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MASTER **PITR**
PRINCETON IGNITION TEST REACTOR

PLASMA PHYSICS LABORATORY, PRINCETON UNIVERSITY
PRINCETON, NEW JERSEY 08544

AND

ADVANCED ENERGY SYSTEMS
GRUMMAN AEROSPACE CORPORATION
BETHPAGE, NEW YORK 11714

PPPL-1525

DECEMBER 1978

Work supported by U.S. Dept. of Energy, Office of Fusion
Energy, under Contract EY-76-C-02-3073.

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SUMMARY

The principal objectives of the PITER — Princeton Ignition Test Reactor — are to demonstrate the attainment of thermonuclear ignition in deuterium-tritium, and to develop optimal start-up techniques for plasma heating and current induction, in order to determine the most favorable means of reducing the size and cost of tokamak power reactors. This report describes the status of the plasma and engineering design features of the PITER.

The PITER geometry is chosen to provide the highest MHD-stable values of beta in a D-shaped plasma, as well as ease of access for remote handling and neutral-beam injection. The principal design approach is to minimize dependence on a central transformer core, which thereby results in a machine of small aspect ratio ($A \sim 2$ to 2.5) and small major radius ($R_0 \sim 2.8$ m) even with significant shielding inside the TF coils. Toroidal field coils of very small aspect ratio (1.45) allow (i) significant major-radius compression, (ii) an ignited low-aspect-ratio plasma with $\beta \sim 0.1$, (iii) small magnetic field at the outside major radius, permitting implementation of a bundle divertor null, as well as electron cyclotron resonant breakdown. Induction of the plasma current of up to 5 MA is achieved by an optimal combination of "leaky OH" coils, equilibrium-field flux swing, a small central solenoid and major-radius compression.

Sixteen normal-copper TF coils of the compound constant-tension shape enable low-stress operation at $B_{\max} = 12.5$ T. The TF-coil flat-top time is limited by resistive heating to 9 sec at full field, but the flat-top time increases to 18 sec at 3/4 field. The TF-coil insulation inboard of the plasma is protected for a total 14-MeV neutron wall-load of 10^6 MJ/m² by a 35-cm-thick shield of stainless steel and water. The steel component also helps take up stresses in the trunk region. The vacuum vessel is constructed of thin gauge, double-wall titanium-alloy. No bellows or insulating break is required.

In the reference plasma heating scenario, a precompression plasma is heated by 60 MW of 160-keV deuteron beams. A subsequent major-radius compression of 1.45 ignites the plasma, which then expands by means of a propagating burn. Impurity control is effected by a bundle divertor during the beam-heating phase, and by a cold plasma blanket during the burn. To accommodate the bundle divertor, one TF-coil must be displaced outward.

A maximum tritium throughput of 20 kCi is required per 10 to 20-sec pulse, and the total tritium inventory is specified as 2.3 MCi. The fusion power production during the burn is 450 to 600 MW. The greatest technological advances required for the PITER are probably in the area of remote handling techniques.

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1.1 Introduction

1.0 OBJECTIVES

A possible goal of the tokamak fusion program during the next fifteen years is the development of an experimental power reactor (EPR), which would deliver net electrical energy in each pulse. In addition to making use of superconducting coils, an EPR of reasonable cost must employ special techniques that will allow start-up and control of an ignited plasma in a machine of practical size. To maximize the success of an EPR, these techniques must be introduced and tested as soon as possible in smaller, less expensive, and less risky devices. One approach toward achieving these goals is to share the development of the EPR-level reacting plasma and the EPR machine technologies between a normal-coil ignition test reactor (ITR), and a superconducting-coil long pulse experiment (SLPX) [1.1], both of which could be made operational in the mid-1980s. With this approach, illustrated in Fig. 1-1, vital technological and plasma physical information would become available at a sufficiently early date to impact the design of an EPR that could become operational in the period 1990-1992.

Equilibrium ignition of a reacting plasma denotes the state wherein the plasma temperature is maintained against transport and radiation losses solely by the energy deposition of charged fusion-reaction products. Representative conditions for ignition in a 50:50 deuterium-tritium plasma with negligible impurity content are that (i) the spatially-averaged temperature be at least 8 keV with the central temperature exceeding 12 keV; (ii) the product of spatially-averaged density and energy confinement time, $\bar{n}_E$, exceed $2.5 \times 10^{14} \text{ cm}^{-3}\text{s}$; (iii) the plasma current exceed $3 \text{ MA} \times K$, where K is the plasma elongation. Achieving net plant efficiencies of at least 30% probably requires sustained ignition for at least a 100-s pulse, although acceptable plant efficiencies could conceivably be reached with $Q_p \geq 10$, where $Q_p = (\text{fusion power/injected power})$. However, a potential problem with high- Q_p operation as a *fallback* contingency in the case of failure to reach ignition, is that a machine designed to reach ignition may call for heating strategies which are not amenable to steady-state usage (e.g., adiabatic compression). To maintain an acceptably low level of circulating power also dictates extraordinarily high efficiency of the plasma heating systems. Hence attaining ignited plasmas could well turn out to be the *sine qua non* for generating net electrical power at reasonable plant efficiency.

Before final commitment is made to construction of an EPR-type fusion facility, which is likely to cost of the order of \$200 M or more, it would seem worthwhile

Fig. 1-1

to have in hand a definitive demonstration of thermonuclear ignition. Realistic design studies tend to show that heating of a full-sized EPR plasma to ignition may well be prohibitively expensive, unless maximum possible use is made of fusion-alpha-particle heating. In order to determine the most efficient design of a power reactor, one must first determine how to optimize the alpha-particle contribution to heating. No magnetic confinement machine built or presently authorized for construction is capable of studying methods of alpha-particle heating, much less of demonstrating sustained ignition.

In this regard, an analysis was made of the possibilities of the TFTR [1.2] or JET [1.3] plasma (the latter in "extended" operation) of reaching ignition, using the following optimistic assumptions: $Z_{\text{eff}} = 1.0$; electron energy transport according to the $\bar{n}_p^2$ "empirical scaling"; ion transport nearly neoclassical; $\langle \beta \rangle = (\text{plasma pressure/magnetic field pressure}) = 0.085$ in the full-aperture D-shaped JET plasma, and 0.030 in the elongated JET plasma; and a method of heating plasmas with $\bar{n}_p = 2 \times 10^{16} \text{ cm}^{-2}$. Defining $[\bar{n}_T]_{\text{ign}}$ as the equilibrium ignition value, it was found that $[\bar{n}_T]_{\text{ign}}/[\bar{n}_T]_{\text{ign}} = 0.4$ in TFTR, and ≈ 1.0 in JET. Nevertheless, ignition in the full-aperture JET seems extraordinarily unlikely for a number of practical reasons: The absence of active impurity control, or even room for a cold plasma blanket when the plasma has maximum size; the need for 200-keV D⁰ heating beams; the large over-turning moment on the TF coils, and the increased stress experienced because of the large vertical field required to sustain $\langle \beta \rangle = 0.085$, make it doubtful that such β -values can in fact be supported; stray field from the iron core, which makes it difficult to shape and stabilize the elongated plasma; the absence of shielding inside the TF coils, which limits the number of full-aperture "test shots" in D-T.

The possibility remains that under these same optimistic assumptions significant temperature excursions due to alpha particle heating could be observable in JET and in TFTR. The situation would then be analogous to the excursions in T_i observed in neutral-beam injection experiments in 1973-74, where the beam power was of the order of one-half or less of the ohmic power, and the ohmic input determined the plasma equilibrium. But those experiments did not prove the feasibility of neutral-beam heating to ignition. It was still necessary — and is still necessary — to demonstrate a neutral-beam sustained plasma where the beams produce large T_e and large T_i , and permit large $\bar{n}_T$ with $Z_{\text{eff}} \gtrsim 2$. Similarly, even with a temperature excursion by fusion-alpha heating in JET or TFTR, which could be observed with Q as small as 2, one must still demonstrate

an alpha-sustained plasma. It is likely that in either machine the conditions for temperature excursion would occur at maximum attainable beta and maximum equilibrium field — so that the excursion would not be observable!

If an EPR is to be seriously contemplated for operation in the early 1990s, the groundwork for this machine cannot be allowed to depend on the unlikely prospect of an ignition demonstration in JET or in TFTR. A successful program calls first for a device which has the margins of safety in confinement, in heating methods, in impurity control, and in equilibrium-field systems that insure that ignition conditions can be reached and sustained. It would seem imprudent to embark on the construction of a billion-dollar fusion installation that is intended to demonstrate net electrical power, prior to the definitive establishment of reactor-physics feasibility.

The present report describes the status of design considerations at PPPL for a copper-coil ITR, referred to as PITER, for Princeton Ignition Test Reactor. Various design approaches have been investigated for a versatile machine capable of testing a variety of plasma start-up techniques, the most promising of which would be applied to reduce the size and cost of an EPR and subsequent power reactors. The reference device described in this report has rather large margins for reaching ignition conditions, but at the penalty of a total cost estimated to be about \$270 M (when located at the TFTR site). This large cost would seem to be unavoidable at the present state of uncertainty of high-temperature tokamak plasma performance and of impurity control, if the probability of attaining ignition conditions is to be high.

1.2 Objectives of PITER

- (1) Demonstrate the attainment of thermonuclear ignition in deuterium-tritium.
- (2) Demonstrate a sustained thermonuclear burn of at least 5-s duration, with a definitive testing of fusion alpha-particle heating and confinement effects, particularly the study and control of thermal runaway.
- (3) Demonstrate an optimized prototypical plasma for power reactor design and operation, especially methods of current start-up, and of maximizing the contribution of fusion alphas in heating to ignition. An EPR would use a similar reacting plasma, but long-pulse operation would permit a duty cycle of the order of 50%, as compared with only 2% in PITER.
- (4) Demonstrate effective heat removal from the plasma and from the first wall throughout the thermonuclear burn.
- (5) Demonstrate remote maintenance and assembly of a tokamak device subject to repetitively pulsed ignited operation.

The PITER goals are to be achieved in as small a machine as possible consistent with good access for beam injection and remote handling. The methods used to facilitate small size should also be applicable for reducing the size and cost of an EPR and of subsequent power reactors. However, provisions for adequate margin in confinement, heating, and impurity control, in order to accommodate present-day uncertainties in scaling tokamak performance, have resulted in a size that is rather larger than might appear optimal several years hence. Regardless of the fate of the present design, a number of plasma and engineering innovations have been analyzed which should find application in future tokamak reactor concepts.

1.3 Use of PPPL Site

An important step toward minimizing the cost of PITER is to strive for the maximum possible usage of equipment and facilities that will be provided at the TFTR site at PPPL. The PITER would take advantage of TFTR buildings, power supplies, controls, and coolant systems. As discussed in Section 2, an upgrade of the power source would include the installation of a 1000-MVA, 500-kV substation. In order to utilize TFTR neutral-beam injectors, tritium systems, remote handling equipment, and diagnostics, the PITER could begin D-T operation only after TFTR operations had ceased. The PITER would be installed in a new containment building adjacent to the central utilities tunnel, and also adjacent to the TFTR-I test cell, which would then serve as the hot cell for PITER components. A tritium inventory of at least 2.3 MCi can be used at the TFTR site, provided that appropriate tritium containment and clean-up systems are installed. Details are given in Ref. [1.4].

1.4 References

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- [1.2] Tokamak Fusion Test Reactor Final Design Report, Princeton Plasma Physics Lab. report PPPL-1475 (August 1978)
- [1.3] The JET Project - Design Proposal for the Joint European Torus, Euratom rep. EUR-5516e (1976).
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2.0 DESIGN APPROACH

2.1 Obtaining High Plasma Pressure

From the point of view of obtaining high $n\tau_E$ and high fusion power density, P_f , and for minimizing impurity influx, it seems clear from present experiments that high performance tokamak plasmas should aim for maximum plasma density, n . The most commonly used "empirical scaling law" [2.1] predicts that $\bar{n}\tau_E \propto \bar{n}^2 a_p^2 \propto \bar{\beta}^2 a_p^2 B_t^4$, where B_t is the magnetic field at the plasma center ($r=0$), a_p is the plasma radius, and $\bar{\beta} = 8\pi\bar{p}/B_t^2$, where $\bar{p}$ is the spatially-averaged plasma pressure. It is likely that any ITR will aim for operation in a rather narrow temperature range, $T(0) \sim 10$ to 15 keV, where the fusion reactivity $\propto T^2$. If $T \approx$ constant, then $P_f \propto p^2$, neutron wall loading $\propto p^2 a_p$, and $\bar{n}\tau_E \propto p^2 a_p^2$. Evidently, a number of options exist for maximizing p . For example, the use of Bitter-type copper coils providing $B_t \geq 10$ T at $r = 0$ with $\bar{\beta} \lesssim 0.03$ is one attractive possibility under investigation at MIT [2.2]. The present PITER design philosophy, on the other hand, envisages a very small aspect ratio ($A = R_0/a_p \sim 2$ to 2.5) and modest vertical elongation in order to enhance β and provide high pressure at magnetic fields of 3.5 to 7 T. The following discussion investigates the rationale for pursuing small aspect ratio while maintaining a small major radius [2.3].

Consider two tokamak devices, depicted in Fig. 2-1, that have exactly the same physical characteristics, except for the major radii, R_0 . (For simplicity, the devices are depicted as circular, but the following discussion applies also to D-shaped plasmas and coils.) Each normal-conducting TF (toroidal-field) coil has the same bore, radial build, and maximum field B_M at the coil windings. The plasma in each device has radius a_p , and is to be heated to a given temperature, T_i . From the previous discussion, we know that if $p \approx$ constant as R_0 is varied, the reactor-plasma parameters $\bar{n}\tau_E$, P_f , and wall loading are approximately constant, so that the performance of the device is nearly independent of R_0 .

Fig. 2-1

For a given $\bar{B}$, $\bar{p}$ actually decreases as R_0 decreases — i.e., as the aspect ratio $A = R_0/a_p$ decreases — because B_t/B_M decreases. However, it is known that for a given plasma shape and safety factor q , the maximum attainable $\bar{B} \propto a_p/R_0$; this result follows either from the most rudimentary considerations of plasma equilibrium or from the most recent results of sophisticated MHD stability codes that determine the thresholds of ballooning modes [2.4]. In higher temperature, more "collisionless" plasmas, there is a small correction to the attainable $\bar{B}$ that favors plasmas of smaller A [2.5]. Figure 2-1 shows $\bar{p} \propto (\bar{p})_{\max} B_t^2$ for $a_c/a_p = 1.25$. Evidently, the attainable $\bar{p}$ changes by relatively little in the range 2.4 to 5.2, so that the attainable $n\tau_E$ and P_f are relatively constant in this range.

Now, with increasing R_0 , the facility cost tends to increase: (i) The weight of the TF (toroidal-field) coils, structure, and vacuum components, (ii) the Joule heating in the TF-coil set, (iii) the stored magnetic energy, (iv) the neutral-beam power required to reach T_i , and (v) the tritium throughput per given pulse length are all proportional to R_0 . (vi) The flux swing required to establish a given plasma current increases faster than R_0 . Thus the cost of a tokamak device of given a_p and B_M , of its auxiliary equipment, and of the associated power supplies are together roughly proportional to R_0 . A simple measure of the cost-effectiveness of the machine is then $(\bar{p})^2/R_0$, which is also shown in Fig. 2-1. Evidently, the most "cost-effective" machines by this criterion have $A \sim 2.5$. This greatly oversimplified analysis gives essentially the same result as determined by the sophisticated analysis of tokamak machine costs reported in Ref. [2.6].

In order to make realistic provision for the achievement of an effective plasma aspect ratio $A \sim 2.5$, it is actually desirable to provide a vacuum chamber and TF system that can produce ideal plasma aspect ratios as low as 2.0. The principal reasons are: (i) the need to allow for geometric effects, associated with the nonconcentricity of the tokamak flux surfaces, that result in effectively larger A ; (ii) the need to allow ample room for a cold plasma blanket between the burning plasma region and the limiter; and (iii) the desire to provide room for special start-up techniques which favor location of the initial plasma at a larger major radius than that of the final configuration (see Sect. 3).

A critical difficulty in minimizing R_0 while maintaining $A \sim 2.5$ is the need for a central current transformer, including space for the primary windings. There is also a requirement for access to the complicated core of the device, a problem that becomes especially acute in DT-burning reactors. It is this latter difficulty that has in recent years led to emphasis on the design of large-A machines, but such machines are inherently incapable of large β . As described in subsequent sections, the PITER design approach seeks to minimize the dependence on a central transformer core, and thereby offers the possibility of ideal plasma aspect ratios as small as 2.0.

2.2 Start-Up Strategy — Magnetics and Heating

Plasma Current. The design study of the PITER has aimed for small aspect ratio and small R_0 , while maintaining good access for maintenance and disassembly. To achieve this goal, some of the techniques proposed in the SMARTOR concept [2.3] have been adopted. However, the suggested use of a beam-driven current to assist in start-up has not been included, because such a current has not yet been demonstrated in the laboratory. Consequently, considerable reliance has had to be placed on PF coils located in and near the rather tight TF-coil throat region, resulting in some difficult PF coil engineering problems, as discussed in Section 5.

In order to produce a small-aspect-ratio ignited plasma at small major radius and moderate magnetic field, it is necessary to rely on the EF (equilibrium-field) coils for inducing most of the plasma current, I_p , at the same time that they provide the required vertical field [2.3]. The plasma pressure must rise in a fairly well-prescribed fashion, with neutral-beam heating initiated at a very early stage (e.g., $I_p \sim 200$ kA), both to increase β_p and to minimize the resistive volt-second requirement. The vertical field is increased to accommodate the increasing plasma pressure, and the incremental flux swing further increases I_p . Finally, the EF coils together with some moderate-current "shaping coils" must provide the proper shaping field if a D-shaped plasma is desired. Each coil requires individual programming.

Sections 3 and 5 describe start-up calculations that show the feasibility of obtaining the desired plasma current with the aid of only a small central solenoid of the conventional type. The most important start-up coils are a set of "leaky OH" coils, located just above the trunk of the TF coils, and the magnetic flux they produce extends into the vacuum vessel region. A slow hexapole null is provided at the plasma breakdown position, to exclude stray vertical field at discharge initiation. (A demonstration of current build-up by "leaky-OH" coils was made in

the ATC device.) Once these coils are discharged, further increase in plasma current is obtained either by continued heating in a stationary position with a concomitant increase in the equilibrium field, or by a rapid major-radius compression. The chief drawback of the leaky-OH method is that a power supply comparable to that required for a conventional large central solenoid may still be needed. The above description is helpful only to relate the individual coil functions to the usual performance specifications of tokamak poloidal field systems. For a system without an iron core, it turns out to be most useful to treat all the PF coils as part of a common network surrounding the plasma, without special regard to the function of each particular coil.

Confinement. Table 2-1 shows the target values for the principal plasma parameters when ignition is attained. The PITER designs include certain "margins of safety" in order to help insure realization of these parameters. Impurity control measures are introduced in order to insure that $Z_{eff} \geq 2$. (The precise limiting Z_{eff} that is allowed depends on the dominant impurity species.)

Table 2-1. Desirable Plasma Characteristics at Ignition.

