPC, hence increasing the overall plant availability.

A detailed list of pretests for the TITAN-I design is included in Table 14.4-I. The tests are categorized by the level of assembly and by the type of test. Three levels of assembly are identified: sub-modules, modules, and full torus. Sub-modules are such items as first-wall pipes, blanket pipes, and ring headers. Modules are the assembled blanket units as shipped from the factory to the site (12 per torus). The full torus is the unit that is installed into the reactor vault (12 blanket modules and 3 divertor modules mechanically attached together). Pretesting of the complete torus is further subdivided into plasma and no-plasma tests.

The benefits of pretesting (higher reliability, higher availability) must be balanced with the additional cost associated with the pretest facility. The more representative the pretests are of the actual operation, the more duplication of the primary-loop components is required. For example, vacuum-field mapping and divertor-plate alignment will require a vacuum system, simulated toroidal and divertor magnetic fields, and the diagnostics for the tests. Furthermore, since the toroidal-field and divertor coils are IBC-type coils (*i.e.*, the poloidally flowing lithium-coolant is used as the electrical conductor for the coil), a lithium circuit will be required for pretest. If the only requirement for pretesting is to perform hot, functional tests, then the only major facility is a hot-gas circulation system. The optimum level of pretesting can be determined with a detailed trade-off study to compare the benefits of the higher reliability and availability against the cost penalties associated with the pretest equipment. The lack of operating experience and data on the new technologies and configurations proposed for fusion systems will lead to

Table 14.4-I.

MAIN PRE-OPERATIONAL TESTING OF THE TITAN-I FPC

Test	Sub-Module ^(a)	Module ^(a)	Full Torus ^(b)	
			No Plasma	Plasma ^(c)
Mechanical				
• Tube-bank vibration (first wall, blanket)	X	X	X	
• Tube-bank expansion (first wall, blanket)	X	X	X	
• Inter-module and full-torus deflection			X	
• Plasma chamber (shell)/coil displacement			X	
Thermal Hydraulic				
• Flow rates, pressure drops, leaks, ... :			X	
* First wall, divertor, blanket, shield	X			
* Coils			X	
* Manifolds, headers			X	
• "Hot" FPC test, (pressure drops, vibrations, ...)			X	
* Electrically heated coolant			X	
* Plasma-driven heat fluxes				X
• Remote coupling, disconnects	X	X	X	
Electrical				
• Magnet test (forces, deflection, voltages, ...)			X	X
• Vacuum-field mapping (TF ripple, vertical field, ...)			X	
• Plasma transients				
* RFP formation			X	X
* Fast-ramp phase			X	X
* Slow-ramp phase				X
• Current-drive (steady-state) phase				X
• Active feedback control			X	X
• Eddy currents (start-up, OFCD)				
* First wall and shell			X	X
* Blanket and shield			X	X
* Coil casing, structure, pumps, ...			X	X
• Termination control/response				X
Vacuum, Fueling, and Impurity-Control Systems				
• Base vacuum			X	
• Full gas-load test			X	
• Pellet injection				X
Neutronics				
• Breeding efficiency				X
• Energy-recovery efficiency				X
• Shielding effectiveness, streaming				X

(a) Performed at factory site.

(b) Performed at plant site during operational year.

(c) Performed in the reactor vault during the scheduled maintenance.

a great degree of uncertainty in that type of trade-off study at present. Simple models of availability have been developed [4] and could be used to show relative benefits of increased reliability. In situations where several reactors are clustered together, the cost of a single pretest facility could be shared, leading to a lower average cost per reactor.

14.5. SUMMARY

The TITAN reactors are compact, high-power-density designs. The small physical size of these reactors permits each design to be made of only a few pieces, allowing a single-piece maintenance approach. Also, because of the small physical size and mass of the TITAN-I FPC, the maintenance procedures can be carried out through vertical lifts, allowing a much smaller reactor vault.

The major tasks required for annual maintenance of the TITAN-I FPC have been identified. Single-piece maintenance of the reactor torus (including the first wall, blanket and divertor modules) appears feasible and must be performed yearly. Following the removal of the old torus, a new, fully pretested assembly is installed.

Potential advantages of single-piece maintenance procedures are identified:

1. Shortest period of downtime resulting from scheduled and unscheduled FPC repairs;
2. Improved reliability resulting from integrated FPC pretesting in an on-site, non-nuclear test facility where coolant leaks, coil alignment, thermal-expansion effects, etc. would be corrected by using rapid and inexpensive hands-on repair procedures prior to committing the FPC nuclear service;
3. No adverse effects resulting from the interaction of new materials operating in parallel to radiation-exposed materials;
4. Ability to modify continually the FPC as may be indicated or desired by reactor performance and technological developments; and
5. Recovery from unscheduled events would be more standard and rapid. The entire reactor torus is replaced and the reactor is brought back on line with the repair work being performed, afterwards, outside the reactor vault.

A high level of pretesting ensures that the new torus will behave as designed, and will have a higher reliability than individual modules that have not been tested together

as a single operating unit under reactor-like conditions. It appears that the single-piece maintenance approach, together with a detailed pretesting program, can substantially improve the availability of the TITAN-I reactor.

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APPENDIX A

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APPENDIX A

A.1. TABLE OF TITAN-I REACTOR PARAMETERS

1. CHARACTERISTIC MACHINE DIMENSIONS

1.1. Reactor Envelope ⁽¹⁻¹⁾	
1.1.1. Height	5.67 m
1.1.2. Outside diameter	13.69 m
1.1.3. Circumference	43.01 m
1.1.4. Volume	834.78 m ³
1.2. Plasma Chamber	
1.2.1. Major toroidal radius, R_T	3.90 m
1.2.2. Plasma minor radius, r_p	0.60 m
1.2.3. First wall minor radius, r_{FW}	0.66 m
1.2.4. Plasma volume, V_p	27.71 m ³
1.2.5. Plasma chamber volume	33.53 m ³
1.2.6. First wall surface area ⁽¹⁻²⁾	169.62 m ²
1.2.7. Number of sectors ⁽¹⁻³⁾	3
1.3. Vacuum Tank ⁽¹⁻¹⁾	
1.3.1. Average height	12 m
1.3.2. Outside diameter	15 m
1.3.3. Wall thickness	0.05 m
1.3.4. Circumference	47 m
1.3.5. Volume	2120 m ³

2. PLASMA PARAMETERS

2.1. Fuel Cycle	DT/Li
2.2. Plasma Dimensions	
2.2.1. Major radius, R_T	3.90 m
2.2.2. Minor radius, r_p	0.60 m
2.2.3. Plasma cross section	circular

2.3. Electron Density, n_e ⁽²⁻¹⁾	
2.3.1. Average electron density	$9.5 \times 10^{20} \text{ m}^{-3}$
2.3.2. Central electron density	$1.4 \times 10^{21} \text{ m}^{-3}$
2.4. Ion Density, n_i ⁽²⁻¹⁾	
2.4.1. Average ion density	$8.9 \times 10^{20} \text{ m}^{-3}$
2.4.2. Central ion density	$1.3 \times 10^{21} \text{ m}^{-3}$
2.5. Average Alpha-Particle Density, n_α ⁽²⁻²⁾	$4.5 \times 10^{19} \text{ m}^{-3}$
2.6. Ion Temperature, T_i ⁽²⁻¹⁾	
2.6.1. Average ion temperature	10 keV
2.6.2. Central ion temperature	15 keV
2.7. Electron Temperature, T_e ⁽²⁻¹⁾	
2.7.1. Average electron temperature	10 keV
2.7.2. Central electron temperature	15 keV
2.8. Average Alpha-Particle Energy, E_α ⁽²⁻²⁾	68 keV
2.9. Effective Plasma Ion Charge, Z_{eff} ⁽²⁻³⁾	1.680
2.10. Global Energy-Confinement Time, τ_E ⁽²⁻⁴⁾	0.22 s
2.11. Effective Global Thermal Diffusivity, $\chi_E = (3/16)r_p^2/\tau_E$ ⁽²⁻⁴⁾	0.31 m ² /s
2.12. Electron Energy-Confinement Time, τ_{Ee} ⁽²⁻⁴⁾	0.28 s
2.13. Particle Confinement Time at Burn, τ_p	
2.13.1. Without recycling ⁽²⁻⁵⁾	1.10 s
2.13.2. With recycling ⁽²⁻⁶⁾	
2.13.2.1. Fuel ions (average)	3.68 s
2.13.2.2. Helium	1.52 s
2.14. Lawson Parameter, $n_i\tau_E$ ⁽²⁻⁴⁾	$1.92 \times 10^{10} \text{ s/m}^3$
2.15. Plasma Toroidal Current, I_ϕ ⁽²⁻⁷⁾	17.8 MA
2.16. Average Toroidal Current Density, j_ϕ ⁽²⁻⁷⁾	15.7 MA/m ²

2.17. Toroidal Field, B_ϕ ⁽²⁻¹⁾	
2.17.1. Average	3.90 T
2.17.2. At plasma edge	0.38 T
2.17.3. At plasma axis	11.85 T
2.17.4. Peak field on the coil ⁽²⁻⁸⁾	4.90 T
2.18. Poloidal Field, B_θ ⁽²⁻¹⁾	
2.18.1. Average	6.21 T
2.18.2. At plasma edge	5.93 T
2.19. Vertical Field,	
2.19.1. At plasma axis	1.56 T
2.19.2. Decay index, n	0.16
2.19.3. Stray field at startup	0.43 mT
2.20. Poloidal Beta, $\beta_\theta = 2\mu_0 p / B_\theta^2(r_p)$	0.22
2.21. Toroidal Beta $\beta = 2\mu_0 p / < B_\phi >^2$	0.52
2.22. Engineering Beta ⁽²⁻⁹⁾	7.75
2.23. Reversal Parameter, F ^{(2-7),(2-10)}	
2.23.1. Mean value at burn	-0.1
2.23.2. During OFCD oscillation	-0.03 to -0.17
2.24. Pinch Parameter, Θ ^{(2-7),(2-10)}	
2.24.1. Mean value at burn	1.56
2.24.2. During OFCD oscillation	1.50 to 1.62
2.25. Safety Factor, $q = rB_\phi / RB_\theta$	
2.25.1. On axis	0.11
2.25.2. At plasma edge	-0.01
2.25.3. Radius of reversal surface ($q = 0$)	0.55 m
2.26. Streaming Parameter, j_ϕ / n	
2.26.1. During startup (maximum value)	6.5×10^{-14} A-m
2.26.2. At burn	1.8×10^{-14} A-m
2.27. Electron Streaming Parameter, $\xi = v_D / v_{th}$	
2.27.1. During startup (maximum value)	2.0×10^{-2}
2.27.2. At burn	1.9×10^{-3}

2.28. Radiation Fraction ⁽²⁻⁹⁾	
2.28.1. Core plasma	0.70
2.28.2. Scrape-off layer	0.23
2.28.3. Divertor plasma	0.04
2.28.4. Total radiation fraction	0.97
2.28.5. Radiation fraction to first wall, f_{RAD}	0.95
2.28.6. Radiation fraction to divertor plate	0.02
2.29. Current Drive ⁽²⁻¹¹⁾	
2.29.1. Method	OFCD
2.29.2. Frequency, f	25 Hz
2.29.3. Toroidal-flux swing, $\delta\phi/\phi_0$	0.035
2.29.4. Θ variation	1.50 to 1.62
2.29.5. F variation	-0.03 to -0.17
2.29.6. Input power (toroidal/poloidal circuits)	
2.29.6.1. Real (supplied) power	44.86/2.08 MW
2.29.6.2. Reactive power	503.88/188.36 MVA
2.29.7. Power-supply dissipation ⁽²⁻¹²⁾	6.92 MW
2.29.8. Current-drive power	56.83 MW
2.29.9. Efficiency, I_ϕ/P_{CD} ⁽²⁻¹³⁾	0.31 A/W
2.30. Plasma Heating Method	Ohmic
2.31. Plasma Ohmic Dissipation	28.5 MW
2.32. Plasma Energy Gain, $Q_p = P_f/P_\Omega$ ⁽²⁻¹⁴⁾	80.2

3. POWER OUTPUT

3.1. Plasma Fusion Power, P_f	2300.6 MW
3.1.1. Neutron power, P_n	1840.5 MW
3.1.2. Alpha-particle power, P_α	460.1 MW
3.2. Blanket Energy Multiplication, M ⁽⁸⁻¹⁾	1.2
3.3. Nuclear Power ($MP_n + P_\alpha$)	2668.7 MW
3.4. Ohmic Heating Power, P_Ω ⁽⁸⁻²⁾	28.5 MW