Plasma current	5 MA
n	$3.5 \times 10^{14} \times (1-r^3/a^3) \text{ cm}^{-3}$
$T_e \approx T_i$	$12 \times (1-r^2/a^2) \text{ keV}$
Average beta	$\geq 6\%$
τ_E	1 s
τ -particle	$\geq 3 \text{ s}$
Burn time	$\sim 10 \text{ s}$

The energy confinement model used to scope the plasma size is some multiple of the "empirical scaling" [2.1], $[\tau_E]_{emp} = 3 \times 10^{-19} \bar{n} a_p^{1/2}$ which is based on results from a variety of low temperature plasmas. Recent measurements at $T_e(0) \geq 3 \text{ keV}$, $T_i(0) \geq 5 \text{ keV}$, indicate that confinement is at least as good at higher temperature [2.15]. For given values of B_t , safety factor q , and temperature, a vertical elongation K of the plasma should permit an increase in the maximum value of $\bar{n}$ by a factor up to $(1+K^2)/2$, for D-shaped plasmas with $K \geq 1.7$ [2.7]. It is uncertain whether the larger average minor radius itself results in an increase in τ_E , but certainly this enhancement is smaller than the factor $(1+K^2)/2$. The plasma size and density are chosen so that $[\bar{n}\tau_E]_{emp} = MI \times [\bar{n}\tau_E]_{ign}$, where MI is an "ignition margin" taken between 1.5 and 5, and $[\bar{n}\tau_E]_{ign}$ is the ignition

value. To confine fusion alpha particles in the hotter plasma region, the plasma current should be at least 3.5 MA for a circular plasma with $A = 2.5$ to 3 [2.8]. The required minimum I_p is roughly proportional to K .

Heating to Ignition. Rather good neutral beam penetration is required to maximize the efficiency of beam heating of the central plasma, to minimize sputtering of the first wall by hot neutrals escaping from an overheated plasma edge, and to minimize the loss of fast ions by charge-exchange, by limiter-intersecting orbits, and by superbanana orbits in ripple fields — all of which are enhanced at the plasma edge. The empirical energy scaling relates $\bar{n}T_E$ directly to a unique beam energy, through the plasma opacity $\bar{n}a_p$. Figure 2-2 shows the D^0 beam energy required to achieve a somewhat peaked trapping profile for near-perpendicular injection into ignition-sized plasmas [2.9]. Even for $MI = 1$, 250 keV beams are required for the temperature range of interest.

A typical PITR design has sufficient size and access for accommodating two heating techniques that allow the attainment of ignition conditions with moderate-energy beams, in the event that 250-keV beams cannot be made available by the mid-1980s. The first method, ripple-assisted beam injection [2.10], permits adequate penetration by vertically injected 90-keV D^0 beams into the center of the final ignition-sized plasma. A test of this technique is being prepared for the ISX-B tokamak at ORNL [2.11]. The second method, compression "boosting", takes advantage of the fact that $na_p \propto C^{3/2}$, where $C = (\text{initial } a_p / \text{final } a_p)$. The opacity $\bar{n}a_p$ of the precompression plasma is increased to the maximum value that permits adequate penetration by medium-energy beams. The plasma is then compressed to reach the value of na_p that is sufficiently large for ignition [2.9]. The PITR design uses $C \approx 1.45$, so that as shown in Fig. 2-3, a beam energy of 160 keV is adequate for heating. The effectiveness of compression boosting has been demonstrated on the ATC device [2.12], and will be tested further on the TFTR.

Overheating. Further reduction in neutral-beam voltage, and greater certainty in attaining ignition, can be realized by a propagating-burn start-up, in which one first "overheats" an undersized plasma, then compresses it, and finally allows the compressed fusing core to burn out into a dense cold plasma blanket until ignition size is attained. In a variation of this technique, the size of the ultrahot core (initially $T \sim 25$ keV) remains constant, while its density builds up by inward flow from a protecting ultradense cold plasma blanket; the $\bar{n}T_E$ of the core becomes sufficiently large for ignition just as the core

Fig. 2-2

Fig. 2-3

temperature has dropped to the desirable range of 10 to 15 keV. Either of these scenarios can help insure ignition if impurity influx into the unprotected beam-heated plasma cannot be avoided. These techniques have been investigated with an analytic model and 1-D transport code simulations, and the results are given in Section 3.

Thermal Stability. At ignition, the PITER plasma is likely to be unstable to thermal perturbations. If compression to ignition is used, the beta of the compressed plasma will already be near the MHD-limiting value, so that thermal runaway will take the form of a propagating burn that enlarges the plasma size, as mentioned above. Eventually, however, a thermal runaway must be braked. One promising control method is the use of special ripple coils to regulate a vertically asymmetric ripple that causes enhanced alpha-particle diffusion [2.13]. Another method is to decompress the plasma slightly, when the temperature rises above the desired value, then recompress it as the temperature falls below. However, the power requirement for the equilibrium field may have to be excessively large to effect the alternating decompression and compression on a sufficiently short time scale.

Ripple-Injection Option. If the compressed plasma is not sufficiently large for ignition, it may be necessary to utilize another option, namely heating a 3.0-MA plasma by ripple-trapping injection with 90-keV beams [2.10]. As the plasma pressure increases, the vertical field continues to increase, thus inducing more current. If necessary, this process can be continued until the plasma reaches the maximum possible size of $a_p = 1.53$ m, $K = 1.31$, with $I_p \approx 6$ MA. The bundle divertor could remain energized during this period, unless prohibited by divertor-coil heating. This large plasma, if elongated, would have an ignition margin of 4.5 at full magnetic field ($B = 4.5$ T at $R = 2.8$ m).

Current Decay Time. For the large size and plasma temperature depicted here, the resistive decay time of the plasma current is of the order of 100 s, so that there would seem to be no need to provide flux swing to sustain the final current for the flat-top times that are permitted in a normal-coil ITR. In the ATC compressed plasma ($a_p = 0.11$ m, $Z_{eff} \geq 3$, $T_e(0) \sim 2$ keV), the current stayed reasonably constant for about 5 ms, and in fact decayed according to the classical skin time [2.14]. Scaling up by $a_p^2 T_e^{3/2} / Z_{eff}$ to the largest PITER plasma gives a flat-top time for I_p of the order of 40 s. The decay time of a 1-MA compressed plasma can be investigated in the TFTR, where the flat-top duration, in the absence of further flux swing, is calculated to be about 1 s.

2.3 Physical Layout

Figures 2-4 and 2-5 show plan and elevation views of the reference design, called PITR-3, which satisfies the objectives stated in Section 1-2, and has the capability of testing the start-up techniques discussed in Section 2.2. Exceptionally large access is available for neutral-beam injection in PITR. Tangential injection and vertical injection are readily accommodated, as well as perpendicular injection in the horizontal midplane. The tangential-injection port size is 0.30 m $\times$ 0.75 m, while the vertical-injection port size is .75 m diameter. Up to 30 MW of neutral-beam power at $E_b \geq 80$ keV (D°) can be injected either in the horizontal plane, or vertically. (If it were desired to drive a beam-induced current during start-up, it would be most economical to utilize quasi-tangential injection, with the same set of injectors used first to produce the beam-driven current in a relatively tenuous plasma, and later operated at a higher voltage to heat a denser plasma.)

Enlargement of the bore of one TF coil enables the installation of a bundle divertor with adequate room for conductor current, structure, and shielding. (The TF coil on the opposite side of the machine must also be enlarged, in order to reduce the ripple at small R.) The bundle divertor null is nominally located at 4.5 m, and is used only during start-up, when the plasma is located at large major radius.

All the poloidal-field coils are located outside the TF coils, with the exception of two pairs of passive vertical stabilizing coils. These latter coils are not actively cooled, carry little current, and are expected not to require maintenance. However, they must be cut in order to remove torus sectors. The ripple coils are easily removed between adjacent TF coils.

Fig. 2-4. PITR-3
GENERAL ARRANGEMENT

2.4 Summary of Pitr Parameters

The principal plasma parameters of Pitr-3 are given in Table 2-2. One measure of the attainable $\bar{n}_E$ of the plasma, according to a commonly used "empirical scaling law", is the magnitude of the quantity $a_p^2 B_t^4$. In the "reference-sized" Pitr plasma, $\bar{n}_E$ is 4.5 times $[n_E]_{\text{ign}}$ at $T_e(0) = 12 \text{ keV}$, $T_e = 8 \text{ keV}$. A circular plasma located at the same position would have an "ignition margin" MI of about 1.5, when calculated in the same way, while a circular plasma with the largest possible half-width ($a_p = 1.53$) would have a somewhat larger MI. The elongated compressed plasma has $MI \approx 2.5$, but a circular plasma at this location would have $MI < 1$. As discussed in Sect. 2.2, these ignition margins apply to plasmas with "empirical" transport scaling and with $Z_{\text{eff}} \sim 1$. The given MI indicate the allowable factors by which transport losses may exceed those expected from empirical scaling due to unforeseen effects at very high temperature, pressure, or elongation; or the allowable increase in radiative loss and impurity dilution if it proves difficult to maintain $Z_{\text{eff}} \lesssim 2$. The bundle divertor on Pitr is intended for active impurity control during heating to ignition, after which the plasma will be protected by a dense cold blanket.

Table 2-3 summarizes the principal magnetics systems parameters of Pitr-3.

Table 2-2. PRINCIPAL PARAMETERS OF PITR-3.

Maximum field at TF coil ($R = 0.98$ m)	12.5 T
TF coil bore	4.37×5.80 m
Stored magnetic energy	2470 MJ
Vacuum vessel bore	3.30 m $\times$ 4.56 m
Maximum flux swing for driving plasma current	45 Wb
Maximum plasma half-width	1.53 m
Flat-top at max. field	9 s
Total pulse length at max. field	14 s

<u>(Conditions Refer to Maximum Field)</u>	<u>COMPRESSED PLASMA</u>	<u>REFERENCE PLASMA</u>
Major radius (m)	2.30	2.80
Plasma half width (m)	0.85	1.35
Plasma vertical elongation	1.6	1.5
Plasma current (MA)	4.4	6.0
Ave. plasma density (cm^{-3})	2.5×10^{14}	2.0×10^{14}
Ave. plasma temperature (keV)	8-10	8-10
Ave. plasma beta	0.07	0.09
"Margin for ignition"	2.5	4.5
Fusion power density (MW/m^3)		4 to 4.5 MW/m^3
Fusion power production (MW)		450 to 550 MW
Neutron power loading (MW/m^2)		1.8 to 2.2 MW/m^2

Table 2-3.
 PRINCIPAL MAGNETICS SYSTEMS PARAMETERS OF PITR-3

TOROIDAL-FIELD COILS

Number of coils	16
Aperture	4.37 m $\times$ 5.80 m
Coil major radius	3.4 m
Conductor	copper, water cooled
Conductor current	178 kA
Ampere-turns/coil	3.9 MAT
Max. B at conductor	12.5 T
Max. stress in copper	22,000 psi
Stored energy	2470 MJ
Weight of copper per coil	20.8 tons

Poloidal-Field Coils (max. ratings)

Conductor	copper, water-cooled
Peak power	1200 MW
Peak voltage	22 kV
Peak current	97 kA
Resistive energy loss	2300 MJ
Stored energy	2100 MJ
Field in throat	12 T
Vertical field at plasma	0.75 T
Flux swing	45 Wb

Bundle Divertor (max. ratings)

Conductor	copper, water cooled
Current/coil	9.4 MAT
Current density	4.5 kA/cm ²
Resistive power loss	54 MW
Stress	15,000 psi
Field nulled	2.7 T
Flat-top	5 s

2.5 Summary of PITER Design Features

The design studies for PITER have used a geometry that should provide the highest MHD-stable value of beta in a D-shaped plasma, consistent with our present limited experimental knowledge of the effectiveness of vertically elongated plasmas. By adopting some of the techniques proposed in the SMARTOR concept [2.3], the PITER aims to operate at small aspect ratio and small R_0 , while maintaining excellent access and minimizing remote handling problems.

Power reactor features that are absent from PITER include (i) superconducting coils, (ii) a sustained burn of duration ≥ 100 s; (iii) radiation damage to the first wall; (iv) an energy conversion blanket (although various tests can be made with small blanket modules); (v) tritium breeding. Nevertheless, the special techniques of plasma current induction and heating to ignition that have been developed for PITER, as well as a number of novel engineering subsystems that have been conceived and analyzed, should be extrapolatable for use on tokamak power reactors. The following is a summary of the plasma, magnetics, and mechanical design features of PITER, with emphasis on the more novel items.

Plasma Engineering

(1) Plasma current induction by the equilibrium-field coils is maximized with minimal dependence on the central transformer core. This practice permits the use of small-aspect-ratio TF coils, which allow small-aspect-ratio plasmas with $\langle \beta \rangle$ in the range 6 to 10%. Although not included in the reference design, analyses have been made of the use of neutral-beam-driven currents during start-up, which would enable elimination of the center solenoid altogether.

(2) Major-radius compression is facilitated by the small-aspect ratio TF coils. Heating to ignition is accomplished by 60 MW of 160-keV neutral-beam injection of a precompression plasma, followed by a major-radius compression of 1.45, which increases both the temperature and the na^2 -value of the plasma. Attaining ignition conditions in a plasma containing impurities can be assisted by a propagating burn following compression, provided that the temperature immediately after compression is much larger than the operating ignition temperature.

(3) Vertical injection. Sufficient access is available for vertical beam injection and the emplacement of ripple-inducing coils. This feature could be utilized for the direct heating to ignition of full-aperture PITER plasmas with 90-keV D^0 beams, by the ripple-assisted injection technique.

(4) Impurity Control. During start-up, a bundle divertor produces a null in the toroidal field at large R. The ignited plasma is protected by a cold plasma blanket. A bundle divertor of practical size for PITER (and presumably for most large tokamaks) is found to cause serious perturbations of the magnetic surfaces. (The ignition margin required to compensate for any consequent degradation in energy confinement needs further investigation.)

(5) Confinement. When $Z_{eff} = 1$, the $\bar{n}\tau_E$ -value calculated from the presently observed empirical scaling law can be up to 4.5 times that required for ignition, using a plasma of $a_p = 1.35$ to 1.50 m, and elongation 1.3 to 1.5.

(6) Plasma Breakdown. The small magnetic field at the outer edge of the PITER plasma ($B_t \leq 2.7$ T) allows breakdown and initial heating of the filling gas by electron cyclotron radiation at 75 GHz.

(7) Thermal Stability. The ripple coils are activated to control the fraction of fusion alpha particles confined in the plasma, and thus to brake temperature excursions. In the case of ignition of a compressed plasma, temperature excursions are accommodated by a "burn wave" expansion. Alternatively, the temperature can be regulated by alternating short strokes of decompression and compression.

(8) Plasma parameters similar to those expected in a power reactor. Plasma radius = 1.0 to 1.5 m, $n(0) = 3$ to $4 \times 10^{14} \text{ cm}^{-3}$, $T(0) = 11$ to 15 keV, $I_p = 4.5$ to 6 MA, $\bar{\beta} = 6$ to 10%.

(9) Pulse length. Joule heating in the trunk region limits the TF-coils "flat-top" time to 9 s at full field, but the flat-top increases to 18 s at 3/4 field. The repetition time is 600 s, so that the duty factor is about 2%.

Mechanical and Magnetics Engineering

(1) TF Coils of nearly constant-tension shape enable low-stress operation at the highest field. The radiation shielding inboard of the plasma helps take up the tensile and bending stresses in the trunk region. The maximum stress is sufficiently small ($\leq 22,000$ psi) so that repetitive operation at high field is not limited by fatigue.

(2) The Vacuum Vessel is constructed of multi-layer, thin-wall, titanium alloy. No bellows or insulating break is required. A low-Z coating is applied to minimize tritium hold-up.

(3) Bundle Divertor. One TF-coil leg is displaced outward, with the two bundle divertor coils then occupying the space between the TF coils on either side of the displaced coil.

(4) Shielding. Normal "radiation resistant" epoxy is adequately protected for a total 14-MeV neutron wall-load of 10^6 MJ/m² by a shield consisting of stainless steel and water. This fluence represents 50,000 full-power D-T pulses. Furthermore, the shielding plays a vital role in counteracting loads on the TF coils: As mentioned above, the shield inboard of the plasma helps take up the tensile stress and bending stress in the trunk region. The outboard shield is used to counteract the turning moments on the TF coils, to the extent that it appears feasible to eliminate the usual torque frame or shear panels.

(5) Feasibility of remote handling is stressed. In keeping with the goal of determining optimized prototypical reactor-plasma operation, it is expected that up to 50,000 D-T shots will be required (not all which will result in full-fledged ignition). Thus PITR is designed to be fully accessible for remote handling. The region inboard of the plasma contains only a small easily removable central solenoid in the throat of the TF coils. The only PF coils inside the TF coils are four passive vertical stabilizing coils that carry little current and require no cooling. Nevertheless, the greatest technological advances required for the PITR are probably in the area of remote handling techniques, for which there has thus far been no experience on controlled fusion devices.

(6) Tritium. A maximum tritium throughput of 20 kCi is required per 10 to 20-s pulse, and the total on-site tritium inventory is specified as 2.3 MCi. An on-site continuous processing facility and several protective clean-up systems are provided.

2.6 References

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3.0 PLASMA PHYSICS

Start-up and ignition of the PITER plasma will involve demonstration and optimization of the following reactor plasma processes: (1) breakdown of the plasma and induction of a 4.5-6 MA plasma current, (2) heating to ignition using neutral-beam injection, compression, and fusion alpha heating, (3) attainment of high- β ignited plasmas, (4) control of impurities and heat flow using divertor and cold plasma blanket techniques and (5) stable thermal and particle balances in an ignited thermonuclear burn of at least 5 s duration. Table 3-1 shows the target values for the principal plasma parameters when ignition is obtained. The PITER design includes sufficient flexibility and 'margins of safety' to allow investigation of a range of these parameters and a variety of methods for obtaining them. Section 3 first describes a typical sequence of start-up events leading to ignition, and then discusses in more detail the required current, heating, pressure balance, impurity control, and burn duration.

Table 3-1 Desirable Plasma Characteristics at Ignition

Plasma current	4.5 - 6 MA
$n_e(0)$	$3 - 4 \times 10^{14} \text{ cm}^{-3}$
$T_e(0) \approx T_i(0)$	11 - 15 keV
$\langle \beta \rangle$	6% - 10%
τ_E	1 s
τ_{particle}	$\geq 3 \text{ s}$
Burn duration	$\geq 5 \text{ s}$

3.1 Sequence of Start-Up Events

In order to produce a small-aspect-ratio ignited plasma at small major radius and moderate magnetic field, it is necessary to rely on the EF (equilibrium-field) coils for inducing most of the plasma current, I_p , at the same time that they provide the required vertical field. The plasma pressure must rise in a fairly well-prescribed fashion, with neutral-beam heating initiated at a very early stage (e.g., $I_p \sim 250$ kA), both to increase β_p and to minimize the resistive volt-second requirement. The vertical field is increased to accommodate the increasing plasma pressure, and the incremental flux swing further increases I_p . Finally, the EF coils together with some moderate-current "shaping coils" must provide the proper shaping field if a D-shaped plasma is desired. Each coil requires individual programming. The following is a summary of a start-up sequence that illustrates the method of obtaining the desired plasma current with the aid of only a very small central solenoid. Table 3-2 gives important plasma parameters at the end of each phase of start-up. Just before start-up, the "leaky OH" coil is charged, and the outside EF coil is activated to produce a shallow hexapole null at $R = 3.8$ m, thereby excluding stray vertical field from the start-up region. The bundle divertor (BD) coil current is increased to produce a null at $R = 4.5$ m.