3.5. Power to First Wall	
3.5.1. Surface heating, $f_{RAD}(P_{\alpha} + P_n)$	464.2 MW
3.5.2. Nuclear heating	
3.5.2.1. Structure	115.9 MW
3.5.2.2. Coolant	118.1 MW
3.5.3. OFCD heating (eddy currents) ⁽³⁻⁸⁾	0.01 MW
3.5.4. Coolant-pumping power ⁽³⁻⁴⁾	37.7 MW
3.5.5. Total thermal power, P_{FW}	735.9 MW
3.6. Power to Integrated Blanket Coil (IBC)	
3.6.1. Nuclear heating	
3.6.1.1. Structure	373.3 MW
3.6.1.2. Coolant	870.1 MW
3.6.2. OFCD heating (eddy currents) ⁽³⁻⁸⁾	1.0 MW
3.6.3. OFCD joule heating ⁽³⁻⁵⁾	16.0 MW
3.6.4. TF-IBC joule heating	25.6 MW
3.6.5. Coolant-pumping power ⁽³⁻⁴⁾	6.0 MW
3.6.6. Total thermal power, P_{IBC}	1292.0 MW
3.7. Power to Hot Shield ⁽³⁻⁶⁾	
3.7.1. Nuclear heating	
3.7.1.1. Structure	458.9 MW
3.7.1.2. Coolant	247.3 MW
3.7.2. Coolant-pumping power ⁽³⁻⁴⁾	3.0 MW
3.7.3. Total thermal power, P_{HS}	709.2 MW
3.8. Power to Divertor Plate	
3.8.1. Surface heating, $(1 - f_{RAD})(P_{\alpha} + P_n)$	24.4 MW
3.8.2. Nuclear heating	
3.8.2.1. Structure	2.8 MW
3.8.2.2. Coolant	1.2 MW
3.8.3. Coolant-pumping power ⁽³⁻⁴⁾	0.9 MW
3.8.4. Total thermal power, P_{DP}	29.3 MW
3.9. Power to Divertor IBC ⁽³⁻⁷⁾	
3.9.1. Nuclear heating	
3.9.1.1. Structure	17.0 MW
3.9.1.2. Coolant	4.0 MW
3.9.2. Divertor-IBC OFCD joule heating	26.8 MW
3.9.3. Divertor-IBC joule heating	120.5 MW
3.9.4. Coolant-pumping power ⁽³⁻⁴⁾	0.5 MW
3.9.5. Total thermal power, P_{DIBC}	168.8 MW

3.10. Total Reactor Thermal Power, P_{th} ⁽⁸⁻⁸⁾	2935.2 MW
3.10.1. Power Cycle I	
3.10.1.1. First wall thermal power	735.9 MW
3.10.1.2. Divertor plate thermal power	29.3 MW
3.10.1.3. Total	765.2 MW
3.10.2. Power Cycle II	
3.10.2.1. IBC thermal power	1292.0 MW
3.10.2.2. Hot shield thermal power	709.2 MW
3.10.2.3. Divertor-IBC thermal power	168.8 MW
3.10.2.4. Total	2170.0 MW
3.11. Thermal-Cycle Efficiencies, η_{th} ⁽⁸⁻⁸⁾	
3.11.1. Cycle I	0.370
3.11.2. Cycle II	0.465
3.11.3. Overall thermal efficiency	0.440
3.12. Gross Electric Power, P_{ET}	1291.5 MWe
3.13. Recirculating Power	
3.13.1. TF-IBC joule losses	25.6 MWe
3.13.2. Divertor-IBC joule losses	120.5 MWe
3.13.3. OFCD system ⁽⁸⁻⁹⁾	84.4 MWe
3.13.4. Coolant-pumping power	48.1 MWe
3.13.5. Auxiliary power ⁽²⁻¹⁰⁾	42.9 MWe
3.13.6. Total	321.5 MWe
3.14. Recirculating-Power Fraction, ϵ	0.249
3.15. Engineering Q ($= 1/\epsilon$)	4.016
3.16. Net Electric Power	970.0 MWe
3.17. Net Plant Efficiency, $\eta_p = \eta_{th}(1 - \epsilon)$	0.330
3.18. Mass Power Density, MPD ⁽⁸⁻¹¹⁾	757 kW _e /tonne
3.19. Plasma Power Density, P_f/V_p	83 MW/m ³

4. REACTOR COOLANT SYSTEM

4.1. First Wall System (4-1)

	A bank of circular tubes with single-pass, poloidal coolant flow Lithium (30% ^6Li) V-3Ti-1Si	
4.1.1. Coolant		
4.1.2. Structural material		
4.1.3. Tube outer diameter (4-1)	10.5	mm
4.1.4. Tube inner diameter (4-1)	8.0	mm
4.1.5. Erosion allowance	0.25	mm
4.1.6. Number of tubes (4-2)	2730	
4.1.7. Structure volume fraction	0.400	
4.1.8. Coolant volume fraction	0.375	
4.1.9. Void volume fraction	0.225	
4.1.10. Outlet temperature	713	K
4.1.11. Inlet temperature	592	K
4.1.12. Outlet pressure	~ 0.1	MPa
4.1.13. Inlet pressure (4-3)	12	MPa
4.1.14. Maximum primary stress	50	MPa
4.1.15. Maximum secondary stress	288	MPa
4.1.16. Maximum wall temperature	1020	K
4.1.17. Coolant flow rate (total)	1464	kg/s
4.1.18. Coolant flow velocity	21.6	m/s
4.1.19. Reynolds number, Re	1.90×10^5	
4.1.20. Magnetic Reynolds number, Re_m	0.48	
4.1.21. Hartmann number, Ha		
4.1.21.1. Perpendicular, $Ha_{\perp}$	2.01×10^2	
4.1.21.2. Parallel, $Ha_{\parallel}$	3.04×10^3	
4.1.22. Interaction parameter, $N = Ha^2/Re$		
4.1.22.1. Perpendicular, $N = Ha_{\perp}^2/Re$	0.21	
4.1.22.2. Parallel, $N = Ha_{\parallel}^2/Re$	48.60	
4.1.23. Nusselt number, Nu (4-4)	10.35	
4.1.24. Prandtl number, Pr	0.04	
4.1.25. Peclet number, Pe	7.76×10^3	

4.2. Integrated-Blanket-Coil (IBC) System ⁽⁴⁻⁵⁾

4.2.1. Coolant	6 rows of circular tubes with single-pass, poloidal coolant flow Lithium (30% ⁶ Li) V-3Ti-1Si
4.2.2. Structural material	
4.2.3. Tube outer diameter ⁽⁴⁻⁸⁾	52.5 mm
4.2.4. Tube inner diameter ⁽⁴⁻⁸⁾	47.5 mm
4.2.5. Tube wall thickness	2.5 mm
4.2.6. Number of tubes ⁽⁴⁻⁷⁾	3075
4.2.7. Structure volume fraction ⁽⁴⁻⁸⁾	0.18
4.2.8. Coolant volume fraction ⁽⁴⁻⁸⁾	0.72
4.2.9. Void volume fraction ⁽⁴⁻⁸⁾	0.10
4.2.10. Outlet temperature	973 K
4.2.11. Inlet temperature	593 K
4.2.12. Outlet pressure	~ 0.1 MPa
4.2.13. Inlet pressure ⁽⁴⁻⁹⁾	2.8 MPa
4.2.14. Maximum primary stress	30.0 MPa
4.2.15. Maximum secondary stress	5.0 MPa
4.2.16. Maximum wall temperature	1020 K
4.2.17. Coolant flow rate (total)	918 kg/s
4.2.18. Coolant flow velocity ⁽⁴⁻¹⁰⁾	0.5 m/s
4.2.19. Reynolds number, Re ⁽⁴⁻¹⁰⁾	2.75×10^4
4.2.20. Magnetic Reynolds number, Re_m ⁽⁴⁻¹⁰⁾	0.066
4.2.21. Hartmann number, Ha ⁽⁴⁻¹⁰⁾	
4.2.21.1. Perpendicular, $Ha_{\perp}$	1.30×10^8
4.2.21.2. Parallel, $Ha_{\parallel}$	1.90×10^4
4.2.22. Interaction parameter, $N = Ha^2/Re$ ⁽⁴⁻¹⁰⁾	
4.2.22.1. Perpendicular, $N = Ha_{\perp}^2/Re$	6.15×10^1
4.2.22.2. Parallel, $N = Ha_{\parallel}^2/Re$	1.33×10^4
4.2.23. Nusselt number, Nu ^{(4-10), (4-11)}	5.6

4.3. Hot Shield System ⁽⁴⁻¹²⁾

	Double-pass, poloidal circuit consisting of two zones:
Zone 1:	5 rows of square channels
Zone 2:	4 rows of rectangular channels
4.3.1. Coolant	Lithium (30% ⁶ Li)
4.3.2. Structural material	V-3Ti-1Si
4.3.3. Channel outer dimensions ⁽⁴⁻⁸⁾	
4.3.3.1. 1st zone	60.0 × 60.0 mm
4.3.3.2. 2nd zone	112.5 × 37.5 mm
4.3.4. Channel inner dimensions ⁽⁴⁻⁸⁾	
4.3.4.1. 1st zone	50.0 × 50.0 mm
4.3.4.2. 2nd zone	80.0 × 5.0 mm

4.3.5.	Channel hydraulic diameter, d_h (4-18),(4-6)	
4.3.5.1.	1st zone	50.0 mm
4.3.5.2.	2nd zone	9.4 mm
4.3.6.	Number of channels	
4.3.6.1.	1st zone	2640
4.3.6.2.	2nd zone	1126
4.3.7.	Structure volume fraction (4-8)	
4.3.7.1.	1st zone	0.3
4.3.7.2.	2nd zone	0.9
4.3.8.	Coolant volume fraction (4-8)	
4.3.8.1.	1st zone	0.7
4.3.8.2.	2nd zone	0.1
4.3.9.	Void volume fraction (4-8)	
4.3.9.1.	1st zone	0.0
4.3.9.2.	2nd zone	0.0
4.3.10.	Outlet temperature	973 K
4.3.11.	Inlet temperature	593 K
4.3.12.	Outlet pressure	~ 0.1 MPa
4.3.13.	Inlet pressure (4-9)	2.8 MPa
4.3.14.	Maximum primary stress	0.5 MPa
4.3.15.	Maximum secondary stress	5.0 MPa
4.3.16.	Maximum wall temperature	1020 K
4.3.17.	Coolant flow rate (total)	445 kg/s
4.3.18.	Coolant flow velocity (4-8)	0.17 m/s
4.3.19.	Reynolds number, Re (4-10)	
4.3.19.1.	1st zone	9.79×10^8
4.3.19.2.	2nd zone	1.84×10^8
4.3.20.	Magnetic Reynolds number, Re_m (4-10)	
4.3.20.1.	1st zone	2.40×10^{-2}
4.3.20.2.	2nd zone	4.52×10^{-3}
4.3.21.	Hartmann number, Ha (4-10)	
4.3.21.1.	Perpendicular, $Ha_{\perp}$	0.
4.3.21.2.	Parallel, $Ha_{\parallel}$ (4-10)	
4.3.21.2.1.	1st zone	7.00×10^3
4.3.21.2.2.	2nd zone	1.30×10^3
4.3.22.	Interaction parameter, $N = Ha^2/Re$ (4-10)	
4.3.22.1.	Perpendicular, $N = Ha_{\perp}^2/Re$	0.
4.3.22.2.	Parallel, $N = Ha_{\parallel}^2/Re$	
4.3.22.2.1.	1st zone	2.24×10^3
4.3.22.2.2.	2nd zone	4.10×10^3
4.3.23.	Nusselt number, Nu (4-11)	
4.3.23.1.	1st zone	3.6
4.3.23.2.	2nd zone	8.2

4.4. Divertor Plate System ⁽⁴⁻¹⁴⁾

The armor is brazed to
a bank of circular tubes with
single-pass, poloidal coolant flow
Lithium (30% ⁶Li)