(1) At $t = 0$, the filling gas is broken down and ionized at $R = 3.8$ m by a pulse of radiation at the electron cyclotron frequency (~ 75 GHz).

(2) At $t < 0.1$ s, a current of 300 kA is established in a slightly triangular plasma centered at $R_0 = 3.8$ m, by a rapid reduction in the current carried by the leaky OH coils. This start-up procedure is similar to that used in the ATC device, which had no poloidal-field coils inboard of the plasma.

(3) During $0.1 < 1.0$ s, the leaky-OH coils are completely discharged, while the plasma is heated either by ~ 150 -keV D^0 beams injected nearly perpendicularly in the horizontal plane, or by ripple-assisted injection of ~ 80 -keV beams injected vertically. Approximately 55 MW of beam power are required. The discharge of the leaky-OH, together with the increase in vertical field, produce a flux swing of ~ 23 V-sec, or sufficient to induce $I_p = 3.0$ MA in a slightly elongated plasma. The "resistive volt-sec" requirement is minimized by initiating intense beam heating when I_p is only ~ 250 kA. (At this early stage, the beam voltage must be reduced to insure beam trapping.) Up to 3 V-s additional flux can be obtained from the central solenoid to counteract enhanced resistive loss, if necessary.

(4) In the compression option, R_0 is reduced from 3.3 m to 2.3 m in a period of 200 ms, which requires an EF-coil power of approximately 500 MW during this period. To conserve magnetic flux, I_p increases to 4.4 MA, while a_p decreases significantly. However, the plasma height can be maintained, so that K is now 1.6. A passive stabilizing coil located inside the TF coils damps vertical motion of the elongated plasma. The hexapole coils in the throat help maintain the required D-shape. Immediately upon compression, gas is admitted to the torus to form a dense cold plasma blanket surrounding the hot core.

(5) Propagating Burn. The compressed plasma has an ignition margin of 1.9 referred to the empirical scaling law. If it ignites, thermal runaway will increase the plasma pressure, with MHD activity resulting in a gradual increase in plasma size. The continuing increase in plasma height and plasma energy compel an increase in the vertical field, even though the geometric center of the plasma moves outward. The plasma current may increase up to about 6 MA, with a final $\langle \beta \rangle \sim 0.1$.

Table 3-2. Illustrative Start-Up Scenario with Compression

Time (sec)	Heating Method	R_0^* (m)	a_p (m)	K	Current Induction	I_p (MA)	B_v (T)	T(0) keV
0.001	ECR 75 GHz	3.8	~0.5	1.0	—	0	0	0.01
0.1	Ohmic	3.8	0.5	1.0	Central Sol + Leaky OH	0.3	0.02	1
1.0	150-keV beams	3.3	1.0	1.5	Leaky OH	3.0	0.4	8
1.2	Compression + alphas	2.3	0.85	1.6	EF	4.4	0.6	12
2.5	Alphas	2.8	1.35	1.5	EF + high- β	5.8	0.7	15

* R_0 is the geometric axis. The magnetic axis can be displaced significantly outward.

3.2 Plasma Current

To maintain plasma equilibrium in PITR without a region of reversed toroidal current on the inside of the torus requires a plasma current 4.5 - 6 MA, depending on the pressure and current profiles (c. f. Section 3.4). At even lower current, fusion alpha particles are still well confined, and the fusion energy is deposited locally. For example, Fig. 3-1 shows the relative particle birth and fusion energy deposition profiles for a PITR-like plasma at $I_p = 3.5$ MA.

The final plasma current at ignition is determined by the choice of start-up sequences. PITR will allow a test of several procedures which maximize the final plasma current with minimum demand on the central OH transformer. These techniques are discussed in Sections 3.2.1 - 3.2.3.

3.2.1 Beam Heating. One method for minimizing the demand on the OH transformer is to initiate neutral beam injection when the plasma current is ~ 250 kA, the value needed to confine coinjected fast ions. Rapid heating then minimizes any further volt-second requirement by reducing the plasma resistivity. As an example of this technique, the evolution of the electron temperature and neoclassical resistivity during start-up has been calculated with a zero-dimensional model for the case of 20 MW of 80 keV D^0 , injected beginning at $t = 40$ ms after formation of a plasma with volume average temperatures $\langle T_e \rangle = \langle T_i \rangle = 10$ eV. The temperature evolution (Figure 3-2a) is determined by ohmic and beam heating, oxygen impurity radiation, and by transport processes described by the empirical electron and ion energy confinement times τ_{Ee} and τ_{Ei} [3.1]. The evolution of the plasma density (Fig. 3-2a) is controlled by beam fueling and the empirical particle confinement time $\tau_p = 3\tau_{Ee}$. The position of the plasma is chosen to vary in such a way as to keep the safety factor q at the limiter equal to 3, using only the change in the EF field to induce the plasma current. This dictates the evolution of plasma minor radius shown in Fig. 3-2b. The resistive volt-seconds consumed in heating the plasma to $\langle T_e \rangle = 3$ keV is only 1 volt-s.

3.2.2 Beam-Driven Current. The plasma current driven by the poloidal field coils can be supplemented with beam-driven current provided by coinjection of moderate power and energy neutral beams. As an example, Fig. 3-2c shows the beam-driven current calculated for the start-up scenario described in Section 3.2.1. Since the beam-driven current profile for a low density plasma is very similar to the transformer-driven current profile, the 4 MA of beam-driven current available can be used to substitute for or supplement the transformer-driven

Fig. 3-1. Birth profile of fusion alpha particles and energy deposition per zone (between r and $r + dr$) as a function of minor radius for $I_p = 3.5$ MA, $B_t = 3.2$ T, $R = 3.2$ M, $a_p = 1.5$ m.

Fig. 3-2. Start-up. $R + a_0 = 4.3$ m, 50%:50% D:T plasma, 1% oxygen in filling gas. (a) Volume average temperature (solid line) and density (dashed line), (b) plasma minor radius, (c) beam-driven current. $E_b = 80$ keV, $P_b = 20$ MW.

current [3.1]. After the beam-driven current is established, the total current in the full-sized, hot plasma is then frozen in during the following fueling, heating, compression, and ignition phases of the start-up procedure.

If the expected beam-driven currents are detected in forthcoming experiments on PLT and ISX-B [3.1], then PITER could be equipped with tangential beams specifically designed to alleviate the demand on the OH coils. It would also be possible to use the specified PITER beam injection system to test a variety of combinations of tangential to perpendicular injection and determine the beam-driven current. Successful use of beam-driven currents in PITER would demonstrate the feasibility of a proposal to eliminate the central OH transformer entirely from future tokamak reactors, with consequent significant savings in size, complexity, and cost [3.2].

3.2.3. EF-Driven Current and Compression. The build-up of the EF field needed for an ignited, high β plasma induces a large plasma current [3.2]. The PITER design utilizes a small aspect ratio which maximizes this effect. The EF field increases due to the increase in plasma pressure during neutral beam heating. The plasma current is therefore gradually increased in a growing plasma during neutral beam heating. The programming of the currents in the poloidal field coils necessary to maximize the usefulness of the EF coils for inducing plasma current is discussed in detail in Section 5 below.

During compression and the thermonuclear burn, the plasma current is determined by flux conservation, since the plasma skin time is ~ 100 s and the compression and thermonuclear burn duration are 0.2 s and 9 s - 18 s, respectively. During compression, the plasma current $I_p \propto C$, where C is the compression ratio [3.3].

3.3 Heating to Ignition

PITER will test a variety of plasma heating methods to determine the optimum combination for ignition of a thermonuclear reactor at minimum cost. These include neutral beam injection, major radius compression, maximizing the use of fusion heating, and possibly rf and/or ripple injection heating.

3.3.1 Neutral Beams. Rather good neutral beam penetration is required to maximize the efficiency of beam heating of the central plasma, to minimize sputtering of the first wall by hot neutrals escaping from an overheated plasma edge, and to minimize the loss of fast ions by charge-exchange, by limiter-intersecting orbits, and by superbanana orbits in ripple fields - all of which

are enhanced at the plasma edge. The empirical energy scaling relates $\bar{n}\tau_E \propto n_p^2 a_p^2$ [3.3] directly to a unique beam energy through the plasma opacity $\bar{n}a_p$. Figure 2-2 shows the D⁰ beam energy required to achieve a somewhat peaked trapping profile for near-perpendicular injection into ignition-sized plasma. It is assumed here that $\tau_E = 4 \cdot 10^{-19} \cdot MI \cdot n(0)a_p^2$ where MI is the 'ignition margin', and $n(0)$ is the electron density at the magnetic axis [3.3]. Even for MI = 1, 250 keV beams would be required for the temperature range of interest. The 55 MW, 160 keV injection planned for PITR will be therefore supplemented by major radius compression and/or alpha heating to obtain ignition.

3.3.2 Compression. Adiabatic compression in major radius increases the plasma temperature, density and opacity, by the factors:

$$\begin{aligned} n_f &= C^2 n_i \\ T_f &= C^{4/3} T_i \\ n_f a_f &= C^{3/2} n_i a_i \end{aligned}$$

where i and f indicate values before and after compression and $C = R_f/R_i$ is the compression ratio. Fig. 2-3 shows the neutral beam energy required for penetration of a plasma to be compressed to ignition, as a function of compression ratio C [3.3]. The PITR design allows compression ratios up to $C = 1.5$.

The combined use of neutral-beam heating and compression to obtain ignition has been investigated in detail using the one-dimensional (1-D) transport code, BALDUR [3.4]. Figures 3-3 to 3-5 show the evolution of density and temperature profiles starting from an ohmically heated plasma ($T_e \leq 1$ keV) which is injected perpendicularly with 60 MW of 150 keV D⁰ beams between $t = 1$ s and $t = 3$ s, and then compressed from $R = 3.2$ m to ignition at $R = 2.13$ m between $t = 3.0$ s and $t = 3.2$ s. A small, circular, precompression plasma ($a_p = 1.0$ m) is used in this calculation, leaving room for improvement due to elongation and to use of a larger plasma or thin cold plasma blanket. The neutral beam parameters are slightly more conservative than the 60 MW, 160 keV system examined in Appendix B. In the example shown in Figs. 3-3 to 3-5 we assume empirical electron transport losses corresponding to $\tau_{Ee} \sim \tau_{E, \text{empirical}}$ [3.3] with a margin of ignition MI = 1, neoclassical ion transport, enhanced transport at high pressure which limits $\bar{\beta}$ to $\bar{\beta} \approx 8\%$, and particle transport corresponding to $\tau_{\text{particle}} \sim 3\tau_E$. The compression time, 0.2 s, is short compared to the energy confinement time $\tau_E \sim 1$ s, during compression, so the compression is nearly adiabatic. Ignition is also obtained in a similar calculation using 50 MW D⁰ injection into an initially 40% D: 60% T plasma.

Fig. 3-3. Evolution $T_e(0)$ and $n_e(0)$ with neutral beam heating and compression to ignition. 50%:50% D:T plasma with oxygen impurity giving $Z_{eff} = 1.5$. During beam heating, $E_b = 150$ keV, $P_b = 60$ MW, $a_p = 1.0$ m.

Fig. 3-4. Evolution of $n_e(r)$ with neutral beam heating and compression to ignition, using heating sequence of Fig. 3-3.

Fig. 3-5. Evolution of $T_e(r)$ with neutral beam heating and compression to ignition, using heating sequence of Fig. 3-3.

3.3.3 Fusion Heating. Even if the heating and compression are not as efficient as indicated in Fig. 3-3, the PITR design allows an additional margin of safety through the use of the 'burn wave' technique, where a fusioning core is fueled to ignition with a dense plasma blanket. This is illustrated with results from the BALDUR code in Figs. 3-6 to 3-8. Neutral gas added during compression fuels an overheated compressed core, which ignites, due to the increasing core density, 0.5 s after the end of compression. The fusion power halts the decrease in T_e at $t \sim 0.5$ s, which eventually halts the decrease in T_i at $t \sim 1.4$ s. The results shown in Figs. 3-6 to 3-8 use the same empirical energy diffusion model as in Section 3.3.2, with a constant diffusion coefficient $D = 1.0 \times 10^3 \text{ cm}^2/\text{s}$, which corresponds to $\tau_{\text{particle}} \sim 2$ s for the ~ 87 cm hot plasma core. The exact evolution of the thermonuclear burn will of course depend on the transport properties of inverted density profiles, the measurement of which is best done in large, high power tokamaks such as TFTR, JET, and PITR where strong gas puffing is not expected to quench the discharge.

The ignition margin provided by the burn wave technique is more conveniently described by a one fluid transport model with two zones (core and blanket) [3.5]. The separation between core and blanket is accurately defined by the inflection point in the plasma pressure profile, which marks the boundary inside which significant fusion heating occurs. An example of the pressure and fusion heating profiles taken from the above results of the 1-D code at the time of ignition is shown in Figs. 3-9a and 3-9b. The two zone model uses the same transport scaling as the BALDUR plasma blanket calculations, and gives almost exactly the same result for the evolution of the average core density and nearly the same result for the evolution of the volume averaged mean core temperature $T_{\text{core}} = \langle T_e + T_i \rangle / 2$. (The two zone model is slightly more conservative, requiring a starting $\langle \beta \rangle$ for eventual ignition $\sim 1/3$ higher than that of the 1-D code). Figure 3-10 shows the evolution of T_{core} with increasing core electron density n_{core} from a variety of starting plasmas. n_{core} increases nearly linearly with time, taking 1 s to rise from $1.5 \times 10^{14} \text{ cm}^{-3}$ to $3 \times 10^{14} \text{ cm}^{-3}$. It is evident from Fig. 3-10 that eventual ignition of a 'smoldering', fusion-heated, plasma can occur in a plasma which starts with n_{core} 60% to 80% of that needed for immediate ignition. Ignition is therefore obtainable starting from an overheated plasma with opacity $\sim 70\%$ of that needed for direct ignition, using beams with 70% of the beam energy needed for direct ignition. These results suggest that

Fig. 3-6. Evolution of $n_e(r)$ to form a dense plasma blanket. 50%:50% D:T plasma. Gas puffing forms a blanket during compression up to $t = 0.08$ s.

Fig. 3-7. Evolution of $T_e(r)$ with dense plasma blanket.

Fig. 3-8. Evolution of $T_i(r)$ with dense plasma blanket.

Fig. 3-9. (a) Alpha heating and (b) beta (plasma pressure) profiles at ignition with dense plasma blanket.

Fig. 3-10. Evolution of core temperature $T_{\text{core}} = (1/2)\langle T_e + T_i \rangle_{\text{core}}$ with increasing core electron density from various starting plasmas. $n_{\text{blanket}} = 4.5 \times 10^{14} \text{ cm}^{-3}$, $T_{\text{blanket}} = 0.1 \text{ keV}$, 0.05% Fe in core and blanket.

ignition can always be obtained by provision of sufficient beam power, without the large cost penalty involved in using high energy ($W_b \gg 160$ keV) beams. A similar but smaller improvement is obtainable using the plasma blanket if τ_E increases more slowly with n than $\tau_E \propto n$.

3.3.4 Other Heating Methods. The PITER design has sufficient size and access to accommodate rf heating and/or ripple-assisted injection. Rf heating at high power will be tested on PLT and other devices during the next two years. Ripple-assisted injection using vertical injection would allow adequate penetration of a full-size igniting plasma with 80 keV to 90 keV beams and can be used either as a supplement to perpendicular injection and/or compression, or as a replacement for the 160 keV perpendicular injection system. The ripple-assisted injection technique is being tested on the ISX-B tokamak [3.6].

3.4 MHD Equilibrium and Stability Studies

MHD equilibrium configurations for PITER have been calculated using the PPPL Equilibrium Code. Figure 3-11 shows an example of the resultant design-point flux surfaces for the reference plasma of Table 2-2. A particular equilibrium problem with high β , low aspect ratio devices, such as PITER, is the tendency for reversal of the toroidal current on the inside of the torus [3.7]; this defines the large plasma current required in PITER. We have demonstrated the existence of suitable high β plasma pressure and safety profiles, which do not have reversed toroidal current.

Many forms of the equilibrium current and pressure profiles have been investigated, in order to determine the optimum stability against ideal MHD interchange and kink modes, as calculated by PEST [3.8] and BALLOON [3.9]. For instance, using the profiles of Sykes, Wesson and Cox [3.10], we have found equilibria at $\beta^* \sim 12\%$ (where $\beta^* = 2\mu_0 \langle p^2 \rangle / B_t^2$) that are stable to ballooning modes. However, these equilibria are extremely unstable to free surface kink modes. Using more conventional profiles, we find the onset of the $N = 1$ kink modes occurring for $\beta^* \sim 6\%$ and the ballooning modes for $\beta^* \sim 7.5\%$. Considering the uncertainty of the resistive and nonlinear behavior of these modes, and the stabilizing effect of conduction coils and structure on the kink modes, configurations with $\beta^* \geq 7\%$ do not seem unreasonable. More detailed studies of the optimal stable profiles possible in a high β plasma are currently in progress [3.11].

The limitations imposed by MHD equilibrium and stability considerations should not interfere with the compression and burn wave start-up techniques. The

CONTOURS OF POLOIDAL MAGNETIC FLUX

Fig. 3-11. MHD equilibrium for ignited PITR plasma.

maximum allowed β for 'balloon' mode stability is thought to scale as $\beta \propto K(q^2A)^{-1}$ where K is the elongation and A is the aspect ratio. The compressed plasma sits in a region of high B_t , low A , and the geometry of the D shaped TF coils allows significant elongation, which all favor stability against ballooning modes. Also the burn wave technique allows an approach to ignition at nearly constant β , as illustrated in Fig. 3-10.

After ignition, a range of ignited plasmas may exist, the allowable range of n and T being bounded approximately by some $\langle\beta\rangle = \text{constant}$, the contours of which are plotted in Fig. 3-10. Excursions beyond the stable operating range may be damped spontaneously by MHD activity, or controlled externally by techniques designed to maintain thermal stability, as discussed in Section 3.6 below. Possible consequences of spontaneous damping are illustrated in Fig. 3-7. The temperature decline which occurs in Fig. 3-7 at $t = 2.4$ s is modelled by gradually adding Bohm diffusion to the transport equations as the plasma pressure approaches a limiting $\langle\beta\rangle \sim 9\%$ [3.4].

3.5 Impurity Control and Heat Flow

In the above discussion it has been assumed that the plasmas are not dominated by radiative losses from high-Z impurities. The effect of such impurities is illustrated in Fig. 3-12, where a uniform concentration of 0.2% to 0.4% Fe is assumed. (The radiative loss from 0.2% Fe in coronal equilibrium is equivalent to that from 0.3% Ti at ignition temperatures [3.12].) The PITER design has been chosen to minimize the generation of impurities during start-up by utilizing the minimum possible beam injection power and by incorporation of the bundle divertor to be activated during start-up (c.f. Section 4.2). The inboard wall will be protected by a carbon 'bumper', or limiter, which has alleviated impurity problems in TFR and PLT. It is expected that the first wall will be protected by a low-Z coating.