4.4.1. Coolant	
4.4.2. Structural material	
4.4.2.1. Armor	W-26Re
4.4.2.2. Coolant tube	V-3Ti-1Si
4.4.2.3. Insulator	Spinel (MgAl ₂ O ₄)
4.4.3. Coolant-tube outer diameter	6.0 mm
4.4.4. Coolant-tube inner diameter	5.0 mm
4.4.5. Armor thickness	2.0 mm
4.4.6. Outlet temperature	813 K
4.4.7. Inlet temperature	593 K
4.4.8. Outlet pressure	~ 0.1 MPa
4.4.9. Inlet pressure ⁽⁴⁻¹⁵⁾	12 MPa
4.4.10. Maximum primary stress	
4.4.10.1. Armor	~ 30 MPa
4.4.10.2. Coolant tube	67 MPa
4.4.11. Maximum secondary stress	
4.4.11.1. Armor	480 MPa
4.4.11.2. Coolant tube	160 MPa
4.4.12. Maximum wall temperature	
4.4.12.1. Armor	1193 K
4.4.12.2. Coolant tube	1033 K
4.4.13. Coolant flow rate (total)	31 kg/s
4.4.14. Maximum coolant flow velocity ⁽⁴⁻¹⁶⁾	25 m/s

4.5. Coolant-Circulator Type

4.5.1. First wall and divertor plate	Two-stage centrifugal pump
4.5.2. IBC, hot shield, and divertor IBC	Single-stage centrifugal pump

4.6. Power Input to Each Circulator ⁽⁴⁻¹⁷⁾

4.6.1. First wall and divertor plate	38.6 MWe
4.6.2. IBC, hot shield, and divertor IBC	9.5 MWe

4.7. Thermal-Energy Storage

Not Required

4.8. Peak Temperature During a Loss-of-Flow Accident (LOFA)

4.8.1. First wall	1328 K
4.8.2. IBC	1320 K
4.8.3. Hot shield	1120 K
4.8.4. Coolant	1328 K

4.9. Peak Temperature During a Loss-of-Coolant Accident (LOCA)

4.9.1. First wall	1819 K
4.9.2. IBC	1816 K
4.9.3. Hot shield	1650 K
4.9.4. Coolant (4-18)	762 K

5. INTERMEDIATE COOLANT SYSTEM ⁽⁵⁻¹⁾

	<u>Cycle 1</u>	<u>Cycle 2</u>
5.1. Secondary Coolant	Lithium	Lithium (natural)
5.2. Total Thermal Power	765	2170 MW
5.3. Number of Intermediate Heat Exchangers, IHX ⁽⁵⁻²⁾	3	6
5.4. Primary-Coolant Inlet Temperature	715	973 K
5.5. Primary-Coolant Outlet Temperature	593	593 K
5.6. Primary-Coolant Flow Rate	1495	1363 kg/s
5.7. Secondary-Coolant Inlet Temperature	573	573 K
5.8. Secondary-Coolant Outlet Temperature	695	953 K
5.9. Secondary-Coolant Flow Rate	1495	1363 kg/s

6. STEAM GENERATION SYSTEM

	<u>Cycle 1</u>	<u>Cycle 2</u>
6.1. Steam Outlet Temperature	669	839 K
6.2. Steam Outlet Pressure	10.7	21.4 MPa
6.3. Total Steam Flow Rate	326	703 kg/s
6.4. Feedwater Inlet Temperature	442	531 K
6.5. Number of Steam Generators per Sector	1	1
6.6. Gross Thermal-Power Efficiency	0.370	0.465
6.7. Gross Electric Output	283.1	1009.4 MWe

7. SHIELD COOLANT SYSTEM (7-1)

7.1. EFC-Shield Coolant (7-2)

Gaseous helium

7.2. B₄C Radiation-Shield Coolant (7-3)

Argon

8. REACTOR AUXILIARY SYSTEM

8.1. Vacuum Pumping System (8-1)

8.1.1. Vacuum and plasma chamber pressure

8.1.1.1. Base plasma chamber pressure

10⁻⁵ mtorr

8.1.1.2. Divertor duct pressure (8-2)

20 - 100 mtorr

8.1.1.3. Vacuum tank pressure

20 mtorr

8.1.2. Temperature of neutral gas

700 K

8.1.3. Plasma chamber volume

33.6 m³

8.1.4. Vacuum chamber volume

2120 m³

8.1.5. Particle throughput (8-3)

8.1.5.1. DT

6.7 × 10²¹ s⁻¹

8.1.5.2. Helium

8.2 × 10²⁰ s⁻¹

8.1.6. Conductance of the divertor duct

8.1.6.1. For DT

215 m³/s

8.1.6.2. For He

224 m³/s

8.1.7. Type of pumps (8-4)

Turbo-molecular

8.1.8. Number of vacuum pumps (8-4),(8-5)

16

8.1.9. Helium pumping speed (per pump) (8-6)

20 m³/s

8.2. Magnet Cooling System

8.2.1. Toroidal-field coils (8-7),(8-8)

8.2.1.1. Coolant

Lithium (30% ⁶Li)

8.2.1.2. Cooling load

41.6 MW

8.2.2. Divertor-field coils (8-7)

8.2.2.1. Coolant

Lithium (30% ⁶Li)

8.2.2.2. Cooling load

147.3 MW

8.2.3. Ohmic-heating coils (8-8),(8-9)

8.2.3.1. Coolant

Gaseous helium

8.2.3.2. Peak Cooling load during startup

200 to 300 MW

8.2.3.3. Cooling load during normal operation

14 MW

8.2.4. Equilibrium-field coils (8-10)

8.2.4.1. Coolant

Liquid helium

8.2.4.2. Cooling load (8-11)

8.3. Plasma-Fueling System ⁽⁸⁻¹²⁾

8.3.1. Type	DT pellet
8.3.2. Fuel composition	50% D, 50% T
8.3.3. Fueling rates	0.035 g/s
8.3.4. Pellet diameter	3.4 mm
8.3.5. Pellet speed	2.1 km/s
8.3.6. Pellet injection frequency	6.25 s ⁻¹

8.4. Tritium-Processing and Recovery System

8.4.1. Type	Molten-salt extraction
8.4.2. Total tritium inventory	1.65 kg
8.4.3. Tritium handling rate ⁽⁸⁻¹³⁾	0.52 kg/d
8.4.4. Lithium processing rate	2.11 × 10 ⁶ kg/d
8.4.5. Primary-coolant tritium concentration	1 wppm
8.4.6. Tritium recovery efficiency	0.90
8.4.7. Number of extractor units	28
8.4.8. Required electrical power	104 kW

9. REACTOR COMPONENTS

9.1. First Wall and Integrated Blanket Coil

9.1.1. Structural material	V-3Ti-1Si
9.1.2. Breeding material	Lithium (30% ⁶ Li)
9.1.3. Neutron multiplier material	none
9.1.4. Tritium breeding ratio (TBR) ⁽⁹⁻¹⁾	1.184
9.1.5. Number of modules ⁽⁹⁻²⁾	1
9.1.6. Weight of modules	45 tonnes
9.1.7. Largest single component	
9.1.7.1. Weight	45 tonnes
9.1.7.2. Dimensions (diameter × height)	9.7 × 1.9 m
9.1.9. Lifetime ⁽⁹⁻³⁾	1 y

9.2. Hot Shield

9.2.1. Structural material	V-3Ti-1Si
9.2.2. Number of modules ⁽⁹⁻⁴⁾	2
9.2.3. Weight of upper module	146 tonnes
9.2.4. Weight of lower module	73 tonnes
9.2.5. Largest single component	
9.2.5.1. Weight	146 tonnes
9.2.5.2. Dimensions (diameter × height)	10.6 × 2.0 m
9.2.6. Lifetime ⁽⁹⁻⁵⁾	5 y

9.3. Divertor

9.3.1. Type	Toroidal-field divertors
9.3.2. Number of divertors	3
9.3.3. Neutralizer plate	
9.3.3.1 Armor	W-26Re
9.3.3.2 Coolant tube	V-3Ti-1Si
9.3.3.3 Coolant	Lithium
9.3.4. Weight of each divertor	11 tonnes
9.3.5. Dimensions (diameter×height)	3 × 3 m
9.3.6. Lifetime ⁽⁹⁻²⁾	1 y

9.4. Magnets

9.4.1. Ohmic-heating coils ^{(9-5),(9-6)}	
9.4.1.1. Type	Normal
9.4.1.2. Number	16
9.4.1.3. Conductor	Copper
9.4.1.4. Structure	Steel
9.4.1.5. Insulator	Spinel (MgAl ₂ O ₄)
9.4.1.6. Coolant	Gaseous helium
9.4.1.7. Operating temperature	20 - 80 °C
9.4.1.8. Maximum stress in coil	23 MPa
9.4.1.9. Mean coil radius	2.15 - 6.0 m
9.4.2. Equilibrium-field coils ⁽⁹⁻⁷⁾	
9.4.2.1. Type	Superconducting
9.4.2.2. Number	2
9.4.2.3. Conductor	Nb-Ti
9.4.2.4. Structure	Steel
9.4.2.5. Stabilizer	Copper
9.4.2.6. Coolant	Liquid helium
9.4.2.7. Operating temperature	4 K
9.4.2.8. Mean stress in coil ⁽⁹⁻⁷⁾	
9.4.2.9. Mean coil radius	6.50 m
9.4.3. Toroidal-field coils ⁽⁹⁻⁵⁾	
9.4.3.1. Type	Normal (IBC)
9.4.3.2. Number	12
9.4.3.3. Conductor	Lithium (30% ⁶ Li)
9.4.3.4. Structure	V-3Ti-1Si
9.4.3.5. Insulator	Spinel (MgAl ₂ O ₄)
9.4.3.6. Coolant	Lithium (30% ⁶ Li)
9.4.3.7. Operating temperature	783 K
9.4.3.8. Mean stress in coil ⁽⁹⁻⁸⁾	30 MPa
9.4.3.9. Mean coil radius	0.8 m

9.4.4.	Divertor-field coils	
9.4.4.1.	Type	Normal (IBC)
9.4.4.2.	Number	12
9.4.4.3.	Conductor	Lithium (30% ^6Li)
9.4.4.4.	Structure	V-3Ti-1Si
9.4.4.5.	Insulator	Spinel (MgAl_2O_4)
9.4.4.6.	Coolant	Lithium (30% ^6Li)
9.4.4.7.	Operating temperature	783 K
9.4.4.8.	Mean stress in coil $(9-8)$	30 MPa
9.4.4.9.	Mean coil radius	0.8 m
9.4.5.	Maximum force transmitted to building $(9-9)$	
9.4.6.	Maximum toroidal field	
9.4.6.1.	At plasma axis	11.85 T
9.4.6.2.	At plasma edge	0.38 T
9.4.6.3.	At TF coil	0.38 T
9.4.7.	Total stored energy $(9-10)$	
9.4.7.1.	Poloidal field	3.9 GJ
9.4.7.2.	Toroidal field	0.7 GJ
9.4.8.	Largest single component $(9-11)$	
9.4.8.1.	Weight	250 tonnes
9.4.8.2.	Dimensions (diameter $\times$ height)	8.5×1.7 m
9.5	Energy Transfer and Storage	None
9.6.	Plasma Heating	
9.6.1.	Type	Ohmic
9.6.2.	Frequency	-
9.6.3.	Power to plasma $(9-12)$	35 MW
9.6.4.	Transmission method	Induction
9.6.5.	Power reflected from plasma	- MW
9.6.6.	Power loss in transmission	- MW
9.6.7.	Power loss in amplifiers	-
9.6.8.	Power loss in power supplies $(9-13)$	5 MW
9.6.9.	System input power $(9-12)$	500 MW
9.6.10.	Heating time $(9-14)$	11 s

10. ELECTRICAL-POWER REQUIREMENTS

10.1. Cold-Plasma Startup Power from Grid ⁽¹⁰⁻¹⁾	500 MWe
10.2. Auxiliary Power Requirements (Normal Operation)	
10.2.1. Electrical energy storage	None
10.2.2. Toroidal-field coils (IBC)	25.6 MWe
10.2.3. Divertor-field coils (IBC)	120.5 MWe
10.2.4. OFCD ⁽¹⁰⁻²⁾	84.4 MWe
10.2.5. First wall and divertor coolant circulators	38.6 MWe
10.2.6. IBC and hot-shield coolant circulators	9.5 MWe
10.2.7. Other auxiliary systems ⁽¹⁰⁻⁸⁾	42.9 MWe
10.2.8. Total	321.5 MWe

11. BUILDINGS

11.1. Reactor Building ⁽¹¹⁻²⁾	
11.1.1. Characteristic dimensions	
11.1.1.1. Length	80 m
11.1.1.2. Width	30 m
11.1.1.3. Height	30 m
11.1.2. Enclosed volume	
11.1.2.1. Free volume ⁽¹¹⁻²⁾	69800 m ³
11.1.2.2. Total volume	72000 m ³
11.1.3. Minimum wall thickness ⁽¹¹⁻⁸⁾ for shielding	1 m
11.1.4. Internal pressure	1 atm
11.1.5. Containment atmosphere	Argon
11.2. Turbine Building ⁽¹¹⁻⁴⁾	
11.2.1. Characteristic dimensions for Cycle 1	
11.2.1.1. Length	86 m
11.2.1.2. Width	40 m
11.2.1.3. Height	32 m
11.2.2. Characteristic dimensions for Cycle 2	
11.2.2.1. Length	113 m
11.2.2.2. Width	40 m
11.2.2.3. Height	34 m
11.2.3. Total enclosed volume	2.64×10^5 m ³
11.3. Reactor Service Building	
11.3.1. Characteristic dimensions ⁽¹¹⁻⁵⁾	

12. REACTOR MAINTENANCE**12.1. First Wall, IBC, and Divertor**

12.1.1. Lifetime

1 y

12.1.2. Weight

45 tonnes

12.2. Hot Shield

12.2.1. Lifetime

5 y

12.2.2. Weight

219 tonnes

12.3. OH and EF Magnet12.3.1. Lifetime ⁽¹²⁻¹⁾

40 y

12.3.2. Weight

566 tonnes

12.4. Radioactive Material Storage for Life of Plant ⁽¹²⁻¹⁾12.4.1. Total Class C ⁽¹²⁻²⁾

12.4.1.1. Weight

1363 tonnes

12.4.1.2. Volume

199 m³12.4.2. Total non-Class C ⁽¹²⁻⁸⁾

12.4.2.1. Weight

14 tonnes

12.4.2.2. Volume

1 m³**12.5. Overall Plant Availability**

0.76

FOOTNOTES TO TABLE A.1

1-1) The reactor envelope is defined by the cylinder enclosed by the upper and lower EF coils.

1-2) The first wall is corrugated, therefore, the total plasma-facing area is $\frac{\pi}{2} (2\pi R_T) (2\pi r_{FW})$.

1-3) Sectors are used only for factory manufacturing and shipment to the site. These sectors, together with divertor modules, are assembled on-site into a single torus and tested to full operational condition prior to installation into the reactor vault.

2-1) During the startup, plasma profiles are assumed to be

$$n(r)/n_0 = (1 - r/r_p)^{2.5}$$

$$T(r)/T_0 = (1 - r/r_p)^4$$

$$\mu(r)/\mu_0 = (1 - r/r_p)^6.$$

During the steady-state burn, TITAN-I operates with a highly radiative plasma (through injection of xenon impurities). The plasma profiles at burn are computed in Section 5.3 and are:

$$n(r)/n_0 = 1.00 - 0.86(r/r_p)^{0.44}$$

$$T(r)/T_0 = \begin{cases} 1.00 - 3.26(r/r_p)^{2.85} & \text{for } 0.00 < r/r_p < 0.25 \\ 1.12 - 0.71(r/r_p) & \text{for } 0.25 < r/r_p < 0.83 \\ 0.56 - 0.55(r/r_p)^{14.02} & \text{for } 0.83 < r/r_p < 1 \end{cases}$$

$$\mu(r)/\mu_0 = 1.00 - 0.44(r/r_p)^6 - 0.56(r/r_p)^6.$$

2-2) Averaged value over both fast and thermal components.

2-3) The TITAN-I design operates with a highly radiative plasma by deliberately doping the plasma with xenon impurities.

2-4) Assuming a beta-limited confinement time of the form $\tau_E = c_\nu I_\phi' r_p^2 f(\beta_\theta)$.

2-5) Assumed to be $\tau_p \approx 4\tau_{E*}$.

2-6) Based on self-consistent core-plasma, edge-plasma, and neutral-particle transport calculations.

2-7) Represents the mean value during the OFCD cycle (see item 2.29).

2-8) Including the poloidal field. The field produced by the coil is about 0.4 T.

2-9) The engineering beta is defined as the ratio of the average plasma pressure to the maximum field strength on the toroidal-field coils.

2-10) The reversal parameter is held constant during the startup (by adjusting the toroidal-field strength), and the values of the pinch parameter are from equilibrium calculations.

- 2-11) Results of detailed calculations assuming full-coverage TF coils (Section 7). Current-drive parameters quoted in the rest of this table are found by scaling the results of Section 7 to include the effects of the currents in the divertor coils which should also be oscillated to keep the separatrix in its position. For this case, the supplied real power in the toroidal circuit is 72.4 MW including 28.55 to the plasma, 16.0 MW to TF coils, 28.8 MW to the divertor coils, and 1.04 MW eddy-current heating in the blanket. The power to TF and divertor coils represent the AC component of the currents in these coils and include additional heating in the hot and cold legs of coolant by IBC power supplies connections.
- 2-12) Assuming a power supply efficiency, $Q = 100$.
- 2-13) A "wall-plug" efficiency including power-supply losses.
- 2-14) All of the current-drive power deposited in the plasma appears as ohmic heating power.
- 3-1) Including nuclear heating in non-current-carrying divertor IBC and divertor shield.
- 3-2) All of the current-drive power deposited in the plasma appears as ohmic heating power.
- 3-3) Heating by eddy currents induced in the first wall and blanket by OFCD cycles.
- 3-4) Assuming an efficiency of 80% for coolant-circulation pumps.
- 3-5) Joule heating in the IBC which also acts as the driver coil of the toroidal circuit of the OFCD system.
- 3-6) Values are summed over the two zones of the hot shield
- 3-7) Values for current-carrying divertor IBC. Values for non-current-carrying part is included in item 3.6.
- 3-8) TITAN-I design has two power cycles. The first power cycle handles the thermal power from the first wall and the divertor plate. The thermal power from the IBC, hot shield, and the divertor IBC are converted by the second power cycle.
- 3-9) Including the losses in the divertor coils (see footnote 2-10).
- 3-10) Allocated for housekeeping and auxiliary functions.
- 3-11) Mass power density is defined as the ratio of the net electric output to the mass of the FPC which includes the first wall, blanket, shield, divertor modules, coils, and related structure.
- 4-1) The first wall tubes have constant cross section. They are staggered slightly on the inboard side to account for the smaller length in the toroidal direction there. Manifolds and headers are not included. The parallel (poloidal) and perpendicular (toroidal) magnetic field strengths are, respectively, 5.4 and 0.36 T.
- 4-2) Assuming 100% coverage of the first wall and does not account for the divertor modules.
- 4-3) Inlet pressure of the first wall and divertor plate coolant circuit. The pressure at the inlet of the first wall tubes is reduced to 10 MPa by orifices.

- 4-4) The coolant flow is turbulent.
- 4-5) The IBC tubes have a circular cross section on the outboard midplane location. The tubes are deformed and have elliptical cross section (but with a constant cross-sectional area) at other places to account for varying toroidal lengths. Manifolds and headers are not included. The parallel (poloidal) and perpendicular (toroidal) magnetic field strengths at the first row of IBC tubes are, respectively, 4.5 and 0.36 T.
- 4-6) Dimensions at the outboard midplane location.
- 4-7) Including the divertor IBC tubes.
- 4-8) Averaged over the entire system.
- 4-9) Inlet pressure of the blanket and shield circuit. Orifices are used to reduce the pressure from 2.8 MPa to the required value for each row of channels.
- 4-10) Values for the first row of coolant channels.
- 4-11) The coolant flow is laminar.
- 4-12) The TITAN-I hot shield is made of two pieces. The upper shield is removed during the annual maintenance to allow access to the torus assembly and is re-installed after the replacement of the reactor torus. The coolant enters upper- or lower-shield sections through the first four square channels, make a 180° turn, and exits through the last square channels and the five rectangular channels. Manifolds and headers are not included. The parallel (poloidal) and perpendicular (toroidal) magnetic field strengths at the first row of channels are, respectively, 2 and 0 T (hot shield channels are outside of the TF-IBCs).
- 4-13) The hydraulic diameter of the channel is defined as $d_h = 4 \times \frac{\text{area}}{\text{perimeter}}$.
- 4-14) The TITAN-I divertor plate is made of an armor which is brazed to (and insulated from) a bank of circular, constant cross section tubes. Manifolds and headers are not included. The parallel (poloidal) and perpendicular (toroidal) magnetic field strengths vary at each coolant-tube location. The coolant flow is laminar in certain tubes and turbulent in others.
- 4-15) Inlet pressure of the first wall and divertor plate coolant circuit which is set by the maximum pressure drops in divertor-coolant tubes. The pressure at the inlet of most of the divertor-coolant tubes is lower than 12 MPa and orifices are used to reduce the pressure to the desired value at the inlet of these tubes.
- 4-16) The maximum coolant-flow velocity, imposed as a constraint on the thermal-hydraulic design of the divertor plate. The coolant-flow velocity in most of tubes is lower than this value.
- 4-17) The efficiency of circulating pumps is assumed to be 90%.
- 4-18) Peak lithium temperature in the event of a combined LOCA and air influx to the reactor vault. A drain-time of 30 s is assumed for the burn-time of the spilled lithium.
- 5-1) TITAN-I design has two power cycles. Power cycle 1 includes first wall and divertor plate coolant circuits. Power cycle 2 includes the IBC, hot shield, and divertor-IBC coolant circuits.

- 5-2) Total number of IHXs for all three sectors. There are two IHXs per sector for blanket and hot shield circuit based on electrical engineering requirements of the IBC system.
- 7-1) Power is recovered from the hot shield (see item 4.3.)
- 7-2) Shield for superconducting EF coils. This shield is cooled by the ohmic-heating-coil cooling circuit.
- 7-3) Radiation shield to reduce activation of the argon cover gas. This shield is cooled by the natural circulation.
- 8-1) The TITAN-I reactor is located inside a vacuum tank.
- 8-2) The pressure in the vacuum duct varies in the poloidal direction.
- 8-3) Based on neutral transport calculations (Section 5.5). Particle throughput is enriched by about a factor 3 in He (with respect to DT throughput) and also by a factor of about 3 in D (with respect to T).
- 8-4) The turbo-molecular pumps are backed by 12 scroll pumps with pumping speed of 600 m³/hr, operating between 50 mtorr and atmospheric pressure.
- 8-5) Based on the required total pumping speed of 260 m³/s, only 13 pumps are needed. The additional 3 pumps are in reserve.
- 8-6) Assuming out-gassing rate of 10^{-11} to 10^{-12} torr-l/s, the required pumping speed to maintain the base pressure in the plasma chamber is in the range 3 to 35 m³/s, much lower than the required pumping speed of 260 m³/s for removal of the He ash.
- 8-7) Both toroidal and divertor-field coils are based on the IBC concept. The cooling loads for these coils are deposited directly into the primary coolant.
- 8-8) Includes both steady state and OFCD contributions.
- 8-9) Peak heating load during startup.
- 8-10) A pair of normal-conducting, EF trim coils are used during startup and OFCD cycles. Cooling loads for these coils are included in item 8.2.3.
- 8-11) The EF coils are based on present day technology with Nb-Ti conductor. Detailed design for these coils, therefore, was not performed.
- 8-12) The neutral-transport calculation of plasma exhaust shows some enrichment in D in the exhaust (Section 5). The effect of this enrichment is not included in this item. Also, plasma-driven permeation of D and T ions through the first wall is ignored.
- 8-13) Based on a tritium-breeding ratio of 1.2 and assuming 100 g/d of plasma-driven permeation of tritium through the first wall.
- 9-1) Based on 3D neutronics calculations of the final design.

- 9-2) The reactor torus is assembled on-site from 3 prefabricated sectors interlinked by the 3 divertors. The reactor torus is installed and removed as one single piece.
- 9-3) Based on a lifetime of 15 MW-y/m².
- 9-4) The TITAN-I hot shield is made of two pieces. The upper shield is removed during the annual maintenance to allow access to the torus assembly and is re-installed after the replacement of the reactor torus.
- 9-5) Also act as the driver coils for the OFCD system.
- 9-6) Includes a pair of normal-conducting, EF trim coils which are used during startup and OFCD cycles.
- 9-7) The EF coils are based on present day technology with Nb-Ti conductor. Detailed design for these coils, therefore, was not performed.
- 9-8) Primarily caused by the coolant pressure in the IBC.
- 9-9) Primarily caused by the weight of the FPC.
- 9-10) Including the plasma stored energy. The poloidal magnetic energies stored in the system are 0.3 GJ internal to the plasma and 3.6 GJ external to the plasma, noting that the plasma current and the EFC current are flowing in opposite directions. The toroidal magnetic energy internal to the plasma is 0.4 GJ and external to the plasma is 0.02 GJ.
- 9-11) The largest component is the EF coil, including EFC shield.
- 9-12) Maximum value during startup.
- 9-13) Assuming an efficiency of 99% for power supplies.
- 9-14) Time from cold startup to ignition.