During and after compression, the fusing core will be protected by a cold plasma blanket, which is thought to be capable of reducing impurity production [3.13] by reducing the energy of outgoing fast neutrals to less than 100 eV before they strike the first wall.

A pure cold plasma blanket can also dilute the impurities incorporated into the core during heating; the results are illustrated in Fig. 3-13. The existing

Fig. 3-12. Evolution of T_{core} vs n_{core} as in Fig. 3-12 with 0.2% to 0.4% Fe in core and blanket.

Fig. 3-13. Evolution of T_{core} vs n_{core} with pure, 0% Fe, blanket (dashed lines) and with 0.4% Fe in blanket (solid lines). Initially 0.4% Fe in core. $n_{\text{blanket}} = 4.5 \times 10^{14} \text{ cm}^{-3}$, $T_{\text{blanket}} = 0.1\%$. Nearly vertical dashed lines denote ignition; short dashes = 0.4% Fe, long dashes = 0% Fe.

impurities present after beam-heating may even tend to concentrate in the blanket, if the predictions of neoclassical ion transport theory [3.13] are realized.

The heat flux will be deflected by the divertor during start-up. During compression and ignition, using the cold plasma blanket, the heat flux should be evenly distributed over the wall by radiation from the blanket. After ignition, the heat flux to the wall can vary from the 30 MW computed for a barely ignited plasma to 100 MW for an MHD-limited burn. 100 MW to the wall (plus 400 MW of fusion neutrons) has been allowed for in the PITR design.

3.6 Steady Thermonuclear Burn

The maximum possible burn duration is limited by the TF and PF systems to a flat top of 9 s at full toroidal field and 18 s at 3/4 field. PITER will allow investigation of a number of other possible limitations on the duration of the thermonuclear burn, including (1) impurity accumulation (2) fueling (3) alpha-particle exhaust and (4) thermal stability.

3.6.1 Impurity Accumulation. The effects of the cold plasma blanket on impurity accumulation can be investigated by controlling the shape of the blanket profile with variations in the neutral gas puffing during and after compression. Controlled asymmetric additions of neutral gas have also recently shown some promise for control of impurities in the 'impurity flow reversal' experiment on ISX [3.14].

3.6.2 Fueling. Fueling of the ignited plasma will occur initially from the cold plasma blanket. The duration of the ignited burn which is achievable by this method will be investigated.

3.6.3 Alpha-Particle Exhaust. The burn-up of D and T proceeds at the rate of about 0.5% per second, so that after 18 s, the fusion reactivity is reduced by about 20% if no thermalized alphas are removed. The accumulation and transport of fusion-generated alphas should not be dominant in determining the duration of the burn.

3.6.4. Thermal Stability. After ignition, the PITER plasma should be unstable to thermal perturbations. The pressure of the ignited plasma is likely to be near the MHD limiting value, and any increase in plasma temperature will tend to lead rapidly to larger β values, as indicated by the upturn of the solid lines in the upper right-hand corner of Fig. 3-10. The enhanced transport expected from exceeding the maximum stable β should first enlarge the ignited region of the plasma and limit its temperature (c.f. Section 3.3 above), and then continue to limit the ignited plasma to a marginally stable ignited state.

If active control of thermal stability is necessary either to limit the power output, or to avoid some sort of disruption, a number of possible methods exist. One promising method is the use of special ripple coils to regulate a vertically asymmetric ripple that causes enhanced alpha-particle diffusion [3.15]. Other possible methods include decompression, shifting the plasma to region of higher TF ripple, programming the blanket density, controlled admission of impurities, and various combinations of these methods. The study of thermal stability and its control is expected to be an important component of the PITER program.

3.7 References

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4.0 TOROIDAL-FIELD MAGNETIC SYSTEM

A small aspect ratio tokamak poses particularly severe constraints on the design of the toroidal field magnet system. Whereas it is usual to provide a center column support structure for the TF coil centering forces, this is not consistent with minimizing the major radius of the machine. Wedging of the tapered coil nose sections provides the structural path instead. By constructing the steel and water shield for the inboard section of the coil integrally as part of the coil structure use has been made of this material in a dual capacity, greatly increasing the available cross section area of steel and reducing the stress levels to a reasonable value. Stress reduction has also been accomplished by *designing the coil based on a compound-constant-tension coil shape [4.1]*. Finite element code stress analyses that included the effect of both in-plane and out-of-plane support structure were then used to optimize the design of the coil.

The structural design of this coil system has been complicated by the inclusion of a bundle-divertor. This introduces additional out-of-plane force components that are asymmetrically distributed about the torus. The bundle divertor is suitable for use in a small aspect ratio tokamak, as the toroidal field falls off steeply enough so that the strength of the required nulling field is less than 3 Tesla; this is estimated to be the practical limit that can be achieved with this type of design. Investigation of the field line topology for this machine, however, has revealed that the bundle divertor produces a perturbation that causes the $q = 2$ "surface" to become ergodic as is illustrated in Fig. 4-1. This behavior is not observed for the geometric parameters of DITE, a machine of larger aspect ratio. It would appear therefore that a limited parameter space exists for the use of bundle divertors on reactors. The aspect ratio must be sufficiently large that the field perturbations do not destroy the magnetic surfaces yet small enough so that the field at the null point is less than 3 Tesla. The exact boundaries of this parameter space have not been determined.

Bundle-divertors require that sufficient space be provided such that the current density in the coils be low enough to limit joule heating (in the case of copper coils) or be sufficiently beneath the critical current density (for superconductors). In this design this space has been provided by using extended TF coils (see Fig. 4-5) produced by increasing the major radius of the outboard leg of the coils. Two such coils located symmetrically at opposite sides of the machine have been used.

Fig. 4-1 Stochastic Behavior at $q = 2$ surface location due to bundle divertor penetration.

The structural design of the TF coils and the supporting structure is presented in Section 4.1; Section 4.2 analyzes the bundle divertor and its effect on the toroidal field coils; Sections 4.3 and 4.4 present an analysis of the thermal and electrical behavior of these coil systems; and Section 4.5 considers fabrication problems associated with these coils.

4.1 TF Coil Design

The TF coil system consists of sixteen stainless steel encased, water-cooled copper wound coils. The configuration and cross sections are shown in Fig. 4-2 and 4-3. A unique feature of the coil case design is that the water and steel shielding, required to protect the coil insulation along the inboard leg, is built into the coil case. The shielding steel is used structurally to reduce the tension stress in the tapered nose section of the coil. The basic TF coil parameters are listed in Table 4-4. (p. 4-32) The shape of the coil is approximately oval and permits installation of OH coils closer to the midplane along the inboard leg of the TF coil.

The coil shape is based on an analytic solution of a compound-constant-tension coil [4.1]. As in all constant tension coil configurations, the shape is based on a solution which utilizes a flexible filamentary conductor and represents a fully expanded filament in its loaded configuration. The in plane and out of plane support systems for these coils restrain the free expansion of the coil and thereby induce bending moments in the coils. The advantage of starting with this coil shape is to restrict the bending moments to those imposed by the support system. By adjusting the position and stiffness of the coil supports a minimum bending and approximate constant tension coil can be obtained.

As shown in Fig. 4-4, two opposing TF coils have increased bores, with the outboard leg displaced outward. One of the coils was extended to provide adequate room for the bundle divertor. The opposing TF coil was extended in order to balance the unsymmetrical forces caused by the extension of only one TF coil. A comparison of the "standard" and "extended" TF coil is shown in Fig. 4-5. Both coils are compound-constant-tension shape coils with a common inboard leg geometry.

4.1.1 TF Coil Force Analysis*

The primary forces acting on the TF coils are the radial forces (in-plane forces), and the lateral (out-of-plane forces) which generate overturning moments. The coils produce a symmetrical force system about the vertical center line of the machine. This symmetrical force system requires identical TF coils. A change in shape of any of the TF coils will produce an unsymmetrical centering force system and additional lateral forces, which vary depending on the distance from the coil that has been changed. The addition of a bundle divertor will also cause a change in centering and lateral forces.

*The coil force analysis data is presented in non-standard units. For convenience we note that 1 lb = .2248N and the 1 psi = 1.4×10^{-4} MPa.

FIG. 4-2

TF COIL
SECTIONS
REF FIG 4-1-1

SECTION A-A
(MID PLANE)

SECTION B-B

SECTION D-D

SECTION C-C

SECTION E-E
(INTER-COIL-STRUCTURE AREA)

FIG. 4-3

Fig 4-4 PITR-3
GENERAL ARRANGEMENT

COMPARISON OF STANDARD
AND EXTENDED
TF COILS

FIG. 4-5

Therefore the TF coil force analysis covers:

Centering Forces

- (a) Symmetric - using "standard" coils
- (b) Unsymmetric - due to "extended" coils
- (c) Unsymmetric - due to bundle divertor.

Lateral Forces

- (a) Turning moment
- (b) Due to extended coils
- (c) Due to bundle divertor.

Circular TF Coil Force Analysis

A preliminary analysis of the forces on the sixteen TF coils and bundle divertor was obtained by using a circular TF coil geometry with the same horizontal bore and locations as the oval shaped TF coils. The difference in fields at or near the midplane between oval and circular shaped coils was felt to be reasonably small (< 5%). A current filament was superposed on the coil centroid, and the Lorentz forces and moments in the three orthogonal directions were computed at each of 100 nodes. These moments and forces were then summed over the entire coil to arrive at the net forces and moments on each coil.

Figure 4-6 (upper half) shows the variation of the net centering force and lateral force on the sixteen TF coils with coil #1 extended, with and without the bundle divertor on. The lower half of Fig. 4-6 shows the variation of the same forces with coil #1 and #9 extended. As can be seen, the extension of Coil #1 only, with no divertor, produces an unsymmetrical centering force system on the machine. Coil #1 in this case has a centering force approximately 1×10^6 lbs. greater than the other coils. The balance of the machine is maintained by slightly increased centering forces on the opposing side of the machine and the horizontal component of the lateral forces of all the other TF coils. This is similar, but in a lesser degree, to a one coil out, fault condition. The bundle divertor configuration produces yet another unsymmetrical force system, but with most of the large changes in force taking place in those coils adjacent to the bundle divertor. Figure 4-7 shows the difference in the distribution of the radial forces on coil #1 and 9 with coil #1 extended.

This unsymmetrical force system must be balanced in the center hub structure and in the out-of-plane load path structure. Figure 4-8 shows graphically and numerically the variation of the radial forces at the inner and outer

FIG. 4-6

VARIATION IN RADIAL FORCESDUE TO TF COIL EXTENSION

UNIT = 6 IN. LONG SEGMENT

FIG. 4-7

4-12
VARIATION IN RADIAL FORCES AT INNER & OUTER LEG OF TF COILS

FIG. 4-8

legs of the TF coils at the midplane, with and without the bundle divertor. The forces shown are acting on a unit height of 15 cm (6 in.) at the midplane. Fig. 4-8 (upper half) shows the unsymmetrical force system acting on the center hub when only coil #1 is extended. A summation of forces in the X direction produces an unbalance force of 61,200 lbs. acting on this 6-inch-unit height. The variation of this unbalance force above and below the midplane is unknown. This unbalance force produces significant bending forces in the crucial hub or trunk area. For this reason, coil #9 was extended and provides a balancing force. Figure 4-8 (lower half) shows the variation of the forces at the midplane with the bundle divertor on. Again, the large change in forces occurs in coils 1, 2, and 16, which are adjacent to the bundle divertor.

Oval TF Coil Force Analysis

An analysis of the forces on the sixteen oval shaped TF coils without the bundle divertor was performed. Data obtained from the circular coil analysis indicate that the effects of the divertor are basically local. The configurations analyzed were (a) sixteen standard (small) coils, (b) two opposing extended (large) coils (#1 and #9), and (c) one extended coil (#1). Each configuration included OH and PF coils, the center solenoid, and the plasma.

The analysis was performed using a TFTR program modified for noncircular coils and plasmas. The primary coil was modeled as two concentric current filaments made up of connected circular arc segments having differing radii and centers of curvature. The other TF coils, the upper and lower PF coil pairs, and a bisected solenoid pair were modeled as single current filaments located at their respective geometric centroids. The dee-shaped plasma was modeled as 11 discrete circular current filaments approximating the plasma cross section assuming an originally uniform current distribution.

Figure 4-9 indicates the inplane and out-of-plane forces on the standard TF coil. Table 4-1 presents the inplane loads and incremental lateral loads on coil #1 due to poloidal fields created by the PF coils, the solenoid, and the plasma, for the three configurations analyzed. Table 4-2 lists the variation of loading for each of the sixteen coils on the two-coils-extended and one-coil-extended configurations.

P1TR-3 APPLIED FORCES
STANDARD T F COIL

RADIAL - IN-PLANE

LATERAL - OUT-OF-PLANE

4-14

FIG. 4-9

Table 4-1.

P1TR, Electromagnetic Load Increments

Total Load on Primary TF Coil (#1)

CONFIGURATION	F_x (LB)	F_y (LB)	F_z (LB)	M_{yy} (IN-LB)
16 Small Coils TF Only	0	-20,466,000	10,953,000	0
PF Increment on Small Coil	+3,797,000	—	—	722,054,000
Solenoid Increm. on Small Coil	+ 39,000	—	—	- 42,841,000
Plasma Increm. on Small Coil	+ 957,000	—	—	-162,088,000
2 Opposing Large 14 Small, TF Only	0	-20,876,000	2,144,000	0
1 Large Coil 15 Small, TF Only	0	-20,801,000	9,113,000	0
PF Increment on Large Coil	+4,203,000			927,679,000
Solenoid Incre. on Large Coil	+ 20,000			- 49,180,000
Plasma Increm. on Large Coil	+ 777,000			-137,723,000
16 Large Coils TF Only (Ref.)	0	-22,131,000	11,575,000	0

4-15

- MTE (1) F_x = Load on upper and lower halves, but of opposite signs.
 (2) F_z = Load on upper half coil, total at midplane, reversed on lower half.
 (3) M_{yy} = Total lateral moment couple on coil.

P.T.R. ELECTROMAGNETIC LOADS

TABLE 4-2

TOTAL LOAD ON PRIMARY COIL (#1)

2 LARGE COIL CONFIGURATION			
Coil No.	F_x (LB.)	F_y (LB.)	F_z (LB.) *
1 - LARGE	0	-20,876,000	9,144,000
2 - SMALL	-317,000	-20,480,000	11,709,000
3 - S	-226,000	-20,566,000	11,203,000
4 - S	-106,000	-20,610,000	11,108,000
5 - S	0	-20,623,000	11,138,000
6 - S	106,000	-20,610,000	11,108,000
7 - S	226,000	-20,566,000	11,203,000
8 - S	317,000	-20,480,000	11,709,000
9 - LARGE	0	-20,876,000	9,144,000
10 - S	-317,000	-20,480,000	11,709,000
11 - S	-226,000	-20,566,000	11,203,000
12 - S	-106,000	-20,610,000	11,108,000
13 - S	0	-20,623,000	11,138,000
14 - S	106,000	-20,610,000	11,108,000
15 - S	226,000	-20,566,000	11,203,000
16 - S	317,000	-20,480,000	11,709,000

1 LARGE COIL CONFIG.			
Coil No.	F_x (LB.)	F_y (LB.)	F_z (LB.) *
1 - LARGE	0	-20,801,000	9,113,000
2 - SMALL	-331,000	-20,409,000	11,677,000
3 - S	-238,000	-20,431,000	11,166,000
4 - S	-164,000	-20,531,000	11,060,000
5 - S	-100,000	-20,544,000	11,021,000
6 - S	-59,000	-20,544,000	11,001,000
7 - S	-32,000	-20,540,000	10,990,000
8 - S	-14,000	-20,536,000	10,985,000
9 - S	0	-20,535,000	10,983,000
10 - S	14,000	-20,536,000	10,985,000
11 - S	32,000	-20,540,000	10,990,000
12 - S	59,000	-20,544,000	11,001,000
13 - S	100,000	-20,544,000	11,021,000
14 - S	164,000	-20,531,000	11,060,000
15 - S	238,000	-20,409,000	11,166,000
16 - S	331,000	-20,409,000	11,677,000

* F_z = LOAD ON UPPER HALF COIL, TOTAL AT MIDPLANE, REVERSED ON LOWER HALF.

Stress Analysis and Structural Requirements

The stress analysis method used was the Grumman COMAP ASTRAL finite element computer code. The model consists of three dimensional beam elements using 60 nodal points around the full coil. The conductor and steel casing were modeled as separate beam elements, and superposed on the same nodal points. The assumption in this model is that perfect bonding exists between the steel casing and the copper winding as well as the adjacent windings. Only toroidal field forces were used in this two dimensional model of the standard coil at this time.

A primary part of this analysis was to compare the support system required by the analytic solution of this compound- constant-tension coil shape with the actual support system employed in the PITER design. The goal was to obtain a minimum bending coil configuration. To this end, three models were analyzed as shown in Fig. 4-10. They were:

- (a) the analytic solution - where the coils are supported normal to the coil contour along the inner leg of the coil only.
- (b) Horizontal supports along the inner leg using spring elements that represent the stiffness of the nesting support.
- (c) The same as B with spring elements along the outer leg representing the stiffness of the intercoil structure.

Axial force, moment distribution, nodal displacement and support reactions are shown in Fig. 4-11, -12, & -13 for configurations A, B, C. As can be seen in Fig. 4-11, configuration "A" maintains approximately constant tension throughout the coil but also feels a varying bending moment along its length. This configuration requires an external vertical restraint along its inboard leg. Configuration "B" produces an approximate 7×10^6 lbs tension load along its inboard leg at the midplane, and has a large bending moment at node #9 in its steel case. Configuration "C", with the intercoil structure reactions along its outer leg, has the same tension distribution at the midplane as Configuration "B". Configuration "C" produces the minimum bending moment along its length as shown in Fig. 4-13. Table 4-3 lists the maximum tension stress due to axial load and bending moments for each configuration at various beam elements.

PITR-3 FINITE ELEMENT MODELS

FIG. 4-10

PITR-3 FINITE ELEMENT MODEL
CONFIGURATION "A"

BENDING MOMENT (KIN-M)

FIG. 4-11

PITR-3 FINITE ELEMENT MODEL CONFIGURATION "B"

BENDING MOMENT (KN-M)

AXIAL FORCE (KN)

REACTIONS (KN)

DISPLACEMENT

FIG. 4-12

PITR-3 FINITE ELEMENT MODEL CONFIGURATION "C"

BENDING MOMENT (KN-M)

AXIAL FORCE (kN)

REACTIONS (kN)

DISPLACEMENT

FIG. 4-13

FINITE ELEMENT MODELS (CONT'D. 5 & 6)
MAXIMUM STRESS IN CONDUCTOR 3 CASE

f_B = STRESS DUE TO BENDING MOMENT

f_{AX} = STRESS DUE TO AXIAL FORCE

f_{TOT} = STRESS DUE TO COMBINE BENDING & AXIAL FORCES

CONFIG. "B"	STEEL			COPPER		
	f_B	$+ f_{AX}$	$= f_{TOT}$	f_B	$+ f_{AX}$	$= f_{TOT}$
MODE 1 KN/M^2 (PSI)	24,400	166,500	190,900 (27,600)	8700	97,400	106,300 (15,400)
NODE 9	31,000 (4500)	34,200 (5700)	70,200 (10,200)	9300 (1200)	23,000 (3300)	31,300 (4500)
NODE 18	44,000 (6400)	135,100 (19,600)	179,100 (26,000)	23,700 (3400)	71,400 (11,500)	103,100 (14,900)
CONFIG. "C"						
MODE 1 KN/M^2 (PSI)	24,000	170,200	194,800 (28,300)	8800	100,400	109,200 (15,700)
NODE 16	30,600 (4400)	67,800 (9800)	98,400 (14,200)	14,700 (2100)	34,500 (5800)	54,500 (7900)

TABLE 4-3

Structural Reaction Load Path

Figure 4-14 summarizes the radial and lateral forces acting on each coil and the method of reacting these forces. The primary reaction load path for the centering forces is formed by the tapered nose portion of each coil bearing against its adjacent coil. This wedging action produces a sixteen segment compression column. The maximum compression loads are felt in the midplane area of the coils and produce a bearing pressure of approximately 20,000 psi if a uniform distribution of load is assumed. The wedging-compression column is also required to react a portion of the lateral moment on each coil, which produces torsion on the segmented column. Shear keys are used to eliminate any slippage between adjacent coils and to balance the difference in centering loads, shown in Fig. 4-8, between adjacent coils in the trunk area.