- 10-1) Maximum value during startup.
- 10-2) Including the losses in the divertor coils (see footnote 2-10).
- 10-2) Includes the refrigeration systems, vacuum systems, tritium-extraction systems, feed-water pumps, heat-rejection systems, and other miscellaneous reactor auxiliary systems.

- 11-1) Reactor building volume includes: reactor vault, lay-down area (for vacuum tank lid, OH coils, and upper shield), and storage area (for new, pretested torus)
- 11-2) Free volume consists of total volume minus the vacuum tank volume.
- 11-3) Required wall thickness is based on activation issues for argon atmosphere surrounding the vacuum tank.
- 11-4) Turbine-building dimensions were scaled from those of the MARS reactor design (B. G. Logan *et al.*, Lawrence Livermore National Laboratory report, UCRL-53480, 1984).
- 11-5) Not calculated.

- 12-1) Assuming 30 full-power-year operation with 76% availability.
- 12-2) Including first wall, blanket, shield, and magnets.
- 12-3) The divertor target plates, if they have to be disposed of separately.

A.2. SYSTEMS CODE PARAMETERS OF TITAN-I

1 TITAN Reversed-Field Pinch Reactor calculations (ver. 9.g)
 1 1 0 0 1 1 0 1 0 11/24/87

plasma parameters

plasma aspect ratio, A	6.500
plasma minor radius (m)	0.600
plasma toroidal major radius (m)	3.900
plasma volume (m ³)	27.714
plasma/first-wall radius ratio, x	0.909
plasma current (MA)	17.818
toroidal current density (MA/m ²)	15.754
plasma ion temperature (keV)	10.000
plasma electron temperature (keV)	9.500
plasma fus. prod. temperature (keV)	65.000
plasma impurity temperature (keV)	10.000
Lawson parameter * t2 (1.0e22 s keV ² /m ³)	1.917
Lawson parameter (1.0e20 s/m ³)	1.917
plasma (total) ion density (1.0e20/m ³)	8.934
plasma electron density (1.0e20/m ³)	9.331
ion (D,T,He3,He4,Xe) density fractions:	
0.48484 0.48484 0.00000 0.03000 0.00033	
effective plasma charge, Zeff	1.690
energy confinement time (s)	0.215
alpha ion particle confinement time (steady state) (s)	0.855
thermal diffusivity (m ² /s)	0.315
dt fusion power density (MW/m ³)	83.014
fusion power, Pf (MW)	2300.630
alpha power (MW)	460.653
bremstrahlung power (MW)	251.297
poloidal beta (incl. fus. prods. andimps., Zeff>1.0)	0.220
poloidal beta (only fuel ions and electrons, Zeff=1.0)	0.200
pinch parameter, theta	1.556
reversal parameter, F	0.100
streaming parameter (1.0e-14 A m)	1.763
streaming function (1.0e-3)	1.856
plasma ohmic dissipation during burn (MW)	28.527
plasma loop voltage (V)	1.601
gdt, gohm, gbr = 1.403 2.924 1.172	
OFCD plasma Poynting power (tor/pol) (MW)	3956.711 247.110
OFCD FV dissipated power (tor/pol) (MW)	0.000 0.011
OFCD blanket dissipated power (tor/pol) (MW)	1.038 0.000
OFCD coil dissipated power (OHC/TFC/PEFC/SEFC) (MW)	
0.131 50.621 0.000 1.948	
OFCD total dissipated power (tor/pol) (MW)	51.660 2.091
OFCD terminal real(lost) power (OHC/TFC/PEFC/SEFC) (MW)	
1.617 79.062 0.000 3.438	
OFCD power-supply dissipated power, pcdps (MW)	7.266
OFCD total dissipated power, pcdd (MW)	61.915
OFCD terminal reactive power (OHC/TFC/PEFC/SEFC) (MW)	
74.861 538.345 0.000 113.350	
OFCD total reactive power, pcdr (MW)	726.555
OFCD power-supply q-value, Q(PS)	100.000
OFCD circuit q-value, Q(C)	11.735
OFCD q-value, Q(CD)	2.170
OFCD efficiency figure of merit (A/W)	0.288

1 poloidal field quantities

OH-coil thickness (m)	0.270
average minor radius of coil (m)	1.565
OH coil center offset	0.439
vertical field (T)	1.562
magnetic field level at the coil (T)	2.277
magnetic field level at the plasma surface (T)	5.939
OH coil back-bias power ratio, $f_{gridp} = (IOH+/IOH-)^{-2}$	1.000
OH coil factor for resistive flux dissipated in startup	0.109
OH coil factor	0.390
OH-coil current swing (MA)	50.422
OH- coil current (MA)	25.211
OH- coil current density (MA/m ²)	13.148
coil stress (MPa)	106.262
OH+ coil current (MA)	25.211
OH+ coil current density (MA/m ²)	13.148
OH coil power supply requirement (MVA)	445.271
mass of OH coil set (tonne)	343.002
ohmic dissipation during back-bias (MW)	120.916
volumetric heating during back-bias (MW/m ³)	2.573
EF coil current (MA)	20.556
EF coil current density (MA/m ²)	19.159
EF coil power supply requirement (MVA)	477.396
mass of EF coil set (tonne)	304.927
magnetic energy stored in coil (MJ)	5194.986
ohmic dissipation during burn (MW)	0.000
volumetric heating during burn (MW/m ³)	0.000
plasma startup risetime (s)	8.000
Ref,zef,delef (m) =	6.1962e+00 2.6663e+00 7.3243e-01

0 toroidal field quantities

TF-coil thickness (m)	0.280
average minor radius of coil (m)	0.680
mass of coil (tonne)	0.000
initial toroidal bias field (T)	3.410
reversed-toroidal field during the burn (T)	0.382
magnetic energy stored in the coil (MJ)	164.739
TF-coil current (MA)	6.603
TF coil current density (MA/m ²)	1.644
ohmic dissipation during burn (MW)	27.629
volumetric heating during burn (MW/m ³)	1.335

0 impurity control

first-wall surface area (m ²)	101.617
total divertor plate surface area (m ²)	6.998
first-wall/divertor surface heat flux (MW/m ²)	4.573
radiation fraction, frad	0.950
scrape-off thickness (m)	0.060
DF-coil current (MA)	2.845
DF coil current density (MA/m ²)	19.840
mass of coil (tonne)	0.554
ohmic dissipation during burn (MW)	141.541

1 engineering summary

thermal conversion efficiency	0.440
auxiliary site power fraction of P_g , f_{aux}	0.030
primary loop pumping power fraction of P_g , f_{pmp}	0.040
engineering q-value, QE	4.017
recirculating power fraction, $\epsilon = 1/QE$	0.249
net plant efficiency	0.330
ohmic q-value, Q_t	12.702
plasma q-value, Q_p	80.648
total thermal power, P_{th} (MW)	2935.187
fusion power, P_f (MW)	2300.650
tritium burnup (kg/day)	0.353
fractional burnup	0.057
tritium thru-put (no recycle) (kg/day)	6.221
blanket LM 6Li enrichment	0.300
14.06-MeV neutron load, I_v (MW/m ²)	18.107
14.06-MeV neutron energy multiplication in blanket, M	1.200
2.45-MeV neutron load (MW/m ²)	0.000
2.45-MeV neutron energy multiplication in blanket, M	0.000
first-wall radius (m)	0.660
FW/SW/blanket/gap/reflector/gap/shield thickness (m)	0.760
shield/coil gap thickness (m)	0.010
OH-coil thickness (m)	0.270
minor radius of system (m)	1.700
blanket volume (m ³)	31.356
reflector volume (m ³)	51.733
shield volume (m ³)	27.553
EF-coil shield volume (m ³)	54.194
TF-coil volume (m ³)	0.000
OH-coil volume (m ³)	46.987
EF-coil volume (m ³)	41.771
DF-coil volume (m ³)	0.721
mass of FW/SW/blanket (tonne)	40.582
mass of reflector (tonne)	116.066
mass of OHC shield (tonne)	151.263
mass of EFC shield (tonne)	325.166
mass of TF coil set (tonne)	0.000
mass of OH coil set (tonne)	343.002
mass of EF coil set (tonne)	304.927
mass of DF coil set (tonne)	0.554
total coil-set mass (tonne)	648.482
PFC (FW/SW/B/R/S/C) mass (tonne)	1281.972
mass power density, MPD (kWe/tonne)	756.643
system power density (MWt/m ³)	13.193
mass utilization (tonne/MWt)	0.437
mass of PFC structure (tonne)	313.897
blanket power density (MW/m ³)	24.119
magnetic energy recovery time (s)	1.826

A.3. COST SUMMARY OF TITAN-I REACTOR

fusion reactor economic evaluation (ver. 4.5)

d 11/24/87

acc. no.	Account title	million dollars (1986)
20. 1.	land & privilege acquisition	4.500
20. 2.	relocation of buildings, utilities, highways, etc.	0.500
20.	land & land rights	5.000
21. 1. 1.	general yard improvements	
21. 1. 2.	waterfront improvements	
21. 1. 3.	transportation access (off site)	
21. 1.	site improvements & facilities	15.208
21. 2. 1.	basic building structures	69.719
21. 2. 2.	building services	2.696
21. 2. 3.	containment structures	60.446
21. 2. 4.	architectural	10.111
21. 2.	reactor building	122.983
21. 3. 1.	basic building structures	60.446
21. 3. 2.	building services	2.696
21. 3. 3.	architectural	2.027
21. 3.	turbine building	45.165
21. 4. 1.	intake structures	
21. 4. 2.	discharge structures	
21. 4. 3.	unpressurized intake & discharge conduits	
21. 4. 4.	recirculating structures	
21. 4. 5.	cooling tower systems	
21. 4.	cooling system structures	10.386
21. 5. 1.	basic building structures	10.826
21. 5. 2.	building services	0.715
21. 5. 3.	architectural	0.809
21. 5.	power supply & energy storage building	12.350
21. 6. 1.	reactor auxiliaries building (incl. switchgear bay)	4.395
21. 6. 2.	hot cell building	72.749
21. 6. 3.	fuel storage building	11.635
21. 6. 4.	control room building	4.179
21. 6. 5.	diesel generator building	2.764
21. 6. 6.	administration building	1.173
21. 6. 7.	service building	2.535
21. 6. 8.	cryogenics building	1.227
21. 6. 9.	miscellaneous structures & building work	2.481
21. 6.	miscellaneous buildings	103.137
21. 7.	ventilation stack	2.440
21.98.	spare parts allowance	
21.99.	contingency allowance	
21.	structures & site facilities	311.669

22. 1. 1. 1.	breeding material (incl. tritium breeding)	12.933	
22. 1. 1. 2.	first wall & structural material	20.149	
22. 1. 1. 3.	attenuators, reflectors, & multipliers		
22. 1. 1. 4.	wall modifiers (coatings, liners, limiters, etc.)		
22. 1. 1. 5.	reflector	29.038	
22. 1. 1. 6.	other		
22. 1. 1.	rw/sh/blanket/reflector		52.017
22. 1. 2. 1.	primary	57.839	
22. 1. 2. 2.	secondary	6.870	
22. 1. 2.	shield		44.705
22. 1. 3. 1.	toroidal field coils	0.000	
22. 1. 3. 2.	ohmic-heating coils	30.058	
22. 1. 3. 3.	equilibrium field coils	53.439	
22. 1. 3. 4.	divertor field coils	0.097	
22. 1. 3. 5.	other coils		
22. 1. 3.	magnets		83.594
22. 1. 4. 1.	beam heating (neutral, ion or electron)		
22. 1. 4. 2.	rf heating	0.000	
22. 1. 4. 3.	laser heating		
22. 1. 4. 4.	other heating systems		
22. 1. 4.	supplemental heating systems		
22. 1. 5. 1.	reactor structure		
22. 1. 5. 2.	equipment support structure		
22. 1. 5.	primary structure & support		7.936
22. 1. 6. 1.	plasma chamber vacuum (incl. pumps/comp./pipe)	6.562	
22. 1. 6. 2.	inert gas vacuum (incl. pumps/comp./pipe)		
22. 1. 6. 3.	suppl. heating vacuum (incl. pumps/comp./pipe)		
22. 1. 6. 4.	direct converter vacuum (incl. pumps/comp./pipe)		
22. 1. 6. 5.	reactor vacuum system (low grade)		
22. 1. 6. 6.	reactor vacuum wall	10.236	
22. 1. 6.	reactor vacuum systems (unless integral elsewhere)		16.799
22. 1. 7. 1.	EF-coil power supplies	13.986	
22. 1. 7. 2.	OH-coil power supplies	11.137	
22. 1. 7. 3.	TF-coil power supplies	32.622	
22. 1. 7. 4.	RF-coil power supplies	45.041	
22. 1. 7. 5.	other power supplies	2.748	
22. 1. 7. 6.	current drive power supplies	36.333	
22. 1. 7. 7.	control system		
22. 1. 7. 8.	central energy storage		
22. 1. 7. 9.	other incl. buffers	6.281	
22. 1. 7.	power supply, switching & energy storage		146.948
22. 1. 8.	impurity control		6.727
22. 1. 9. 1.	vacuum tank		
22. 1. 9. 2.	direct converter modules		
22. 1. 9. 3.	thermal panels		
22. 1. 9. 4.	power conditioning equipment		
22. 1. 9.	direct energy conversion system		
22. 1. 10.	exch breakdown system		2.147
22. 1.	reactor equipment		360.371