The primary purpose of the intercoil structure is to provide a reaction load path for approximately 70% of the lateral moment on the coil system, as shown in Fig. 4-14. The intercoil structure also interacts with the radial forces on the coils as shown in the finite element analysis, Figs. 4-12 and -13. The net effect is to restrict the outward deflection of the outer leg of the coil. This produces tension loads in the intercoil structure.

P1TR-3 TF COIL (STANDARD)

APPLIED FORCES&
REACTIONS LATERAL RADIAL

4.2 Bundle Divertor

Two circular copper-wound water-cooled bundle divertor coils are used to null the field at the outer edge of the plasma. The divertor coil parameters are listed in Table 4-5. In order to provide adequate room for the two divertor coils, structural supports and neutron shielding, the bore of one of the TF coils has been increased by extending the outer leg outboard. This is shown in Fig. 4-15, and illustrates rectangular cross sectional coils with a cross sectional area of 2800 cm^2 . The case is designed to permit removal of the coil from the case. Figure 4-15 also illustrates the same cross sectional area coil but with an increased bore in order to provide additional neutron shielding and therefore longer coil life. An evaluation of various coil configurations is required to optimize the forces, null point, and current density.

4.2.1 Divertor Coil Force Analysis

The analysis of the forces on the bundle divertor were obtained using the circular TF coil geometry described in Section 4.1, "TF Coil Force Analysis". The moments and forces on the circular bundle divertor coils were summed over the entire coil to arrive at the net forces and moments on each coil. Figure 4-16 shows the net forces obtained as well as the size and location of the coils. 9.4 MAT was used to null the field (2.5 T) at 4.65 meters.

Structural Requirements and Analysis

The structural arrangement for the bundle divertor coils is shown in Fig. 4-15, and sections thru the coils and supports in Fig. 4-17.

The two coils are built into a common steel coil case which provide the load path for the opposing net horizontal forces ($1.872 \times 10^6 \text{ lbs.}$) and opposing moments ($184 \times 10^6 \text{ in. lbs.}$). These forces produce maximum bending and tension in the area of the coil case between the two coils. The case can be designed to resist these loads, including the hoop loads generated by each coil, but will actually share the loads with the adjacent TF coils and intercoil structure.

The centering force ($.493 \times 10^6 \text{ lbs.}$) on each divertor coil results from the interaction of the bundle divertor coil current and the toroidal field produced by the TF coils. Therefore a balancing load path between the divertor coils and all the TF coils is required. Data obtained from the TF Coil Analysis was used to estimate the distribution of these forces among the TF coils.

PITR-3 BUNDLE DIVERTOR CONFIGURATIONS

ALT. CONFIGURATION

FIG. 4-15

PITR-3
BUNDLE DIVERTOR
FORCES & GEOMETRY

(CPL. CROSS-SECT AREA)
 (1935 cm²)

FIG. 4-16

BUNDLE DIVERTOR STRUCTURAL SUPPORT (REF FIG. 4.2-1)

SECTION A-A
(COIL #1)

SECTION B-B
(COIL #2 & 16)

FIG. 4-17

Fig. 4-8 shows graphically and numerically the variation of the radial forces on the TF coils at the midplane of all sixteen coils with and without the bundle divertor. Figure 4-18 shows the difference in the radial forces on TF coils number 1, 2, and 16, with and without current flow in the adjacent bundle divertor coils. Figure 4-18 (Coil #1) indicates an increase in radial force along the outer leg of the TF coil in the area of the bundle divertor when the divertor is on. This is due to the interaction of the coil currents and magnetic fields in the adjacent coil legs. Therefore a tension force will be felt by the structural member shown in Fig. 4-17 between coil #1 and the center of the divertor case. In a like manner, Fig. 4-8 (coils 2 and 16) indicates a reduction in radial forces and therefore the structural tie in this area, shown in Fig. 4-17, will feel a compressive force.

The summation of these compression and tension forces at coils 1, 2, and 16 account for about 60% of the net centering force of the two divertor coils. The remaining 40% is distributed among all the other TF coils. The amount of force decreases as the distance increases between the TF coils and the divertors increases.

The shear-tension intercoil structure between TF coils, whose primary function is to react the TF coil lateral forces, provides a direct load path to all the other TF coils. This is in addition to the load path provided by TF coils 1, 2, and 16 to the trunk area of the machine. It is obvious that a three-dimensional finite element model would be required to obtain the distribution of the centering forces in this highly redundant structure.

BUNDLE DIVERTOR &
TF COIL INTERACTION
(RADIAL FORCES)

REF. SECT 4.1 CIRCULAR
 TF FORCE ANALYSIS

FIG. 4-18

4.3 TF Coil Electrical/Thermal Analysis

Basic TF coil parameters are listed in Table 4-4 for the PITER-3 design. Sixteen (16) coils, of 22 turns each, are used to generate a 4.47 Tesla toroidal field at the plasma centroid ($R = 2.80$ m) during a nominal flat top of 9 seconds. With the coil system forced at 10 kV (no load; actually approximately 8 kV average during current rise), the equivalent square wave time for a flat top coil current of 177.6 kA is 11.3 seconds. The resulting local peak temperature in the innermost (nose) portion of the turns, which have a maximum current density of 3.4 kA/cm^2 , is 110°C . This is a marginal, but probably acceptable, condition. The calculation is, of necessity, conservative in that it assumes all the heat generated in these turns to be completely trapped for at least the period of the equivalent square wave, and that the copper resistivity changes instantaneously with a change of temperature. (See the discussion of flat-top time below.)

As noted in Table 4-4, fourteen of the coils are "normal", with an internal bore of $4.37 \text{ m} \times 5.8 \text{ m}$. The other two, spaced 180° around the torus from each other, are "extended" to allow for inclusion of bundle divertor coils in outside structure. These extended coils have an internal bore of $5.13 \text{ m} \times 6.59 \text{ m}$.

Figure 4-19 shows curves of flat-top time vs. field at the plasma centroid at constant local peak hot spot temperatures (THS) of 90°C and 110°C — i.e., the boiling point of water $\pm 10^\circ\text{C}$. Notice that the nominal operating point described above lies on the upper curve. It may be concluded from these curves that moderate variations in TF field will result in significant changes of pulse duration.

The curves of Fig. 4-19 point up a set of problems. It is not easy to determine just what limit value to assign to peak hot spot temperature. TFTR uses 66°C for its PF coils, and originally wanted to restrict the TF coils to the same level. It was found necessary, however, to relax the TF requirement to 77°C . Some research into the problem elicited the following additional facts:

- (a) The range of temperature over which annealing, of the type of copper to be used in TFTR and PITER, will occur is from slightly less than 70°C (158°F) to just over 150°C (302°F). At the lower end, it will simply take longer to produce the same effect.

Table 4-4

TOROIDAL FIELD COIL PARAMETERS

<u>Parameter</u>	<u>Value</u>	<u>Unit</u>	<u>Remark</u>
Ampere-Turns	62.5	MA _t	
Number of Coils	16	-	
Turns/Coil	22	t	
Ampere-Turns/Coils	3.9	MA _t	
Coil Current	177.6	kA	
Total Turns	352	t	
Coil Major Radius, R ₀	280	cm	
Inside Radius, Copper	98	cm	
Outside Radius, Copper	580	cm	"Normal" Coil (14)
Outside Radius, Copper	675	cm	"Extended" Coil for Bundle Divertors (2)
Field at Coil Center	3.95	T	R = 3.165M
Field at Vac Vessel Center	4.17	T	R = 3.0M
Field at Plasma Centroid	4.47	T	R = 2.80M
Maximum Field in Coil	12.5	T	R = 1M
i ² R/Pulse	~452	MW	
1/2 LI ²	~2470	MJ	
Equiv. Square Wave Time	~11.3	s	
Total i ² t	~3.56 x 10 ¹¹	A ² s	
Total Diss. Energy/ Pulse	~5100	MJ	
Inductance, TF Coil Sys.	~0.1566	H	
Resistance, TF Coil Sys.	~0.0143	Ω	T = 328°K (55°C)
Coil Time Constant	~10.93	s	
Weight, Copper/Coil	~20,800	kg	"Normal" Coil
	~26,030	kg	"Extended" Coil
Max. Current Density	~3.4	kA/cm ²	

Table 4-4
(Continued)

<u>Parameter</u>	<u>Value</u>	<u>Unit</u>	<u>Remark</u>
Forcing Voltage	10kV		at No Load, 8kV Av during rise
Centering Force per Coil	91.1	MN	"Normal" Coil
Recov. Energy/ Pulse	~2120	MJ	
Insulation Test Volt	15	kV	
Max. Av ΔT /Coil	~37	$^{\circ}\text{C}$	
Coolant Flow/Coil	7.002×10^{-3}	m^3/s	
Inlet Coolant Temp.	10	$^{\circ}\text{C}$	
No. of Passages/Turn	1		{ Ellipse: 0.9525 Minor Dia x 1.905 Major, Area = 1.426 cm^2
No. of Passages/Coil	12		
Thermal Recovery Time	7.8	min	.13 hr
Total Coolant Flow	0.112	m^3/s	
Estimated Press. Drop/Coil	≤ 1.034	MPa	$v(\text{H}_2\text{O}) \leq 4.57 \text{ m/sec}$
Innermost Turn, Adiabatic Rise	~93	$^{\circ}\text{C}$	Borderline Condition - 78 $^{\circ}\text{C}$ max preferred

Fig. 4-19. Thermal limitation on pulse duration.

- (b) Actual effects of temperatures in the annealing range are dependent upon the mechanical stresses present at the time, and the coil is under stress while being heated.

It can be seen that, at this juncture, it is impossible to determine whether thermal fatigue, creep, stress fatigue or some other parameter will predominate to limit coil life in PiTR-3's present TF design. It is probable that a combination of all these will be effective if the machine is indeed operated in the annealing range. To see how this will affect operations, at normal B_T (4.47 T), the following brief table can be constructed:

<u>Hot Spot Temp (°C)</u>	<u>Flat-Top Time (S)</u>	<u>Remarks</u>
110	9	Normal
90	7	—
66	4.8	150°F

That is, dropping hot-spot temperature below the annealing range roughly halves the available flat-top time. To a first approximation, the 66°C limit can be maintained at 9 seconds by multiplying the area of the innermost (nose) turns of each TF coil by a factor of $\sqrt{9/4.8} \approx 1.37$ [since flat-top time is nearly proportional to the reciprocal of (current density)²]. This, of course, involves increasing the major radius of the inside of the torus to accommodate the necessary additional copper, i.e., larger machine, more material, higher costs, and loss of the advantages of a low aspect ratio machine.

Water flow design is based on the ability to return the coil to within 1.67°C (3°F) of the water inlet temperature of 10°C between each pulse. Table 4-4 shows that, with $7.002 \times 10^{-3} \text{ m}^3/\text{s}$ per coil, and the coil split into 12 parallel cooling passages (6 per "pancake", and $\sim 5.84 \times 10^{-4} \text{ m}^3/\text{s}$ average per passage), the TF system will recover thermally in about 7.8 minutes, allowing a pulse repetition rate of once every 8.1 minutes maximum. Thus one pulse per 10 minutes is amply safe. This operating point is shown on the recovery curve of Fig. 4-20, which further demonstrates that operation is below the estimated maximum pressure drop limit of 1.034 MPa, and the maximum recovery time limit for a 10 minute cycle time (0.16 hour — 9.6 minutes). Other calculations indicate that water velocity is between 3.05 and 4.27 m/sec. The complete TF water cooling system, then, has a total flow of $0.112 \text{ m}^3/\text{s}$, and should be capable of operating from a pump source pressure of 1.724 — 1.862 MPa.

Fig. 4-20. Thermal response time of coil between pulses.

A two-passage-per-turn system with the same flow area as above was studied briefly. It showed heat transfer benefits, but first estimates of pressure drop made it look most unattractive at the moment. However, detailed design analysis (i.e., modal analysis) may ultimately change this. It should also be noted that the quoted flow requirements could vary as much as $\pm 10\%$ once detailed hydraulic analyses of a complete system including manifolds and orifices is made.

Reliability Considerations

TFTR experience dictates that, when a TF coil develops a fault, either turn-to-turn short or short to ground, a warning must be issued. In the case of a turn-to-turn short, it will be necessary to provide for nearly immediate action to shut down the TF system in a controlled manner. For a short to ground, a warning would doubtless prove sufficient. To implement these requirements, the following steps will have to be taken:

- (a) Parametric Studies of the TF system under fault conditions including:
 - 1. Coil transients and steady state offsets due to turn-to-turn faults.
 - 2. Traveling wave analyses of normal and faulted TF system (both turn-to-turn and coil-to-ground).
 - 3. Safe shutdown scenario analyses.
 - 4. Stress vs. time analyses for various postulated faults to establish action time limit.
- (b) Develop instrumentation to detect faults.
- (c) Develop interface requirements/equipment to control computer.

For TFTR, such studies found that for turn-to-turn faults, shutdown had to be initiated within 0.1 second to avoid structural damage, and optimum safe conditions were maintained when natural L/R decay of the system current was used. The detection instrumentation was designed to provide this action through the power converter controls. The detection equipment permitted action only when transient coil voltage errors exceeded 50 V to avoid false triggering on normal traveling waves. It was found that shutdown initiation was well under the 0.1 second limit. Coil-to-ground faults were handled by a resistive bridge whose output drove a sensitive relay. This device was required

only to issue a warning since it was found safe to finish the pulse in progress, and simply not start the next pulse. The TFTR study also resulted in development of an off-live test concept to verify that coils are good or faulted by using a resonant shift technique. This procedure is to be used during set up, normal maintenance, and to verify a coil fault.

4.4 Bundle Divertor Electrical/Thermal Analysis

Basic parameters are shown in Table 4-5. The design assumptions used (5 second flat-top, 47 kA maximum, 9.4 MA-turn/coil) resulted in a 200-turn coil using 2 kV (no load) forcing voltage. The coil is water cooled using 10°C inlet temperature water at a mass flow of $2.27 \times 10^{-3} \text{ m}^3/\text{s}/\text{coil}$ for a thermal recovery time of ~9.8 minutes.

Two values of axial coil cross section were examined. The first was a $44 \times 44 \text{ cm}$ square cross section. Average turn area, based on using a square conductor and 0.9 linear packing factor, was 7.744 cm^2 , resulting in a current density of $\sim 6.1 \text{ kA}/\text{cm}^2$. This design, however, led to a peak local hot spot temperature of 158°C . This is excessive, of course, so the design was modified to an axial coil width of 64 cm. Using a rectangular conductor, the same linear packing factor, and conductor aspect ratio roughly the same as coil width to coil thickness (64 cm: 44 cm), average turn area is $\sim 10.5 \text{ cm}^2$, current density is $\sim 4.5 \text{ kA}/\text{cm}^2$, and peak local hot spot temperature is 93.7°C , an acceptably safe value.

Like the TF coils, the bundle divertors should be examined for possible problems when a fault develops. Studies similar to those done for TF, but not as extensive or timeconsuming, should be undertaken to assure that turn-to-turn shorts or coil-to-ground shorts will not result in extensive thermal or structural damage. Where problems exist, development should be performed to assure reasonable levels of detectability and implementation or appropriate action.

Also, like the TF coils, the relationships between thermal and mechanical stresses should be carefully checked during detailed design. This will provide reasonable assurance that no hidden problems exist to limit coil life or operating scenario unwarrantedly.

TABLE 4-5

BUNDLE DIVERTOR FIELD COIL CHARACTERISTICS

<u>Parameter</u>	<u>Value</u>	<u>Unit</u>	<u>Remarks</u>
Ampere-Turns	18.8	MA _t	
Number of Coils	2		
Turns/Coil	200	t	
Ampere-Turns/Coil	9.4	MA _t	
Coil Current	47	kA	
Flat Top Time	5	S	
Total Turns	400	t	
Major Radius to Coil Center	~ 5.2	M	
Inside Radius, Copper	35	cm	
Outside Radius, Copper	79	cm	
Coil Width	64	cm	
Field at Coil Center	0.45	T	
Null at Major Radius (R_m)	4.65	M	
I^2R	~54.2	MW	
$LI^2/2$	~51.3	MJ	
Inductance of Coil System	~0.04646	H	
Resistance of Coil System	~0.02455	Ω	
Coil Time Constant	~1.892	s	
Equiv. Square Wave Pulse Time	~6.332	s	
Total I^2t /Pulse	~1.40 x 10 ¹⁰	A ² s	
Total Diss Energy/Pulse	~343.5	MJ	
Energy Recovered/Pulse	~37.4	MJ	
Forcing Voltage	2	kV	No load
Insulation Test Voltage	5	kV	
Weight of Copper/Coil	~6708	kg	
Peak Local Hot Spot Temp.	~93.7	°C	Adiabatic

Table 4-5
(Continued)

<u>Parameter</u>	<u>Value</u>	<u>Unit</u>	<u>Remarks</u>
Max. Current Density	4.495	kA/cm ²	
Water flow/passage	1.45×10^{-4}	m ³ /s	4.572 m/s 0.635 cm
Passages/coil	16		
Water flow/coil	2.27×10^{-3}	m ³ /s	
Est. ΔP	≤ 1.034	MPa	
Thermal Recovery	~ 9.8	min	

4.5 PITR 3 - TF Coil Construction

The most significant obstacle in the manufacture of noncircular coils is maintaining a uniform coil winding tension, thereby insuring a tightly wound coil, control of the radial build, and maintenance of coil shape. With circular coils, this is easily accomplished by providing a winding table with sufficient torque and a tensioning device (usually a friction brake with a feedback loop to regulate the brake shoe pressure). Since the tension during winding is generally not sufficient to yield the copper throughout the cross section, some residual strain energy is present in the coil. Before the tension is relaxed and the coil is removed from the winding mandrel, it is clamped to prevent springback. Application of toroidal wrapping and lead clamps then secures the coil and prevents unwinding. Since the circular coil is in its minimum strain-energy configuration, it will maintain its circular shape after removal from the winding mandrel. This is not true of noncircular coils, however. Since the coil must maintain its noncircular shape after removal from the winding mandrel, it has been the experience of the coil manufacturer of ASDEX and JET coil (both D-shapes manufactured by Brown Boveri Corp.) that the copper must be yielded throughout its entire cross section (stretch formed essentially) and/or overbent to compensate for springback in the straight sections. This procedure requires high winding torque, additional tooling and a specially shaped winding mandrel to accommodate the overbending. The amount of overbend is normally established by trial and error and the mandrel modified to suit. Due to the variation in material properties of copper and the radial build from inner to outer layer, this method probably cannot be optimized and appropriate accommodations must be made in the coil tolerance requirements. This generally leads to problems in fitting the coils to cases and/or appropriate structural supports. The variations in configuration due to this increased tolerance also produce deviations from the minimum bending shape, thereby increasing coil stresses.