22. 1. 1. 1.	pumps & motor drives(modular & nonmodular)	
22. 2. 1. 2.	pipings	
22. 2. 1. 3.	heat exchangers	
22. 2. 1. 4.	tanks(dump,make-up,clean-up,trit.,hot storage)	
22. 2. 1. 5.	clean-up system	
22. 2. 1. 6.	thermal insulation, piping & equipment	
22. 2. 1. 7.	tritium extraction	
22. 2. 1. 8.	pressurizer	
22. 2. 1. 9.	other	
22. 2. 1.	primary coolant system	161.286
22. 2. 2. 1.	pumps & motor drives(modular & nonmodular)	
22. 2. 2. 2.	pipings	
22. 2. 2. 3.	heat exchangers	
22. 2. 2. 4.	tanks(dump,make-up,clean-up,trit.,hot storage)	
22. 2. 2. 5.	clean-up system	
22. 2. 2. 6.	thermal insulation, piping & equipment	
22. 2. 2. 7.	tritium extraction	
22. 2. 2. 8.	pressurizer	
22. 2. 2. 9.	other	
22. 2. 2.	intermediate coolant system	
22. 2. 3. 1.	pumps & motor drives(modular & nonmodular)	
22. 2. 3. 2.	pipings	
22. 2. 3. 3.	heat exchangers	
22. 2. 3. 4.	tanks(dump,make-up,clean-up,trit.,hot storage)	
22. 2. 3. 5.	clean-up system	
22. 2. 3. 6.	thermal insulation, piping & equipment	
22. 2. 3. 7.	tritium extraction	
22. 2. 3. 8.	pressurizer	
22. 2. 3. 9.	other	
22. 2. 3.	secondary coolant system	0.000
22. 2.	main heat transfer & transport systems	161.286
22. 3. 1. 1.	refrigeration	
22. 3. 1. 2.	pipings	
22. 3. 1. 3.	fluid circulation driving system	
22. 3. 1. 4.	tanks	
22. 3. 1. 5.	purification	
22. 3. 1.	magnet cooling system	
22. 3. 2. 1.	refrigeration	
22. 3. 2. 2.	pipings	
22. 3. 2. 3.	fluid circulation driving system	
22. 3. 2. 4.	tanks	
22. 3. 2. 5.	purification	
22. 3. 2.	shield & structure cooling system	2.651
22. 3. 3. 1.	refrigeration	
22. 3. 3. 2.	pipings	
22. 3. 3. 3.	fluid circulation driving system	
22. 3. 3. 4.	tanks	
22. 3. 3. 5.	purification	
22. 3. 3.	supplemental heating system cooling system	
22. 3. 4. 1.	refrigeration	
22. 3. 4. 2.	pipings	
22. 3. 4. 3.	fluid circulation driving system	
22. 3. 4. 4.	tanks	
22. 3. 4. 5.	purification	
22. 3. 4.	power supply cooling system	
22. 3. 5.	other cooling systems (ECCS)	
22. 3.	auxiliary cooling systems	2.651

22. 4. 1.	liquid waste processing & equipment	
22. 4. 2.	gaseous wastes & off-gas processing system	
22. 4. 3.	solid waste processing equipment	
22. 4.	radioactive waste treatment & disposal	4.744
22. 5. 1.	fuel injection systems	10.001
22. 5. 2.	fuel processing systems	13.701
22. 5. 3.	fuel storage	5.000
22. 5. 4.	atmospheric tritium recovery	2.131
22. 5. 5.	water tritium recovery systems	5.000
22. 5. 6.	blanket tritium recovery systems	5.000
22. 5. 7.	other	
22. 5.	fuel handling & storage systems	40.834
22. 6. 1. 1.	blanket & coil maintenance equipment	
22. 6. 1. 2.	components rotated into service to allow maint.	
22. 6. 1. 3.	other maintenance equipment	
22. 6. 1.	maintenance equipment	
22. 6. 2.	special heating systems(start-up, t. ace, etc.)	
22. 6. 3.	coolant receiving, storage & make-up systems	
22. 6. 4.	gas systems	
22. 6. 5.	building vacuum systems	
22. 6.	other reactor plant equipment	43.289
22. 7. 1.	reactor i/c equip.(burn control, diagnostics, etc.	
22. 7. 2.	radiation monitoring systems	
22. 7. 3.	isolated indicating & recording gauges, etc.	
22. 7.	instrumentation & control(i/c)	31.561
22.98.	spare parts allowance	
22.99.	contingency allowance	
22.	reactor plant equipment	644.742

23. 1. 1.	turbine-generators & accessories	
23. 1. 2.	foundations	
23. 1. 3.	standby exciters	
23. 1. 4.	lubricating system	
23. 1. 5.	gas systems	
23. 1. 6.	reheaters	
23. 1. 7.	shielding	
23. 1. 8.	weather-proof housing	
23. 1.	turbine-generators	96.606
23. 2.	main steam (or other fluid) system	6.640
23. 3. 1.	water intake common facilities	
23. 3. 2.	circulating water systems	
23. 3. 3.	cooling towers	
23. 3. 4.	other systems which reject heat to the atmosphere	
23. 3.	heat rejection systems	31.843
23. 4. 1.	condensers	
23. 4. 2.	condensate system	
23. 4. 3.	gas removal system	
23. 4. 4.	turbine by-pass systems(excl. piping)	
23. 4.	condensing systems	23.415
23. 5. 1.	regenerators & recuperators	
23. 5. 2.	pumps	
23. 5. 3.	tanks	
23. 5.	feed heating system	10.446
23. 6. 1.	turbine auxiliaries	
23. 6. 2.	auxiliaries cooling system(excl. piping)	
23. 6. 3.	make-up treat. system(excl. piping)	
23. 6. 4.	chemical treat. & condensate purification systems	
23. 6. 5.	central lubrication service system(excl. piping)	
23. 6.	other turbine plant equipment	64.284
23. 7.	instrumentation & control(isc) equipment	11.355
23.98.	spare parts allowance	
23.99.	contingency allowance	
23.	turbine plant equipment	244.593

24. 1. 1.	generator circuits	
24. 1. 2.	station service	
24. 1.	switchgear	14.969
24. 2. 1.	station service & startup transformers	
24. 2. 2.	low voltage unit substation & lighting transformer	
24. 2. 3.	battery system	
24. 2. 4.	diesel engine generators	
24. 2. 5.	gas turbine generators	
24. 2. 6.	motor generator sets	
24. 2.	station service equipment	24.715
24. 3. 1.	main control board for electric system	
24. 3. 2.	auxiliary power & signal boards	
24. 3.	switchboards (incl. heat tracing)	9.399
24. 4. 1.	gen. station grounding sys. & cathodic protection	
24. 4.	protective equipment	2.845
24. 5. 1.	concrete cable tunnels, trenches & envelopes	
24. 5. 2.	cable trays & support	
24. 5. 3.	conduit	
24. 5. 4.	other structures	
24. 5.	electrical structures & wiring containers	22.582
24. 6. 1.	generator circuits wiring	
24. 6. 2.	station service power wiring	
24. 6. 3.	control wiring	
24. 6. 4.	instrument wiring	
24. 6. 5.	containment penetrations	
24. 6.	power & control wiring	46.729
24. 7. 1.	reactor building lighting	
24. 7. 2.	turbine building lighting	
24. 7. 3.	reactor auxiliaries building lighting	
24. 7. 4.	radioactive waste building lighting	
24. 7. 5.	fuel storage building lighting	
24. 7. 6.	miscellaneous buildings lighting	
24. 7. 7.	yard lighting	
24. 7.	electrical lighting	11.055
24.98.	spare parts allowance	
24.99.	contingency allowance	
24.	electric plant equipment	132.294

25. 1. 1.	cranes, hoists, monorails, & conveyors	
25. 1. 2.	railway	
25. 1. 3.	roadway equipment	
25. 1. 4.	watercraft	
25. 1. 5.	vehicle maintenance equipment	
25. 1.	transportation & lifting equipment	21.140
25. 2. 1.	air systems(excl. piping)	
25. 2. 2.	water systems(excl. piping)	
25. 2. 3.	auxiliary heating boilers(excl. piping)	
25. 2.	air & water service systems	16.650
25. 3. 1.	local communications systems	
25. 3. 2.	signal systems	
25. 3.	communications equipment	8.386
25. 4. 1.	safety equipment	
25. 4. 2.	shop, laboratory, & test equipment	
25. 4. 3.	office equipment & furnishings	
25. 4. 4.	change room equipment	
25. 4. 5.	environmental monitoring equipment	
25. 4. 6.	dining facilities	
25. 4.	furnishings & fixtures	1.618
25.98.	spare parts allowance	
25.99.	contingency allowance	
25.	miscellaneous plant equipment	47.796
26. 1.	reactor coolant	98.653
26. 2.	intermediate coolant	
26. 3.	turbine cycle working fluids	
26. 4.	other materials	0.337
26. 5.	Ar reactor-building cover gas	0.174
26.98.	spare parts allowance	
26.99.	contingency allowance	
26.	special materials	99.166

Los Alamos fusion reactor economic evaluation (ver. 4.5)

d 11/24/87

acc. no.	account title	million dollars (1986)
20.	land & land rights	5.000
21.	structures & site facilities	311.669
22.	reactor plant equipment	644.742
22. 1. 1.	PW/SW/blanket/reflector	52.017
22. 1. 2.	shield	44.709
22. 1. 3.	magnets	83.594
22. 1. 4.	supplemental heating systems	0.000
22. 1. 5.	primary structure & support	7.936
22. 1. 6.	reactor vacuum systems(unless integral elsewhere)	16.799
22. 1. 7.	power supply, switching & energy storage	146.948
22. 1. 8.	impurity control	6.227
22. 1. 9.	direct energy conversion system	0.000
22. 1.10.	scrb breakdown system	2.142
22. 1.	reactor equipment	360.371
23.	turbine plant equipment	244.593
24.	electric plant equipment	132.294
25.	miscellaneous plant equipment	47.794
26.	special materials	99.166
90.	direct cost (not incl. spares and contingency)	1485.298
91.	construction services & equipment	148.526
92.	home office engineering & services	148.526
93.	field office engineering & services	148.526
94.	owner's cost	74.263
96.	project contingency	148.526
97.	interest during construction (IDC)	242.067
99.	total cost	1395.691

				constant	then-current
thermal power (MWth)	= 2935.19	unit direct cost (\$/kWe)	[90]=	1531.20	1531.20
gross electric power (MWe)	= 1291.48	unit base cost (\$/kWe)	[94]=	2220.24	2220.24
net electric power (MWe)	= 969.99	unit total cost (\$/kWe)	[99]=	2469.87	3384.60
1/recirculating power fraction Q ₈	= 4.017	capital return (mill/kWeh)	=	29.72	69.24
plant availability factor	= 0.759	O&M(2.00%) (mill/kWeh) [40-47.51]	=	6.68	9.47
construction= 6 yr: constant then-current		M/W/E replace. (mill/kWeh) [50]	=	3.26	4.62
PCR= 0.0800 0.1360		decommissioning allowance	=	0.00	0.00
fIDC= 0.1124 0.2444		deuterium fuel (mill/kWeh) [02]	=	0.04	0.04
fEDC= 0.0000 0.2800		COE (mill/kWeh)	=	39.69	83.38

A.4. COST DATA BASE FOR TITAN-I REACTOR

cost data base (ver. 4.3)

date:11/24/87

acc. no.	account title UC(1986) = 1.348 * UC(1980)	unit cost(1980)	no. of units	refs.
20. 1. 0. 0	land & privilege acquisition	1.116e+01 \$/acre	1.000e+03	10
20. 2. 0. 0	relocation of buildings, utilities, highways, etc.	3.710e+02 \$/acre	1.000e+03	10
20. 0. 0. 0	land & land rights	0.	1.000e+00	
21. 1. 1. 0	general yard improvements	0.	1.000e+00	
21. 1. 2. 0	waterfront improvements	0.	1.000e+00	
21. 1. 3. 0	transportation access (off site)	0.	1.000e+00	
21. 1. 0. 0	site improvements & facilities	1.126e+07 \$	1.000e+00	10
21. 2. 1. 0	basic building structures	3.000e+02 \$/m ²	1.724e+05	13,14
21. 2. 2. 0	building services	2.000e+06 \$	1.000e+00	13,14,*
21. 2. 3. 0	containment structures	3.000e+07 \$	1.000e+00	13,14,*
21. 2. 4. 0	architectural	7.500e+06 \$	1.000e+00	13,14,*
21. 2. 0. 0	reactor building	0.	1.000e+00	
21. 3. 1. 0	basic building structures	3.000e+07 \$	1.000e+00	13,14,18
21. 3. 2. 0	building services	2.000e+06 \$	1.000e+00	13,14,*
21. 3. 3. 0	architectural	1.500e+06 \$	1.000e+00	13
21. 3. 0. 0	turbine building	0.	1.000e+00	
21. 4. 1. 0	intake structures	0.	1.000e+00	
21. 4. 2. 0	discharge structures	0.	1.000e+00	
21. 4. 3. 0	unpressurized intake & discharge conduits	0.	1.000e+00	
21. 4. 4. 0	recirculating structures	0.	1.000e+00	
21. 4. 5. 0	cooling tower systems	0.	1.000e+00	
21. 4. 0. 0	cooling system structures	8.982e+03 \$/unit	8.377e+00	2,13,14
21. 5. 1. 0	basic building structures	8.030e+06 \$	1.000e+00	13,14
21. 5. 2. 0	building services	5.300e+03 \$	1.000e+00	13,14
21. 5. 3. 0	architectural	6.000e+03 \$	1.000e+00	13
21. 5. 0. 0	power supply & energy storage building	0.	1.000e+00	
21. 6. 1. 0	reactor auxiliary building(incl. switchgear bay)	3.268e+06 \$	1.000e+00	13,14
21. 6. 2. 0	hot cell building	5.396e+07 \$	1.000e+00	13,14
21. 6. 3. 0	fuel storage building	8.610e+06 \$	1.000e+00	13,14
21. 6. 4. 0	control room building	3.100e+06 \$	1.000e+00	13,14
21. 6. 5. 0	diesel generator building	1.450e+06 \$	1.000e+00	13,14
21. 6. 6. 0	administration building	8.700e+05 \$	1.000e+00	13,14
21. 6. 7. 0	service building	1.880e+06 \$	1.000e+00	13,14
21. 6. 8. 0	cryogenics building	9.100e+05 \$	1.000e+00	13,14
21. 6. 9. 0	miscellaneous structures & building work	1.640e+06 \$	1.000e+00	13,14
21. 6. 0. 0	miscellaneous buildings	0.	1.000e+00	
21. 7. 0. 0	ventilation stack	1.610e+06 \$	1.000e+00	13,14
21. 98. 0. 0	spare parts allowance	0.	-1.000e+00	2,13,14
21. 99. 0. 0	contingency allowance	0.	-1.000e+00	2,13,14
21. 0. 0. 0	structures & site facilities	0.	1.000e+00	

22. 1. 1. 1	breeding material (incl. tritium breeding)	2.927e+02 \$/kg	3.252e+04 15,18
22. 1. 1. 2	first wall & structural material	1.855e+02 \$/kg	4.058e+04 10
22. 1. 1. 3	attenuators, reflectors, & multipliers	0.	1.000e+00
22. 1. 1. 4	wall modifiers (coatings, liners, limiters, etc.)	0.	1.000e+00
22. 1. 1. 5	reflector	1.855e+02 \$/kg	1.161e+05 4
22. 1. 1. 6	other	0.	1.000e+00
22. 1. 1. 0	PW/SW/blanket/reflector		
22. 1. 2. 1	primary	1.855e+02 \$/kg	1.513e+03 10
22. 1. 2. 2	secondary	1.567e+01 \$/kg	1.252e+03 10
22. 1. 2. 0	shield	0.	1.000e+00
22. 1. 3. 1	toroidal field coils	6.500e+01 \$/kg	0. 10
22. 1. 3. 2	ohmic-heating coils	6.500e+01 \$/kg	3.430e+03 10
22. 1. 3. 3	equilibrium field coils	1.300e+02 \$/kg	3.049e+03 10
22. 1. 3. 4	divertor field coils	1.300e+02 \$/kg	5.541e+02 10
22. 1. 3. 5	other coils	0.	1.000e+00
22. 1. 3. 0	magnets	0.	1.000e+00
22. 1. 4. 1	beam heating (neutral, ion or electron)	0.	1.000e+00
22. 1. 4. 2	rf heating	1.654e+00 \$/W	0. 16,17
22. 1. 4. 3	laser heating	0.	1.000e+00
22. 1. 4. 4	other heating systems	0.	1.000e+00
22. 1. 4. 0	supplemental heating systems	0.	1.000e+00
22. 1. 5. 1	reactor structure	0.	1.000e+00
22. 1. 5. 2	equipment support structure	0.	1.000e+00
22. 1. 5. 0	primary structure & support	1.125e+05 \$/m ³	5.232e+01 13,14
22. 1. 6. 1	plasma chamber vacuum (incl. pumps/comp./pipe)	1.500e+01 \$/kg	3.245e+05 9
22. 1. 6. 2	magnet dwarf vacuum (incl. pumps/comp./pipe)	0.	1.000e+00
22. 1. 6. 3	suppl. heating vacuum (incl. pumps/comp./pipe)	0.	1.000e+00
22. 1. 6. 4	direct converter vacuum (incl. pumps/comp./pipe)	0.	1.000e+00
22. 1. 6. 5	reactor vacuum system (low grade)	0.	1.000e+00
22. 1. 6. 6	reactor vacuum wall	2.500e+06 \$/unit	3.837e+00 9
22. 1. 6. 0	reactor vacuum systems (unless integral elsewhere)	0.	1.000e+00
22. 1. 7. 1	EF-coil power supplies	2.173e+01 \$/kVA	4.773e+03 10
22. 1. 7. 2	OH-coil power supplies	1.855e+01 \$/kVA	4.453e+03 10
22. 1. 7. 3	TF-coil power supplies	0.811e+02 \$/kVA	2.763e+04 22
22. 1. 7. 4	DF-coil power supplies	2.361e+02 \$/kVA	1.415e+03 22
22. 1. 7. 5	other power supplies	1.000e+06	1.000e+00
22. 1. 7. 6	current drive power supplies	3.709e+01 \$/kVA	7.266e+03 10
22. 1. 7. 7	control system	0.	1.000e+00
22. 1. 7. 8	central energy storage	0.	1.000e+00
22. 1. 7. 9	other incl. busbars	4.659e+06 \$	1.000e+00
22. 1. 7. 0	power supply, switching & energy storage	0.	1.000e+00
22. 1. 8. 0	impurity control	6.600e+05 \$/m ²	6.990e+00 10
22. 1. 9. 1	vacuum tank	0.	1.000e+00
22. 1. 9. 2	direct converter modules	0.	1.000e+00
22. 1. 9. 3	thermal panels	0.	1.000e+00
22. 1. 9. 4	power conditioning equipment	0.	1.000e+00
22. 1. 9. 0	direct energy conversion system	0.	1.000e+00
22. 1.10. 0	acrb breakdown system	1.589e+06 \$	1.000e+00 13
22. 1. 0. 0	reactor equipment	0.	1.000e+00

22. 2. 1. 1	pumps & motor drives(modular & nonmodular)	0.	1.000e+00
22. 2. 1. 2	piping	0.	1.000e+00
22. 2. 1. 3	heat exchangers	0.	1.000e+00
22. 2. 1. 4	tanks(dump,make-up, clean-up, trit., hot storage)	0.	1.000e+00
22. 2. 1. 5	clean-up system	0.	1.000e+00
22. 2. 1. 6	thermal insulation, piping & equipment	0.	1.000e+00
22. 2. 1. 7	tritium extraction	0.	1.000e+00
22. 2. 1. 8	pressurizer	0.	1.000e+00
22. 2. 1. 9	other	0.	1.000e+00
22. 2. 1. 0	primary coolant system	4.076e+04 \$/MWh	2.925e+02 10
22. 2. 2. 1	pumps & motor drives(modular & nonmodular)	0.	1.000e+00
22. 2. 2. 2	piping	0.	1.000e+00
22. 2. 2. 3	heat exchangers	0.	1.000e+00
22. 2. 2. 4	tanks(dump,make-up, clean-up, trit., hot storage)	0.	1.000e+00
22. 2. 2. 5	clean-up system	0.	1.000e+00
22. 2. 2. 6	thermal insulation, piping & equipment	0.	1.000e+00
22. 2. 2. 7	tritium extraction	0.	1.000e+00
22. 2. 2. 8	pressurizer	0.	1.000e+00
22. 2. 2. 9	other	0.	1.000e+00
22. 2. 2. 0	intermediate coolant system	0.	1.000e+00
22. 2. 3. 1	pumps & motor drives(modular & nonmodular)	0.	1.000e+00
22. 2. 3. 2	piping	0.	1.000e+00
22. 2. 3. 3	heat exchangers	0.	1.000e+00
22. 2. 3. 4	tanks(dump,make-up, clean-up, trit., hot storage)	0.	1.000e+00
22. 2. 3. 5	clean-up system	0.	1.000e+00
22. 2. 3. 6	thermal insulation, piping & equipment	0.	1.000e+00
22. 2. 3. 7	tritium extraction	0.	1.000e+00
22. 2. 3. 8	pressurizer	0.	1.000e+00
22. 2. 3. 9	other	0.	1.000e+00
22. 2. 3. 0	secondary coolant system	5.238e+05 \$/MWh	2.935e+02 10
22. 2. 0. 0	main heat transfer & transport systems	0.	1.000e+00
22. 3. 1. 1	refrigeration	7.546e+01 \$/unit	3.471e+01 23
22. 3. 1. 2	piping	0.	1.000e+00
22. 3. 1. 3	fluid circulation driving system	0.	1.000e+00
22. 3. 1. 4	tanks	0.	1.000e+00
22. 3. 1. 5	purification	0.	1.000e+00
22. 3. 1. 0	magnet cooling system	0.	1.000e+00
22. 3. 2. 1	refrigeration	0.	1.000e+00
22. 3. 2. 2	piping	0.	1.000e+00
22. 3. 2. 3	fluid circulation driving system	0.	1.000e+00
22. 3. 2. 4	tanks	0.	1.000e+00
22. 3. 2. 5	purification	0.	1.000e+00
22. 3. 2. 0	shield & structure cooling system	6.700e+02 \$/MWh	2.935e+01 11
22. 3. 3. 1	refrigeration	0.	1.000e+00
22. 3. 3. 2	piping	0.	1.000e+00
22. 3. 3. 3	fluid circulation driving system	0.	1.000e+00
22. 3. 3. 4	tanks	0.	1.000e+00
22. 3. 3. 5	purification	0.	1.000e+00
22. 3. 3. 0	supplemental heating system cooling system	0.	1.000e+00
22. 3. 4. 1	refrigeration	0.	1.000e+00
22. 3. 4. 2	piping	0.	1.000e+00
22. 3. 4. 3	fluid circulation driving system	0.	1.000e+00
22. 3. 4. 4	tanks	0.	1.000e+00
22. 3. 4. 5	purification	0.	1.000e+00
22. 3. 4. 0	power supply cooling system	0.	1.000e+00
22. 3. 5. 0	other cooling systems (ECCS)	0.	1.000e+00
22. 3. 0. 0	auxiliary cooling systems	0.	1.000e+00