To avoid these manufacturing problems and reduce tooling costs and winding torque requirements, the winding mandrel for PITR - 3 coils would become an integral part of the coil case. Fig. 4-21 illustrates schematically the general winding approach. An interior portion of the coil case (~ 3 cm thick) would be bolted to the stiffening spider which is attached to the winding table. A tensioning device, free to pivot, would maintain the tension during winding. To maintain a uniform current density in the nose section, the coil has been designed with varying cross sections. Pre-machined transition pieces will be brazed in to connect one cross section to another.

Fig. 4-21. A TYPICAL WINDING LAYOUT

Fig. 4-22. Coil Construction

After the two pancakes have been wound and clamped, the entire assembly, still attached to the spider, would be removed from the winder. The assembly would then be vacuum bagged and cured in an autoclave @ 350°F. for 2 hours as outlined in the steps below:

- a. Apply a permeable tear cloth such as Armalon[®] to exterior surfaces of coil.
- b. Add additional layers of bleeder cloth as required (number being determined by test sample resin content and strength).
- c. Apply layer of non-permeable Armalon[®] over bleeder cloth.
- d. Install vacuum bag over assembly.
- e. Install in autoclave with appropriate thermocouples to monitor temperature of prepreg.
- f. Draw vacuum of ~ 200 micron and apply ~ 20 psi pressure.
- g. Slowly raise the temperature to 250°F. (~ 3°F./min.) and then apply 100 psi.
- h. Continue heating to 350°F. and hold for 2 hours.
- i. Cool slowly to 150°F. before releasing pressure and removing from autoclave.
- j. Remove bleeder cloth and Armalon[®] sheets.

The coil assembly would then be machined in the nose section and reinsulated. The outer sections of the coil case (machined forgings of nitronic-33 would then be welded to the inner mandrel/coil case). Cooling channels will be provided to protect the coil insulation during welding. After welding, the coil will be potted in the case using a glass bead reinforced epoxy potting compound. At this time, the reinforcing spider would be removed from the coil case. The coil would then be electrically tested at 5 kV to ground and pulse tested at 2 kV turn to turn.

The general approach to the insulation of the coil operating in a radiation environment has been to reduce the radiation flux as much as possible with the coil case providing the shielding. The first ground insulating layer beyond the shielding will be inorganic (mica sht.) and should show no degrading under the anticipated fluence. Beyond this layer, the main turn to turn and coil to ground insulation will be a high temperature novalac. Epoxy prepreg (Cycom 980 - American Cyanamid - or equivalent). Typical properties at room and elevated temperatures are listed in Table 4-6 below. A woven, silane treated E-glass provides the mechanical reinforcement in the prepreg. Note the high temperature shear and tensile properties. Fig. 4-23 illustrates the typical degradation of mechanical properties of glass

TABLE 4-6

Typical CYCOM 980 Laminate Properties

	Kevlar 45	E-Glass
Style	281	7781
Finish	CS 800	A1100
Number of Plies	12	13
Vacuum Dwell at 250°F (120°C)	60 minutes	60 minutes
Cure Pressure, psia (MPa)	100 (.7)	100 (.7)
Cure Temperature, °F (°C)	350° (177°)	350° (177°)
Cure Time	120 minutes	120 minutes
Heat-up Rate, degrees per minute	6°F (3°C)	6°F (3°C)
Bleeder	None	None

Physical Properties

Thickness, inches (mm)	.089 (2.3)	.098 (2.5)
Specific Gravity	1.4	2.0
Resin Content	34%	24%

Mechanical Properties

Tensile Strength, ksi (MPa)		
at R.T.	73 (503)	60 (414)
at R.T., Wet*	77 (531)	54 (372)
at 230°F (110°C)	70 (483)	—
at 230°F (110°C), Wet*	75 (571)	—
at 350°F (177°C)	65 (448)	60 (414)
at 350°F (177°C), Wet*	63 (434)	51 (352)
Tensile Modulus, msi (GPa)		
at R.T.	4.6 (32)	4.5 (31)
at R.T., Wet*	4.6 (32)	4.3 (30)
Compressive Strength, ksi (MPa)		
at R.T.	24 (165)	84 (579)
at R.T., Wet*	19 (131)	69 (476)
at 230°F (110°C)	17 (117)	—
at 230°F (110°C), Wet*	14 (97)	—
at 350°F (177°C)	15.5 (107)	47 (324)
at 350°F (177°C), Wet*	9 (62)	33 (228)
Compressive Modulus, msi (GPa)		
at R.T.	4.6 (32)	4.2 (29)
at R.T., Wet*	3.4 (23)	4.0 (28)
Flexure Strength, ksi (MPa)		
at R.T.	—	102 (703)
at R.T., Wet*	—	103 (710)
at 350°F (177°C)	—	83 (572)
at 350°F (177°C), Wet*	—	57 (393)
Flexural Modulus, msi (GPa)		
at R.T.	—	5.0 (34)
at R.T., Wet*	—	4.8 (33)
at 350°F (177°C)	—	3.9 (27)
at 350°F (177°C), Wet*	—	3.6 (25)
Horizontal Shear, ksi (MPa)		
at R.T.	4.7 (32)	11.8 (81)
at R.T., Wet*	4.2 (29)	11.9 (82)
at 230°F (110°C)	3.6 (25)	—
at 230°F (110°C), Wet*	3.0 (21)	—
at 350°F (177°C)	3.2 (22)	6.7 (46)
at 350°F (177°C), Wet*	2.0 (14)	5.4 (39)

* 281 Kevlar = 30 days at 140°F (60°C), 100% R.H.

* 7781 E-Glass = 18 hour water boil.

Test Methods

The following test methods were used to develop the data herein:

Tensile Properties: Federal Standard 406, Method 1011, Type II

Compression Properties: Federal Standard 406, Method 1021

Flexural Properties: Federal Standard 406, Method 1021

reinforced organic insulators [4.2]. A usable insulating system appears reasonable without significant loss in performance up to 5×10^9 rad fluence. The above mentioned prepreg is expected to exhibit at least as good, if not better, performance under radiation environments.

Fig. 4-23

Since the brazed joint area will be the weakest section of the coil due to annealing, the location, design, and number of joints in the coil is of considerable importance. Experience at PPL indicates that sections of copper induction brazed using a scarf joint configuration similar to that illustrated below can be reliably made without leaks and will perform adequately at moderate stress levels.

Fig. 4-24. Induction brazed scarf joint.

Tensile tests indicate that the joint interface is at least as strong as the parent material; however, recent test data on the fatigue life of similar joints at higher stress (developed by JET, BBC) indicates a shorter fatigue life for joints made with this counterbore and ferrule design due to the stress concentrations at the counter bore transition. With this in mind, it appears appropriate, due to the higher stress levels anticipated in a PITR type TF coil, to reduce the stress concentration by eliminating the counterbore and ferrule and using a simpler butt joint as illustrated below:

Fig. 4-25

Fatigue tests on this joint have been conducted and indicate a minimum fatigue life of 200 kC at a stress level of 16 ksi. This would appear adequate if the joints are located in a low stress region of the coil. This would dictate putting all the joints in the outer region of the coil, near the horizontal midplane, and would require copper extrusions of considerable length (> 60 ft.). Discussions with Kabelmetal of Osnabruck, Germany, indicate the extrusions of 60 - 65 ft. of the required copper cross section are within their capability. They have also demonstrated the ability to extrude heavy copper cross sections with eccentric cooling holes which are required in the PITR (F coil design).

To minimize the thermal stresses in the coil, and permit a reasonable duty cycle, ($\sim 2\%$), it will be necessary to cool each turn of the TF coil individually. At 7 gpm, this will require 2500 gpm for the TF cooling since this water requirement, combined with the other machine subsystems, may put too heavy a burden on the water supply, it may be necessary to have serial paths at a somewhat reduced duty cycle.

4.6 References.

- [4.1] Gralnicks, S. L. and Tenney, F. H., "Analytical Solutions for Constant-Tension Coil Shapes," *Journal of Applied Physics*, Vol. 47, No. 6, June 1976.
- [4.2] McGrath, B., et al., "Mechanical Tests on Irradiated Organic Mat'ls from BBC-Baden", CERN Rep. Lab II - RA (37.40), TM/74-27.

5.0 POLOIDAL-FIELD MAGNET SYSTEMS

5.1 Design Philosophy

The poloidal magnetic field system has two major functions: to induce the current in the plasma and to maintain the plasma equilibrium with the required size and shape. The first function requires the creation of a toroidal electric field by means of a flux rate-of-change in the hole in the plasma loop. The second function requires that appropriate magnetic fields be established in the plasma region.

In previous air-core designs, such as PLT, there has been a tendency to keep these two functions as separate as possible. The induction or ohmic heating (OH) coils are arranged to have a very small stray field in the plasma region, and the equilibrium field coils are disposed so that the total magnetic flux threading the magnetic axis of the plasma is very small. Thus in principle the equilibrium field can be varied (in order to move the plasma radially or to react to plasma tendencies to move) without affecting the plasma current. In practice the indirect mutual coupling via the plasma must be considered as well as the direct coupling, when computing the reaction on one driving source due to a change in the other drive current.

In larger tokamaks where a) the aspect ratio is being made low in order to realize plasma physics objectives at minimum cost, b) the poloidal-field coils are to be placed outside the toroidal field coils for ease of assembly, and c) there is extensive shielding or blanket material between the toroidal field coils and the plasma, it becomes very difficult to find the space near the axis of the machine that is required for the poloidal field coils. In that inner region there are normally coils belonging both to the OH system and to the EF system. The directions of the currents near the axis of such a system are such that when the OH coil current is increasing in one direction so as to increase the plasma current, the current in the EF coil nearby is increasing in the other direction so as to provide the vertical field required. This increase in vertical field can be due to increases in the current and to increases in plasma pressure. It therefore becomes reasonable to enquire whether the two coils can be merged and made to carry only the difference current.

An entirely equivalent way of looking at the problem arises from the observation that the function of the inner coils of an EF coil set is to create

a return flux in the middle which will cancel the vertical field flux linking the magnetic axis. If these inner coils are omitted and the vertical field is produced by coils located principally outside the plasma, raising the vertical field will cause an increase in the flux linked by the plasma. This will cause the plasma current to increase. During the evolution of the plasma to its final state, the vertical EF field will be brought from zero to the value required by the current, size, shape and pressure of the final plasma configuration. An upper bound to the flux swing that can be induced this way is given by the product of the final equilibrium field and the area inside the magnetic axis.

As a measure of the usefulness of this concept, Fig. 5-1 shows the ratio of vertical field flux to the total required inductive flux swing $L_p I_p$ for a circular plasma of minor radius equal to 1.25 m. The rise in flux ratio for large major radius is due to the pressure term in Shafranov's formula for the equilibrium field

$$B_v = \frac{\mu_0 I_p}{4\pi R} \left(\ln \frac{8R}{a} + \beta_p - \frac{3}{2} + \frac{\ell_i}{2} \right)$$

and to the assumption made here that the plasma beta poloidal is (β_p) always equal to the aspect ratio. The figure is drawn for values of ℓ_i , the internal inductance, varying from 0 (skin current) to 1.5 (peaked profiles typical of present tokamak plasmas. The value of ℓ_i cannot, however, be chosen independently of β_p since the two parameters are linked by the plasma equilibrium condition $\nabla p = \underline{j} \times \underline{B}$. The high value (R/a) of β_p chosen for Fig. 5-1 will in general lead to high values of ℓ_i , and thus to values of the flux ratio which are near the lower limit of those illustrated in the figure.

The design approach followed here, which is in fact equivalent to both the above viewpoints, is to establish a certain number of poloidal coils around the plasma cross section, to specify the plasma shape and approximate current density distribution, and to calculate the currents required in the coils to produce this plasma. The only variable left to be chosen is the actual flux number at the magnetic axis, which will represent the amount of flux stored in the system before initiating the plasma. This procedure explicitly assumes that the flux enclosed by the magnetic axis is conserved during the plasma evolution. Each coil pair has its own associated power supply with independent control.

Fig. 5-7. Flux Ratio vs. Major Radius.

5.2 PF Coil Locations and Currents

The currents in the PF coils (whose physical location in the minor cross section are shown in Fig. 5-2) have been calculated by requiring that the combination of coils and plasma current produce a flux contour which fits the assumed plasma shape and current distribution.

5.2.1 The Numerical Procedure. The code used to calculate the coil currents, for given coil positions and plasma current position and shape, assumes that the plasma shape can be taken to be elliptical. A number of points, equal to or greater than the number of coil pairs, are spaced out on the plasma ellipse for the purpose of fitting a flux contour. The ellipse is only used for convenience; any test points could be specified without affecting the computation.

The problem is set up by computing the flux at each test point per ampere of current in each source coil. These quantities fill a matrix $\underline{A}$. The flux at each test point due to an elliptical plasma is then calculated and used to specify the vector $\underline{b}$, which is the inhomogeneous part of the matrix equation,

$$\underline{A} \underline{X} = \underline{b} \quad ,$$

where $\underline{X}$ is the unknown vector specifying the coil currents. If the number of test points is equal to the number of coil pairs the program inverts the equation to find the solution vector for the currents. If more test points are specified a least squares fit to the specified flux points is done. In most cases, especially well conditioned ones, the coil currents calculated are independent of the number of test points chosen. This indicates that the fit to the flux contour between test points is good.

In high power tokamaks a large portion of the required vertical field for equilibrium is a consequence of the high plasma pressure. To model this pressure, the current density in the plasma has been made an increasing function of the plasma radius. This has the effect of moving the magnetic axis out in major radius to a location appropriate to the desired pressure and shape. The function chosen for this study is

$$j(R') \propto (R' - R'')^{1.5}$$

where R' is the distance of the point in the plasma from the major axis and R'' is the radius of the inner edge of the plasma. The variation of current density

Fig. 5-2. Location of poloidal field coils in the minor cross section.

in the axial direction has much less bearing on the results since minor radius quantities enter only logarithmically into the plasma inductance and other related parameters.

Following the matrix inversion to find the coil currents, the program checks the solution by calculating the flux at each test point and by calculating the magnetic field due to the external conductors in the cross section. Then the magnetic axis is located iteratively and the flux there is equivalent. This allows a recalculation using an offset flux number so that the flux value at the magnetic axis has the value chosen.

As in all similar procedures, the method runs into numerical difficulties when the test points are too far from the source coils and cannot, in effect, distinguish between them. This occurs particularly for small plasma minor cross sections situated at large major radii because the source coils are situated more closely on the major axis side of the plasma. Thus the procedure cannot be used directly for the plasma configurations at early times. A way around is to specify a minor radius which is larger than really desired. The coil currents thus arrived at will not differ greatly from those desired since the procedure will produce a nearly uniform vertical field for a round plasma which covers the desired region. The value of this uniform vertical field will differ little from that desired since the minor radius enters only through a logarithmic term.

5.2.2 Results of the Computations. The currents in coils which are located as shown in Fig. 5-2 have been calculated for the plasma configurations shown in Fig. 5-3.

The plasma is initiated at major radius 3.85 m with the help of a hexapole null, described below. The plasma current is increased to 300 kA at $R = 3.85$ meters and then to 3 MA at $R = 3.3$ meters, the plasma shape becoming modestly elongated. The progression from 3 MA to 4.4 MA is a flux conserving compression. The final 4.4 MA plasma is centered at 2.3 meters radius.

The sum of the absolute values of the currents required in the coils is a measure of the dissipation in the poloidal coil system. The total current is plotted in Fig. 5-4 as a function of the arbitrary flux number at the magnetic axis for each of the 4 plasma configurations of Fig. 5-3. Each curve has a minimum which occurs very near the point where the total current (added algebraically) encircling the

Fig. 5-3. Plasma Operating Scenario.

Fig. 5-4. Total current ($\Sigma |I_{coil}|$) as a function of magnetic axis flux number for the plasma configuration of Fig. 5-3. For curves C & D the current density is non-uniform (see text).

major axis is zero. The minimum for configuration A is at the origin. Curves C and D have their minima at the same flux number because the progression from one to the other represents a flux conserving compression. Since the flux enclosed by the magnetic axis during the discharge is conserved, an operating scenario usually takes place on a vertical line in Fig. 5-3. A value near 10 Wb in the figure gives a final state with a very small current in the central solenoid. Since the current in this solenoid is very high at early times, this case represents a full single swing. A value of 6 Wb in Fig. 5-3 typifies a double swing case. In the latter case the solenoid current swings symmetrically about zero.

In order to reduce the current required in the solenoid at early times, coil pair #6 can be used as "leaky" OH coils. Of the total of 10 Wb needed to be stored at $t = 0$, 4 Wb can be produced by currents of approximately 12.4 MAT in coils #6. The current in coils #1 must also be increased by 53 kAT in order to cancel the strong field from the leaky OH. In this arrangement a hexapole null, centered at 3.85 meters, is created. When this is done the operating locus in Fig. 5-4 is no longer a vertical line, but rather as shown by the dashed line. The starting point for configuration A is offset by 4 Wb to 6 Wb because this amount of flux is provided by an external source. The currents in the coils for this operating scenario are given in Table 5-1. It is seen that the total current $\sum |I_{coil}|$ is approximately that which would have been obtained from a purely vertical line at 10 Wb in Fig. 5-4. However, the current on the inboard side of the plasma is now shared nearly equally between the solenoid and the leaky OH coils (#6), making the coil design much less difficult.

The time variation of the currents in the various coils is illustrated in Fig. 5-5. The currents in the outer coils change the most rapidly during the compression; those of the solenoid and leaky OH vary the most during the current induction to 3 MA.

Figure 5-6 shows the flux from the external coils on the equatorial plane as a function of radius for the four plasma configurations A, B, C and D of Fig. 5-3. The solenoid stores 6 Wb at the starting configuration, curve A, as shown by the rapid rise to this value near the axis. The more gentle slope of this curve to the starting null point at 3.85 meters is due to the 4 Wb provided by the leaky OH. The interrupted line shows the progression of the magnetic axis during the plasma operating scenario. The external flux changes very little during the compression phase from curve C to curve D.

TABLE 5-1. Poloidal Field Coil Currents

Flux Number at Magnetic Axis 10 Wb. (Initially 6 Wb in solenoid, 4 in Leaky OH)

Plasma Configuration			A	B	C	D
Plasma Current	MA		~0	0.3	3.0	4.4
Minor Radius	m		-	0.5	1.20	0.85
Half Height	m		-	0.5	1.40	1.40
Major Radius	m		3.85	3.85	3.30	2.30
Magnetic Axis	m		3.85	4.06	3.73	2.59
Coil Locations			Coil Currents (MAT)			
#	R (m)	Z (m)				
1	6.625	0.750	0.068	0.038	-0.94	-3.60
2	6.190	2.310	0.028	-0.009	-0.63	-1.73
3	4.700	3.620	0.098	0.031	-0.23	3.21
4	2.130	3.560	0.726	0.498	-0.35	0.06
5	1.000	2.750	0.523	0.540	-0.59	0.22
6	0.625	2.000	17.181	15.020	-0.10	-0.96
Sol.	0.375	0.0+1.5	16.349	14.232	1.18	1.49
ΣI_{coil}			34.97	30.35	-1.66	-1.31
$\Sigma I_{coil} $			34.97	30.95	4.03	11.28

Fig. 5-5. Time variation of coil currents. Initial stored flux 10 Wb, 6 Wb from the solenoid and 4 Wb from leaky OH coils (#6).