22. 4. 1. 0	liquid waste processing & equipment	0.	1.000e+00	
22. 4. 2. 0	gaseous wastes & off-gas processing system	0.	1.000e+00	
22. 4. 3. 0	solid waste processing equipment	0.	1.000e+00	
22. 4. 0. 0	radioactive waste treatment & disposal	1.200e+03 \$/MWh	2.935e+03	13,14
22. 5. 1. 0	fuel injection systems	3.709e+06 \$	2.000e+00	10
22. 5. 2. 0	fuel processing systems	3.709e+04 \$/unit	2.740e+02	10
22. 5. 3. 0	fuel storage	3.707e+06 \$	1.000e+00	10
22. 5. 4. 0	atmospheric tritium recovery	1.484e+04 \$/m 1/hr	1.063e+02	10
22. 5. 5. 0	water tritium recovery systems	3.709e+06 \$	1.000e+00	10
22. 5. 6. 0	blanket tritium recovery systems	3.709e+06 \$	1.000e+00	10
22. 5. 7. 0	other	0.	1.000e+00	
22. 5. 0. 0	fuel handling & storage systems	0.	1.000e+00	
22. 6. 1. 1	blanket & coil maintenance equipment	0.	1.000e+00	
22. 6. 1. 2	components rotated into service to allow maint.	0.	1.000e+00	
22. 6. 1. 3	other maintenance equipment	0.	1.000e+00	
22. 6. 1. 0	maintenance equipment	0.	1.000e+00	
22. 6. 2. 0	special heating systems(start-up, trace, etc.)	0.	1.000e+00	
22. 6. 3. 0	coolant receiving, storage & make-up systems	0.	1.000e+00	
22. 6. 4. 0	gas systems	0.	1.000e+00	
22. 6. 5. 0	building vacuum systems	0.	1.000e+00	
22. 6. 0. 0	other reactor plant equipment	1.094e+04 \$/MWh	2.935e+03	13,14
22. 7. 1. 0	reactor i/c equip.(burn control, diagnostics, etc.)	0.	1.000e+00	
22. 7. 2. 0	radiation monitoring systems	0.	1.000e+00	
22. 7. 3. 0	isolated indicating & recording gauges, etc.	0.	1.000e+00	
22. 7. 0. 0	instrumentation & control(i/c)	2.341e+07 \$	1.000e+00	13,14
22.99. 0. 0	spare parts allowance	9. fraction	-1.000e+00	2,13,14
22.99. 0. 0	contingency allowance	9. fraction	-1.000e+00	2,13,14
22. 0. 0. 0	reactor plant equipment	0.	1.000e+00	

23. 1. 1. 0	turbine-generators & accessories	0.	1.000e+00	
23. 1. 2. 0	foundations	0.	1.000e+00	
23. 1. 3. 0	standby exciters	0.	1.000e+00	
23. 1. 4. 0	lubricating system	0.	1.000e+00	
23. 1. 5. 0	gas systems	0.	1.000e+00	
23. 1. 6. 0	reheaters	0.	1.000e+00	
23. 1. 7. 0	shielding	0.	1.000e+00	
23. 1. 8. 0	weather-proof housing	0.	1.000e+00	
23. 1. 0. 0	turbine-generators	4.754e+05 \$/unit	1.506e+02	1,13,14
23. 2. 0. 0	main steam (or other fluid) system	1.678e+03 \$/MWch	2.935e+03	12
23. 3. 1. 0	water intake common facilities	0.	1.000e+00	
23. 3. 2. 0	circulating water systems	0.	1.000e+00	
23. 3. 3. 0	cooling towers	0.	1.000e+00	
23. 3. 4. 0	other systems which reject heat to the atmosphere	0.	1.000e+00	
23. 3. 0. 0	heat rejection systems	6.319e+04 \$/unit	3.718e+02	10
23. 4. 1. 0	condensers	0.	1.000e+00	
23. 4. 2. 0	condensate system	0.	1.000e+00	
23. 4. 3. 0	gas removal system	0.	1.000e+00	
23. 4. 4. 0	turbine by-pass systems(excl. piping)	0.	1.000e+00	
23. 4. 0. 0	condensing systems	2.753e+04 \$/unit	6.165e+02	2,13,14
23. 5. 1. 0	regenerators & recuperators	0.	1.000e+00	
23. 5. 2. 0	pumps	0.	1.000e+00	
23. 5. 3. 0	tanks	0.	1.000e+00	
23. 5. 4. 0	feed heating system	2.440e+03 \$/unit	2.935e+03	2,12,14
23. 6. 1. 0	turbine auxiliaries	0.	1.000e+00	
23. 6. 2. 0	auxiliary cooling system(excl. piping)	0.	1.000e+00	
23. 6. 3. 0	make-up treat. system(excl. piping)	0.	1.000e+00	
23. 6. 4. 0	chemical treat. & condensate purification systems	0.	1.000e+00	
23. 6. 5. 0	central lubrication service system(excl. piping)	0.	1.000e+00	
23. 6. 0. 0	other turbine plant equipment	6.482e+05 \$/unit	7.356e+01	2,13,14
23. 7. 0. 0	instrumentation & control(i&c) equipment	9.420e+05 \$/unit	8.577e+00	1,13,14
23.98. 0. 0	spare parts allowance	0.	fraction	-1.000e+00 2,13,14
23.99. 0. 0	contingency allowance	0.	fraction	-1.000e+00 2,13,14
23. 0. 0. 0	turbine plant equipment	0.	1.000e+00	

24. 1. 1. 0	generator circuits	0.	1.000e+00	
24. 1. 2. 0	station service	0.	1.000e+00	
24. 1. 0. 0	switchgear	8.600e+03 \$/unit	1.291e+03	2,13,14
24. 2. 1. 0	station service & startup transformers	0.	1.000e+00	
24. 2. 2. 0	low voltage unit substation & lightning transformer	0.	1.000e+00	
24. 2. 3. 0	battery system	0.	1.000e+00	
24. 2. 4. 0	diesel engine generators	0.	1.000e+00	
24. 2. 5. 0	gas turbine generators	0.	1.000e+00	
24. 2. 6. 0	motor generator sets	0.	1.000e+00	
24. 2. 0. 0	station service equipment	1.420e+04 \$/unit	1.291e+03	13,16
24. 3. 1. 0	main control board for electric system	0.	1.000e+00	
24. 3. 2. 0	auxiliary power & signal boards	0.	1.000e+00	
24. 3. 0. 0	switchboards (incl. heat tracing)	5.400e+03 \$/unit	1.291e+03	2,13,14
24. 4. 1. 0	gen. station grounding sys. & cathodic protection	0.	1.000e+00	
24. 4. 0. 0	protective equipment	2.110e+06 \$	1.000e+00	13,14
24. 5. 1. 0	concrete cable tunnels, trenches & envelopes	0.	1.000e+00	
24. 5. 2. 0	cable trays & support	0.	1.000e+00	
24. 5. 3. 0	conduit	0.	1.000e+00	
24. 5. 4. 0	other structures	0.	1.000e+00	
24. 5. 0. 0	electrical structures & wiring containers	1.675e+07 \$	1.000e+00	13,14,+
24. 6. 1. 0	generator circuits wiring	0.	1.000e+00	
24. 6. 2. 0	station service power wiring	0.	1.000e+00	
24. 6. 3. 0	control wiring	0.	1.000e+00	
24. 6. 4. 0	instrument wiring	0.	1.000e+00	
24. 6. 5. 0	containment penetrations	0.	1.000e+00	
24. 6. 0. 0	power & control wiring	3.466e+07 \$	1.000e+00	13,14,+
24. 7. 1. 0	reactor building lighting	0.	1.000e+00	
24. 7. 2. 0	turbine building lighting	0.	1.000e+00	
24. 7. 3. 0	reactor auxiliary building lighting	0.	1.000e+00	
24. 7. 4. 0	radioactive waste building lighting	0.	1.000e+00	
24. 7. 5. 0	fuel storage building lighting	0.	1.000e+00	
24. 7. 6. 0	miscellaneous buildings lighting	0.	1.000e+00	
24. 7. 7. 0	yard lighting	0.	1.000e+00	
24. 7. 0. 0	electrical lighting	8.200e+06 \$	1.000e+00	13,14,+
24.98. 0. 0	spare parts allowance	0. fraction	-1.000e+00	2,13,14
24.99. 0. 0	contingency allowance	0. fraction	-1.000e+00	2,13,14
24. 0. 0. 0	electric plant equipment	0.	1.000e+00	

25. 1. 1. 0	cranes, hoists, monorails, & conveyors	0.	1.000e+00	
25. 1. 2. 0	railway	0.	1.000e+00	
25. 1. 3. 0	roadway equipment	0.	1.000e+00	
25. 1. 4. 0	watercraft	0.	1.000e+00	
25. 1. 5. 0	vehicle maintenance equipment	0.	1.000e+00	
25. 1. 0. 0	transportation & lifting equipment	1.560e+07 \$	1.000e+00	13.14
25. 2. 1. 0	air systems(excl. piping)	0.	1.000e+00	
25. 2. 2. 0	water systems(excl. piping)	0.	1.000e+00	
25. 2. 3. 0	auxiliary heating boilers(excl. piping)	0.	1.000e+00	
25. 2. 0. 0	air & water service systems	1.235e+07 \$	1.000e+00	13.14
25. 3. 1. 0	local communications systems	0.	1.000e+00	
25. 3. 2. 0	signal systems	0.	1.000e+00	
25. 3. 0. 0	communications equipment	6.220e+06 \$	1.000e+00	13.14
25. 4. 1. 0	safety equipment	0.	1.000e+00	
25. 4. 2. 0	shop, laboratory, & test equipment	0.	1.000e+00	
25. 4. 3. 0	office equipment & furnishings	0.	1.000e+00	
25. 4. 4. 0	change room equipment	0.	1.000e+00	
25. 4. 5. 0	environmental monitoring equipment	0.	1.000e+00	
25. 4. 0. 0	dining facilities	0.	1.000e+00	
25. 4. 0. 0	furnishings & fixtures	1.200e+06 \$	1.000e+00	2
25.98. 0. 0	spare parts allowance	0.	fraction	-1.000e+00 2.13.14
25.99. 0. 0	contingency allowance	0.	fraction	-1.000e+00 2.13.14
25. 0. 0. 0	miscellaneous plant equipment	0.	1.000e+00	
26. 1. 0. 0	reactor coolant	2.927e+02 \$/kg	2.500e+05	15.10
26. 2. 0. 0	intermediate coolant	0.	1.000e+00	
26. 3. 0. 0	turbine cycle working fluids	0.	1.000e+00	
26. 4. 0. 0	other materials	2.500e+05 \$	1.000e+00	13.14
26. 5. 0. 0	Ar reactor-building cover gas	7.500e-01 \$/m ³	1.724e+05	
26.98. 0. 0	spare parts allowance	0.	fraction	-1.000e+00
26.99. 0. 0	contingency allowance	0.	fraction	-1.000e+00
26. 0. 0. 0	special materials	0.	1.000e+00	

90. 0. 0. 0	direct cost (not incl. spares and contingency)	0.	1.000e+00	
91. 0. 0. 0	construction services & equipment	1.000e-01	fraction	-1.000e+00 4,10
92. 1. 0. 0	systems engineering	0.	-1.000e+00	
92. 2. 0. 0	management	0.	-1.000e+00	
92. 3. 0. 0	quality assurance	0.	-1.000e+00	
92. 4. 0. 0	safety & environmental engineering	0.	-1.000e+00	
92. 0. 0. 0	home office engineering & services	1.000e-01	fraction	-1.000e+00 4,10
93. 1. 0. 0	construction management	0.	-1.000e+00	
93. 2. 0. 0	inspection	0.	-1.000e+00	
93. 3. 0. 0	preoperational testing	0.	-1.000e+00	
93. 0. 0. 0	field office engineering & services	1.000e-01	fraction	-1.000e+00 4,10
94. 1. 0. 0	project administration	0.	-1.000e+00	
94. 2. 0. 0	staff training & plant startup	0.	-1.000e+00	
94. 3. 0. 0	inventories & spares	0.	-1.000e+00	
94. 0. 0. 0	owner's cost	5.000e-02	fraction	-1.000e+00 4,10
95. 0. 0. 0	process contingency	0.	fraction	-1.000e+00 4,10
96. 0. 0. 0	project contingency	1.000e-01	fraction	-1.000e+00 4,10
97. 0. 0. 0	interest during construction (IDC)	1.124e-01	fraction	-1.000e+00 4,10
98. 0. 0. 0	escalation during construction (EDC)	0.	fraction	-1.000e+00 4,10
99. 0. 0. 0	total cost	0.		9.700e+02

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