Fig. 5-6. External flux on the equatorial plane as a function of radius for the plasma configuration of Fig. 5-3. The initial stored flux of 10 Wb comprises 6 Wb from the solenoid and 4 Wb from the leaky OH.

The total external flux swing required is seen to be approximately 23 Wb, from the 10 Wb stored in the system before the plasma initiation to -13 Wb in the final state, curve D. In this final state there is no flux due to the solenoid and the leaky OH coils - all the flux is provided by the equilibrium field. For this operating scenario 57% of the total, external flux required is provided by the equilibrium field. This proportion varies somewhat with plasma elongation as shown in Fig. 5-7. Here the plasma current, minor radius, and current density as a function of radius were kept constant while the elongation (plasma half height) was varied. It is seen that circular, or even slightly oblate, plasmas favor the maximization of the ratio of equilibrium field flux to total flux.

5.3 Control of Plasma Position and Shape

Most tokamaks have only two input ports for control action, namely the currents in the OH and EF coil systems. The plasma shape is dictated by the actual field configuration that the EF field system produces. This in turn is determined by the physical location of the coils and their turns. The field configuration is characterized by its curvature index or its multipole components, but these parameters are not available for control. The plasma radial position and the plasma current are the two controllable parameters and the EF and OH currents each affect only their respective counterpart. The control matrix is made diagonal by means of the coil arrangement.

In contrast, the poloidal field system for PITER-3 has up to 8 separate coils whose currents must be controlled. These 8 inputs must be determined by a combination of preprogramming to obtain the desired operating scenario and control to limit excursions about the desired values.

In order to visualize the operation it is useful to characterize each plasma configuration by a series of scalar quantities, in approximate descending order of importance: the plasma current, the major radius, the minor radius, the elongation ratio, the plasma beta, the current distribution, the vertical position, etc. Since one does not anticipate driving the main coils in an asymmetric manner with respect to the equatorial plane, the vertical position must be controlled by appropriate curvature of the field lines or by the inclusion of auxiliary stabilizing or control coils, above and below the plasma.

Fig. 5-7. Ratio of equilibrium field flux to total flux as a function of plasma elongation. The total current and major and minor radii are kept constant.

The coil currents are determined by specification of the plasma parameters listed above and by specifying as well the flux number at the magnetic axis, which is conserved during the time evolution of the plasma. Should, however, the plasma current density distribution or pressure profile be different from the anticipated values, then the plasma position and shape will not be as desired. If the difference is important, then corrective action must be taken. For corrective action to be possible there are two conditions which must be satisfied. First, the need for corrective action must be observable, which implies the existence of a diagnostic capable of measuring the characteristic in question; radial position, shape, etc. Second, there must be available a decoding algorithm which can translate the action required into appropriate changes in the coil's currents.

This decoding algorithm will in general be nonlinear, although in specific circumstances a linear approximation may be useful. The nonlinearity is mainly a function of the plasma's reaction, by changing its equilibrium configuration, to a change in the poloidal magnetic field; the inductance matrix, including a plasma with fixed parameters, is inherently linear. The control of plasma current and radial position are likely quasi-linear. Increments in the respective coil currents can be found which create virtually no change in magnetic field in the plasma region or which do not change the flux encompassed by the plasma. Such increments could be used to change the plasma current or its radial position, respectively.

Control actions on the plasma shape are more difficult since this requires adding a quadrupole component. In general the action to be taken will depend on the major radius at which the plasma is to sit and thus cannot be well separated.

The beta is influenced by compression type scenarios and by the application of neutral beam heating. The major impact of an increase in beta is the requirement of a large equilibrium vertical field or; otherwise the plasma major radius will increase.

The general philosophy envisaged is to have the coil currents preprogrammed in order to provide the required startup and operating scenario. The program used would be modified in the light of operating experience, and critical deviations from the required configuration would be detected and corrective action taken. Although this is consistent with the approach used in present day machines, the existence of multiple inputs, (the various coil currents) will necessitate the use of real-time digital computation for the analysis of performance and the specification of appropriate corrective action required for the cancellation of unwanted tendencies of the plasma to deviate from the desired physical parameters. Such a system would also be required to deal with large scale deviations such as plasma disruption or system fault.

5.4 PF Coil Design

Basic parameters for an illustrative design of Poloidal Field (PF) coils are shown in Table 5-2, "PF Coil Summary". This design was based on the coil currents shown in Table 5-3 which are required for uniform plasma current density. These coil currents are qualitatively similar to those of the non-uniform current density of Table 5-1. This configuration has six coils above and six below the machine midplane, plus a solenoid whose half-height is 1.5 m. The single swing mode was chosen since it requires much less current, and thus energy, during the burn period. Each coil member is tied in series with its mate in the other hemisphere to form a pair which is driven from the power supply. For coil number 6 and the solenoid, it was assumed that the high initial currents would be established by release of stored energy.

(It should be noted that the I^2t , ESW Time and Q_{DISS} columns of Table 5-2 do not include allowance for charging the coils to initial levels, since these should be negligible, and are properly a subject for more detailed design study.)

As discussed in the TF coil section of this report, coil maximum hot-spot temperature is a function of $(\exp J^2t)$ where J is current density, and t is equivalent square wave (ESW) time. Curves of hot-spot rise vs. ESW time as a function of current density are shown in Fig. 5-8. For a maximum hot-spot temperature of 90°C, and a base of 22°C, 50 kA/in² (77.5 A/mm²) was selected for an assumed 2-second ESW in the solenoid, and 30 kA/in² (46.5 A/mm²) for an assumed 5-second ESW in the other coils. From the summary, it is seen that all coils are conservative but coils 3 to 6, are greatly overdesigned. The following changes should be made to the current densities, in a more detailed design study:

- a. Coils 1 and 2 should be sized for $55 \text{ A/mm}^2 < J < 65 \text{ A/mm}^2$
- b. Coils 3-6 should be sized for at least 75 A/mm^2
- c. The solenoid density can go to 85 A/mm^2 .

The above does not relieve the solenoid's major problem, however. As the machine is currently designed there is insufficient space for the required solenoid thickness on the basis of the above data. The available radial dimension, centerline to TF coil case along the midplane, is 0.435 m. The selected mean radius of the solenoid is 0.375 m. Thus the available radial depth for winding is 0.115 m allowing ~0.25 cm for clearance, cooling inlets/outlets and coil support structure. The selected design would require a winding depth

Table 5-2
PF COIL SUMMARY

*COIL	TURNS *1/2-COIL	AREA/TURN (IN ²) (CM ²)		TURN DIM(CM)		TURN* LENGTH (M)	RESIST.* @55°C(Ω)	INDUCT.* (mH)
				a	b			
1	25	1.6	10.323	3.213	3.213	41.63	19.77	24.1
2	42					38.89	16.46	58.9
3	27					29.53	20.78	18.25
4	29					13.38	8.73	7.68
5	33					6.28	3.62	3.55
6	189	1.6	10.323	3.213	3.213	3.93	14.11	35.04
SOL.	702	0.9353	6.034	2.101	2.872	2.36	53.8	82.35

*COIL	SOURCE** VOLT (kV)	MAX CURR (kA)	$\Sigma I^2 t$ 10 ⁶ A ² s	ESW TIME (s)	Q _{DISC} (MJ)	Q _{RET} (MJ)
1	11	46.16	4871.4	2.286	193.4	46.3
2	8	46.17	7723.5	3.624	254.3	111.6
3	5	46.74	3795.7	1.737	157.7	31.8
4	5	46.79	3975.3	1.816	69.4	3.2
5	3	46.76	3951.1	1.807	28.3	0.5
6	7	47.68	3642.4	1.602	102.8	0.1
SOL.	22	46.96	3255.3	1.476	350.3	0.2
*HALF-COIL VALUES ** NO LOAD			TOTALS		<u>1156.2</u>	<u>193.7</u>

Table 5-3 Coil Currents. Single Swing

PTR-3 Poloidal Field Coils

Flux Number - Magnetic Axis: 12 WB (Uniform Current Density).

Configuration			A		C	D	E
Plasma Current	MA		0.1		1.0	3.0	4.4
minor radius	m		(0.5)		1.00	1.20	0.85
half height	m		(0.5)		1.00	1.40	1.40
major radius	m		3.9		3.50	3.30	2.30
Magn. axis radius	m		(3.9)		3.61	3.46	2.41
Coil Locations			Coil Currents (MAT)				
#	R	Z					
1	6.625	0.750	0.022		-0.079	-0.410	-1.154
2	6.190	2.310	0.042		-0.081	-0.405	-1.939
3	4.700	3.620	0.176		-0.126	-0.292	1.262
4	2.130	3.560	1.294		0.462	-0.449	0.850
5	1.000	2.750	1.367		0.510	-0.417	0.582
6	0.625	2.000	8.769		5.302	-0.871	-0.512
7(sol)	0.375	0.0/1.50	31.836		20.533	1.028	1.492
$\sum I_k$			43.51		26.52	-1.82	0.58
$\sum I_k $			43.51		27.09	3.87	7.79

Figure 5-8.

of just over 35 cm. Even if current density were raised to 55 kA/in^2 , the winding depth would be about 32 cm.

To alleviate this problem an additional flux swing can be obtained from coils #6 in the manner of leaky OH-coils. A current of approximately 12.4 MA in these coils will produce a flux swing of 4 WB at $R = 3.85$ when the net field in that region is nulled with approximately 53 kA in coil #1. This procedure in effect permits having the low initial dissipation of a double swing case coupled with the low final dissipation of a single swing case. This region near coil #6 is sufficiently unencumbered that such a pulsed increase is possible. The extremely high forces inherent in the single swing case as described would also be reduced by a factor of nearly 4.

Any current that could be induced by the neutral beams would, of course, remove flux swing requirements from the poloidal coil system, greatly alleviating the problem of limiting coil current density.

5.5 Power Requirements

The last six columns of the coil summary table were derived from calculations of coil voltage requirements made for each significant period of the plasma build-up and burn, and allow for shutdown at the end of the pulse. Table 5-4 shows the terminal voltages for each coil pair and period. Note the assumptions. From these data, and the ensuing calculations, maximum open circuit (no load) power supply voltages were chosen, and Table 5-5, "Power Converters - PF", constructed. It should be noted that the current rating of each converter was raised from 20 kA to 25 kA due to the short pulse times (current rating is a function of pulse time).

5.6 Structural Support

The structures needed to support the various poloidal field coils have not yet been defined. In general, no particular problems are anticipated in finding suitable anchor points for these coils. It is expected that brackets will be attached to the TF coil supports, giving an integrated PF-TF structure.

TABLE 5-4

COIL PAIR TERMINAL VOLTAGE

Coil Pair Number	<u>KILOVOLTS</u>			
	0-0.1 MA	0.1-1 MA	1-3 MA	3-4.4 MA
1	-2.6	-1.87	-2.9	-9.3
2	-5.4	-3.8	-6.2	-22 ⁽⁴⁾
3	-3.6	-2.4	-3	+2.4
4	-1.9	-1.6	-2	+3.8
5	-1.9	-1	-1.5	+2.5
6	-4.9	-3.8	-5.7	+2.5
SOLENOID	-13.2	-9.8	-20	+1.7

ASSUMPTIONS AND NOTES

- (1) Plasma interactions not included. Error estimates are 10% max for 0-0.1MA period, 1% max for 3-4.4 MA period.
- (2) Voltage ~ constant during each period. Current programming used.
- (3) Maximum current in any pair ≤ 48 kA.
- (4) This value obtained by capacitor discharge adding to converter voltage.

TABLE 5-5

POWER CONVERTERS -- PF
Based on 25 kA at 1 KV (N.L.) each

<u>Coil</u>	<u>No. Parallel</u>	<u>No. Series</u>	<u>Total</u>	<u>Double Units</u>
1	2	11	22	11
2		8	16	8
3		5	10	5
4		5	10	5
5		3	6	3
6		7	14	7
SOL	2	22	44	22
TOTALS			122	61

6.0 VACUUM VESSEL DESIGN

6.0.1 The Design Problem. The vacuum vessel of an ignition test reactor must be designed to fulfill several functional requirements. It must: provide vacuum integrity under both normal operating, (1 atm external pressure) and fault conditions (dynamic loads well in excess of 1 atm); meet electrodynamic requirements prescribed on one hand by a requirement of sufficiently high resistance to allow for rapid flux penetration at minimum power dissipation during startup and on the other hand by a requirement to provide a choking mechanism by which excessive coil voltages are limited during a plasma disruption; be provided with adequate cooling to accommodate thermal loads in the range of $.4 - .5 \text{ MW/m}^2$ for a 10-20 s pulse duration; be amenable to surface treatment designed to inhibit the generation of plasma contaminants; and, if possible, be of low Z material construction and provide a secure barrier against the escape of tritium.

Each of these functional requirements can be met in several ways. For example, high vessel resistance can be provided by special bellows sections or by thin gauge construction; a variety of cooling alternatives are available and many different combinations of low Z coatings and structural substrates can be imagined. The selection of a particular design strategy represents an exercise of judgement in which compromises are made between optimal solutions for particular requirements in an attempt to arrive at a concept that is suitable to all of the requirements.

The design concept that we will present here is based on thin gauge double wall construction. A titanium alloy, Ti - 6% V - 4% Al has been chosen as the nominal structural alloy and several surface treatments are being considered. Water has been selected as the coolant medium and both surface boiling and liquid heat transfer analyses have been performed. Fabrication and particularly remote assembly and reassembly of this type of vacuum vessel is a major and yet unsolved problem. Some simplification has been provided by using single curvature sections (see Fig. 6-1), but it is expected that serious bending stress problems will be introduced at the ring joints. The double wall structure and interspace coolant concept provides an excellent tritium barrier, and based on this design the leakage has been estimated to be minimal.

6.0.2 Design Rationale. The most radical aspect of the design that is presented here is the selection of a titanium alloy for use in a hydrogen environment. A propensity toward hydrogen embrittlement is a well documented

Fig. 6-1 PITR-3. Single curvature vessel section. Co-ordinates for vessel eddy current, s' , and known plasma current flux, s , are shown.

weakness of these materials, and their use, which will require the development of an effective hydrogen barrier coating, must be well justified by other desirable properties that they possess. Among these are, high strength especially at elevated temperatures, high resistivity, favorable activation behavior as compared to stainless steel and the ability to form surface layers of a variety of chemical compounds (nitrides, carbides and oxides), in which the structural material is an active chemical constituent. These surface materials can potentially present a tough durable low α surface to the plasma as well as provide an effective hydrogen barrier that will impede hydriding and subsequent embrittlement. Some preliminary evidence of this in fact being a viable concept has been presented [6.1] and it is hoped that more extensive investigations of such titanium surface treatments including exposure to plasma environments will be undertaken. In summary then titanium has been chosen as the base structural material due to its high strength, large range of operating temperatures, high resistivity and suitability to surface coating. It is anticipated that these surface coatings will impede hydride formation and subsequent embrittlement.

Many titanium alloys are available on the commercial market. Ti - 6% Al - 4% V has been selected as the basis of this design based on its common usage in the aerospace industry and the ease with which it may be welded as compared to other alloys. An alternative choice is Ti - 8 Al - 1 Mo - 1 V. This alloy has a higher resistivity, $1.99 \mu\Omega \cdot m$ vs. 1.71 for 6-4, a slightly higher modulus of elasticity and strength properties that are essentially the same [6.2]. Another high resistivity alloy, Ti - 6 Al - 2 S - 4 Zr - 2 Mo has been rejected as it has limited weldability which would compound vessel fabrication difficulties.

Given the physical properties of the selected structural alloy, a design configuration is now evolved that fulfills the requirements prescribed above. The vacuum vessel is a toroidal shell loaded externally by a uniform pressure. On the outboard side (largest major radius) this loading places the vessel in compression (see Section 6.1 for a determination of the structural loads) and consequently it is susceptible to buckling instability. Two possible design solutions are available; either a thick enough gauge of material must be used to provide adequate stiffness to resist buckling or semimonocoque construction using thin gauge material may be used. In the latter design two thin gauge shells are separated by poloidal rings spaced to provide adequate structural stability. Thick wall vessels are difficult to cool, experience large thermal stresses and

require the use of an insulating break. These negative attributes have dictated the choice of semimonocoque construction.

The design concept then consists of a double wall thin gauge Ti alloy vessel in which the interspace region contains the coolant. The structural analysis of this concept will be presented in Section 6.1 of this paper while the thermal analysis is contained in Section 6.2; Section 6.3 discusses vessel fabrication. Before proceeding to these details, however, we present a discussion of the electrodynamic interaction of the vessel with the plasma and coils - this has strongly influenced the selection of the design strategy - and finally close these introductory remarks with a brief discussion of the role of the vacuum vessel in tritium confinement.

6.0.3 Electrodynamic Interaction of the Vessel with the Plasma and the Poloidal Field Coils. A continuous vacuum vessel will interact, electrostatically, with the plasma and poloidal field coils during start up and shut down and in the event of a plasma disruption. At these times changes in the magnetic flux that links the vessel induce currents in the thin gauge shells. The effect of these eddy currents is to impede the transfer of magnetic energy between the poloidal coil system and the plasma and to apply non-uniform forces to the vessel due to the interaction of the currents flowing in the vessel and the magnetic field at its location. This interaction is also a source of energy dissipation, but this is a negligible effect. The vessel design must provide sufficient resistance to permit rapid flux penetration during start up, but should be sufficiently conductive so as to limit the induced coil voltage during a plasma disruption. The characteristic magnetic diffusion time in a material of thickness ℓ and having resistivity η is given by

$$t_m = \frac{\mu_0 \ell^2}{\eta} \quad (6-1)$$

Providing the desired start up or current transfer time is much longer than t_m for the vacuum vessel it poses no impediment to the start up process. This establishes the condition that

$$\ell^2 \ll \frac{t_s \eta}{\mu_0} \quad (6-2)$$

If t_s is chosen as 20 msec., a time appropriate for compression during start up and ignition, and if $\eta = 1.71 \mu\Omega \cdot m$ (Ti - 6 Al - 4 V) then

equation 6-2 is easily satisfied for thin wall designs in which the penetration depths are of the order of 1 cm.

During a major disruption the plasma current will be reduced rapidly from the operating value to nearly zero in a time that may be short compared to the eddy current decay time of the vacuum vessel. In PLT for example a current $I_p \sim .5$ MA is lost in approximately 1-2 msec. During this event negative voltages are measured across the ceramic break of the order of 500 V, (as compared to the normal 1 ~ 2 V OH driving voltage). In a continuous vacuum vessel toroidal eddy currents will result. The time scale of this event is not the same in different devices and there is considerable variability in the available data. In the smaller ATC and ST devices the characteristic time scales were approximately 250 msec. As these machines were approximately a factor of 3 smaller in radius, this information tends to support a scaling of this phenomena with the square of the minor radius as suggested by Lackner and Karger [6.3]. However, as they point out, data that gives a time scale a factor of 20 shorter than their estimates have been observed. In light of this information the model that we consider is to assume a sudden loss of the plasma current on a time scale that is very short compared to the resistive time scale of the vessel. The resulting distribution of eddy currents is then given by the solution of the following Fredholm equation of the first kind, .

$$\psi(s) = \oint j(s') G(s', s) ds' \quad (6-3)$$

(The notation of equation 6-3 is defined in Fig. 6.0-1.)

The kernel $G(s', s)$ is given by

$$G(s', s) = 2\mu_0 \sqrt{R(s)R(s')} k^{-1} \left[\left(1 - \frac{k^2}{2}\right) K - E \right] \quad (6-4)$$

in which

$$k^2 = \frac{4 R(s) R(s')}{[R(s) + R(s')]^2 + [Z(s) - Z(s')]^2} \quad (6-5)$$

K and E are complete elliptic integrals of argument k .

This problem is analogous to the virtual casing problem of Shafranov and Zakhavov [6.4,6.5]. In their problem, the flux or field interior to a shell is known and the current on the shell is determined. In this problem $\psi(s)$ is the

known flux, produced by the plasma current, exterior to the vacuum vessel and we seek to determine the distribution of current on the vacuum shell.

Numerical techniques for the solution of this problem are available [6.6,6.7], and once $j(s')$ is found the force on the vessel is determined by the interaction of this current and the local poloidal field. An estimate of the over pressure that would be felt instantaneously can be made by assuming that the shape of the vacuum vessel conforms to that of a flux surface of the equilibrium in which case the pressure will be given by the local values of the poloidal field energy density of the plasma. Based on this model we estimate the maximum local over pressure to be 4 atm.

6.0.4 Role Of The Vacuum Vessel In Tritium Containment. As the vacuum vessel serves as the primary or first tritium barrier in an ignition test reactor, it is important to consider the proposed design in this context. Two questions must be addressed: What amount of tritium will be held up in the vacuum vessel? and, What amount will permeate the vacuum wall into the cooling region? An assessment [6.8] has been made of these problems based on the assumptions: that the vacuum vessel wall area is 300 m^2 , and that the contained volume is 240 m^3 , the wall thickness is 3 mm, the temperature of the wall on the plasma side is 300°C and that on the coolant side is 100°C , the fill and pump out time is 30 s, during a pulse 20 - 40 kC_i of tritium are in the vacuum chamber, that 2000 such pulses define an operating year and that the machine lifetime is 5 operating years.

Under these circumstances it has been estimated that 12 C_i of tritium will permeate the vessel wall and that 27 kC_i will be held up in the vessel material.

It should be pointed out that the required data on tritium permeation and solubility in titanium are not readily available in the literature and consequently the estimates offered here are at best an approximation. The effects of oxides and other potential barrier coatings such as nitrides and carbides of titanium are not known and have not been taken into account in the above estimates.

6.1 Structural Analysis

The ITR vacuum vessel is shown in perspective, plan and elevation views in Fig. 6-2. The sectors (see Fig. 6-3) are simple curvature cylindrical segments of approximately elliptical cross section, which are joined with stiffener rings at eight locations about the torus. Each sector comprises 45°. The vessel wall construction consists of two thin titanium alloy plates separated by spacer webs that are also made of the same titanium alloy. The determination of the required gauges of the plates and webs and the web depth and spacing are the subject of this section.

Thin wall shell structures such as this are frequently analyzed by the use of the membrane shell theory [6.9,6.10], that is generally valid if the shell thickness is small compared to the radius of curvature of the shell. The resulting solutions are physically realizable if deformations that are compatible with the geometry of the shell result, and it has been shown that in the case of a toroidal shell this condition cannot generally be met [6.11], the deformation predicted at the crown of the toroid being infinite. Membrane stress solutions therefore have little actual design value and at best offer an approximation of the stress conditions at points of the toroid that are far from the crown points.

Membrane stress solutions have been found for elliptical shaped cross section vacuum vessels [6.12]. Using formulas quoted in reference [6.12] we find that the maximum poloidal and toroidal stress components are

$$\sigma_T = - \frac{2.743 \times 10^8}{h} \text{ (N/m}^2\text{) / (Atm. Load)} \quad (6-6)$$

and

$$\sigma_P = - \frac{2.576 \times 10^8}{h} \text{ (N/m}^2\text{) / (Atm. Load)} \quad (6-7)$$

in which h is the effective material thickness in mm which consists of the area of the plates in the case of the toroidal stress and that of the plates and webs in the case of the poloidal stress. These stresses occur on the equatorial plane of the vessel on the large major radius side. For high strength titanium alloys with yield stresses in the range of 500 to 700 mega N/m² at temperatures as high as 300°C, structures with thin wall construction ($h \sim 1$ cm: total thickness) will be adequately strong.

PITR-3 VACUUM VESSEL

Fig. 6-2 PITR-3. Vacuum vessel plan, elevation section and isometric views.

PITR-3 VACUUM VESSEL SECTOR

Fig. 6-3. PITR-3. Single curvature vessel section, sketch for beam column and free ring buckling models shown.

A more critical consideration is that of static buckling. To estimate the required member sizes and spacings and simultaneously bolster our confidence in the membrane stress estimate, we will consider the following two analyses. First, we treat a unit height strip of the vacuum vessel in the equatorial plane spanning between two of the stiffener rings as a beam column, carrying a uniform running load and with variable end restraint. The axial loading of this beam-column results from the reaction force of the stiffening rings (the geometry of this problem is pictured in Fig. 6-3). Secondly, we will consider a ring element taken from the vacuum vessel at the mid point between the two stiffening rings as an unstiffened ring under a uniform inward load (see Fig. 6-3). These problems which can be analyzed for critical collapse loads provide a conservative estimate of the required material sizes and spacing.

Considering a circular ring of 1.56 m radius we find from well known formulas for the critical load on a circular ring that the required moment of inertia of the cross section in order to maintain neutral stability is

$$I = P \times 1.56 \times 10^2 \text{ (mm)}^4 \quad (6-8)$$

where P is the load in atmosphere. The ring cross section is of the form of and "I" consisting of 3 mm plates and 10 cm wide by 66 mm deep including the plate thickness. The moment of inertia of this section is $1.136 \times 10^6 \text{ (mm)}^4$ and consequently stability of the ring is assured.

For a fixed ended beam column of unit width on the outboard horizontal mid-plane the moment of inertia required to maintain neutral stability is

$$I = P \times 4.42 \times 10^3 \text{ (mm)}^4 \quad (6-9)$$

This moment is provided by the two 3 mm plates which are separated by the 6 cm interspace. The resulting moment of inertia is $2.728 \times 10^6 \text{ (mm)}^4$ and consequently neutral stability is available at the 4 atmosphere fault load condition. It should be noted that a generous margin of safety is allowed between the design and neutrally stable condition and that these estimates are of a conservative nature in that they do not consider the additional rigidity provided by the structural shape. If instead of assuming a fixed end constraint on the beam column solution pin joints were used, the required moment of inertia would be increased by a factor of 4 which is still within the capacity of the structure.

Based on the above considerations, the plate thickness has been chosen at 3 mm and the plate spacing at 6 cm. The webs which are also 3 mm thick must be spaced frequently enough to prevent excessive deformation of the plates between the webs. For a spacing of 12.7 cm this deflection is limited to 1 mm, and this can be reduced sharply by placing the webs closer together - at 8 cm spacing the deflection is limited to .15 mm.

6.2 Thermal Analysis

6.2-1 First Wall Temperature Limits. It has been presumed that embrittlement of the titanium vacuum structure will be inhibited by a suitable barrier coating. At temperatures in excess of 300°C titanium oxide is known to be soluble in titanium metal [6.13] with the solubility increasing as a function of temperature. At elevated temperatures the barrier coating would be absorbed into the base metal and the resulting metal surface would be subject to hydrogen permeation. Consequently, we limit the maximum first wall temperature to 300°C, and provide a coolant design adequate to maintain this limit.

Three methods of cooling are investigated: (1) Surface boiling heat transfer to a flowing liquid coolant; (2) Turbulent heat transfer to a flowing liquid coolant; and (3) Gaseous coolant flow between the walls.

6.2-2 Surface Boiling Heat Transfer. In surface boiling heat transfer the surface in contact with the fluid is above the boiling temperature of the fluid. The bulk temperature of the fluid is below the boiling temperature of the fluid. Fluid vapor bubbles form on the wetted surface, escape into the bulk fluid destroying the heat transfer film and then condense in the cool bulk liquid. This brings the cool bulk fluid intimately into contact with the wall resulting in extremely high heat transfer rates. Fig. 6-4 taken from ref. [6.14] is a plot of heat transfer rates in surface boiling heat transfer against the bulk to wetted surface temperature difference for various fluid velocities and subcool temperature differences. The difference between the fluid boiling and bulk temperatures is the subcool temperature difference. Surface boiling occurs to the right of the break point on the curves. Conventional liquid turbulent heat transfer occurs to the left of the break point.

To study the transient temperature rise of the first wall during a burn cycle with surface boiling heat transfer, the following heat transfer model is proposed. It is assumed that at time zero, at the start of the energy input to the wall from the plasma, the wall and the cooling water are at 89°C. The temperature of the wall then begins to rise and the flowing water cools the wall by liquid turbulent heat transfer. For this condition an average turbulent heat transfer film coefficient is derived from the data on Fig. 6-4. Here 20°F subcool and a fluid velocity of one ft/sec is assumed. In this linear model, the heat transfer rate per unit area to the water is the product of the average film coefficient and the wall-fluid temperature difference. When the

Fig. 6-4. Effect of Velocity and Degree of Subcooling on Surface Boiling of Water (taken from Industrial Engineering Chemistry, Vol. 41 (1949)) with permission of American Chemical Society.

average temperature of the wall reaches 111°C surface boiling heat transfer comes into play. An average surface boiling heat transfer film coefficient is derived from two points on Fig. 6-4. They are at heat transfer rates of 5×10^5 and 2.5×10^4 BTU/hr ft² at 20°F subcool and a fluid velocity of 1 ft/sec. Again the heat transfer rate to the water during the surface boiling is the product of the derived average film coefficient and the wall-fluid temperature difference for this linear model. Comparison of the wetted wall temperature at various surface boiling heat transfer rates for the linear model with those obtained using the data of Fig. 6-4 show temperature differences of no more than 3.5°C.

In ref. [6.15] an expression is given for the transient temperature in a slab of thickness, λ , after the initiation of a thermal power flux F_0 from the plasma at time $t = 0$, with linear rear surface cooling. The equation is solved for the transient temperature on the front surface of the slab for various thicknesses. The model used is that described above, namely turbulent liquid heat transfer with a wall temperature varying from 89°C to 111°C and surface boiling thereafter. Figure 6-5 is a plot of the transient front surface temperature during a burn cycle for various slab thicknesses. A power density of 0.4 MW/m² into a titanium wall is used. The results show that the 0.3 cm wall reaches steady state heat transfer in 3.5 sec. The maximum temperature on the front surface of the slab is 278°C. This is below the 300°C maximum allowable cited earlier; therefore, surface boiling heat transfer is an acceptable first wall cooling method at a cooling water temperature of 89°C.

The 0.5 cm wall reaches steady state heat transfer in 17.5 s. Therefore, all walls less than 0.5 cm thick will reach steady state heat transfer prior to the end of a 20 s burn cycle. As a result then, it is possible to investigate surface boiling by a simpler steady state analysis. In the following sections turbulent liquid cooling and the gaseous cooling models will be investigated by steady state analyses also.

6.2.3 Turbulent Heat Transfer to a Flowing Liquid Coolant. For water at 93°C the forced convection heat transfer film coefficient can be expressed as [6.16]

$$h_f = 0.3044 \frac{V_m^{0.8}}{D^{0.2}} \quad (6-10)$$

where h_f is in watts/cm²°C, V_m m/sec and D an effective length given by four times the area of the channel divided by its wetted perimeter, is in m [6.17].

Fig. 6-5. Surface Temperature Adjacent to Plasma vs. Time for Titanium First Wall of Thickness 1. Water Cooling on Outside Surface from $T = 89^\circ\text{C}$ to 111°C and then Surface Boiling Heat Transfer. Thermal Power Flux to Wall = 40 Watts/cm^2 .

To avoid boiling in liquid turbulent heat transfer, the wetted wall temperature must not exceed 100°C. In steady state heat transfer the expression for the front surface T_{FS} and the rear wetted surface T_{RS} temperature are

$$T_{FS} = \frac{2F_0}{K} + T_{RS} = \frac{2F_0}{K} + \frac{F_0}{h_f} + T_w \quad (6-11)$$

where T_w is the cooling water temperature. Using expressions 6-10 and 6-11, the front and rear surface temperatures have been plotted against the fluid velocity, Fig. 6-6, for a channel width $w = 12.7$ cm, height $h = 6$ cm, wall thickness $\lambda = 0.3$ cm and a thermal flux $F_0 = 0.4$ MW/m². The assumed cooling water temperature is 0°C. Results for other cooling water temperatures can be obtained by adding that temperature to the results of the curve. The variation of the physical properties of the water in the temperature range of interest do not significantly affect the value of the film coefficient and hence the wall temperature.

Figure 6-6 shows that the wetted rear wall temperature governs the design. For a cooling water temperature of 0°C a water velocity in excess of 0.7 m/sec will result in front and rear wall temperatures that are lower than the maximum allowable values. Higher cooling water temperature will require higher fluid velocities. The results show that turbulent heat transfer to flowing water is an acceptable first wall cooling method.

6.2.4 Gaseous Coolant Flow. Turbulent heat transfer to flowing helium is investigated. A fluid velocity of 30.5 m/sec and coolant temperature of 0°C is chosen. Front wall temperatures are calculated as a function of helium pressure using the forced convection film coefficient relation. The results are shown on Fig. 6-7. Results for other coolant temperatures can be obtained by adding that temperature to the results of the curve. Helium properties used in the calculation were evaluated at 50°C. However, the variation of the physical properties of helium in the temperature range of interest will not significantly affect the film coefficients. The results obtained for helium are typical of what one would expect for other coolant gases.

The results of Fig. 6-7 show that adequate heat transfer rates are attained at gas pressures in excess of 35 atmospheres. The required high coolant pressure is acceptable with the thin wall but high structural strength vessel design proposed but would require high coolant pumping power and is therefore not considered further.

Fig. 6-6. Front Wall T_{FS} and Wetted Rear Wall T_{RS} Temperature with Turbulent Liquid Cooling, Cooling Water Temperature = 0°C .

Fig. 6-7. Front Wall Temperature With Turbulent Heat Transfer to Flowing Helium, Helium Coolant Temperature = 0°C .

6.2.5 Cooling Water Quantity and Distribution. The above analysis of the two acceptable methods of water cooling be viewed in another way. If a coolant temperature can be found that will maintain the front and rear wall temperatures below certain specified values then that cooling method is deemed acceptable. However, the coolant temperature will rise as it flows through the coolant passages. Furthermore, there is a maximum value allowed for the coolant temperature. In this section of the report we will address the problem of how much coolant is required and how it is to be distributed to satisfy the condition of maximum allowable front and rear wall temperatures and maximum allowable coolant temperature.

The temperature of the coolant will rise due to the reactor thermal output and the neutron output absorbed by the coolant. We can define the factor f_{th} , the fraction of thermal power flux to total power flux deposited into the cooling medium.

$$f_{th} = \frac{F_O}{F_O + F_N} \quad (6-12)$$

where F_N is the neutron power flux deposited into the cooling water. That flux can be related to the neutron power flux incident on the first wall through the usual exponential decay relation. The e-folding distance for neutron absorption in the water is 8 cm. The cooling water requirement W_C in m^3/sec is

$$W_C = \frac{F_O A}{\Delta T C_p f_{th}} \quad (6-13)$$

where A is the inside surface area of the vacuum vessel and ρ and C_p refer to the coolant. A sketch of a reference design of the vacuum vessel is shown on Fig. 6-8. Each 45° segment is composed of two 22.5° sectors. The present design shows a total of 13 channels per sector. Three of these channels completely envelop the vacuum vessel around the poloidal circumference. The other ten channels envelop only the outer part of the poloidal circumference, starting and ending at a segment parting plane or a center plane. Dimensions of the channel are shown on the sketch.

As the coolant flows in a channel from the inlet header, its temperature will rise. At the point where its temperature reaches the maximum allowable coolant temperature it must flow out of the channel and into the return header. The

Fig. 6-8. Vacuum Vessel.

length along the channel from the inlet to the outlet is called the run length, L_R . An expression for L_R can be derived by taking the ratio of the allowable temperature rise of the water ΔT to the temperature rise per unit length of the run.

$$L_R = \frac{\Delta T C_p h V f_{th}}{F_0} \quad (6-14)$$

If we assume that the cooling water will flow in runs of equal length in each sector, then an expression for the number of runs per sector, N_R , can be derived by dividing the total channel length per sector by the run length.

$$N_R = \frac{N_C (2 - f_v) \left[\cos \frac{\Theta_S}{2} f_v R_v + h_v \right] F_0}{\Delta T C_p h V f_{th}} \quad (6-15)$$

where h_v is the vessel height, N_C is the number of channels per sector (13 in the reference design) and f_v , R_v , Θ_S are derived on Fig. 6-8. Assuming PITER reference design dimensions shown on Fig. 6-8 and $h_v = 4.2$ m, $h = 5$ cm, $N_C = 13$, $F_0 = 0.4$ MW/m² and with a calculated value of $f_{th} = 0.34$ for a neutron power of 400 MW and a thermal power of 100 MW equations 13-15 yield

$$W_C = \frac{1.178 \times 10^6}{\Delta T} \quad (6-16)$$

where W_C is in GPM and ΔT in °C

$$L_R = 3.365 (\Delta T) (hV) \quad (6-17)$$

where L_R is in m and hV in m²/sec

$$N_R = \frac{37.39}{(\Delta T) (hV)} \quad (6-18)$$

In turbulent liquid heat transfer, for water entering a channel at 0°C for a maximum wetted wall surface temperature of 100°C, the allowable temperature rise of the cooling water in a run, L_R , is

$$\Delta T = 100 - \frac{F_0}{h_f} \quad (6-19)$$

The allowable coolant temperature rise has been plotted against the water velocity for velocity for various channel depths, h (Fig. 6-9). The curves are for a thermal power input of 0.4 MW/m^2 . Results for other higher coolant inlet temperatures can be obtained by subtracting that temperature from the results of the curve.

For a channel depth, $h = 6 \text{ cm}$, the following workable liquid heat transfer schemes are designed and given in Table 6-1. The maximum wetted wall temperature in all cases is 100°C .

Investigation of the data given on Fig. 6-4 at heat transfer rates of $1.3 \times 10^5 \text{ Btu/hr ft}^2$ (0.4 MW/m^2) shows that surface boiling will occur for a fluid velocity of 1 ft/sec (0.30 m/sec), at subcool temperature ranges from 20 to 100°F (11.1 to 55.6°C). The data for a coolant velocity of 4 ft/sec (1.2 m/sec) shows surface boiling at 20°F subcool and turbulent liquid heat transfer for all other subcool temperatures at the same heat transfer rate. The data for 12 ft/sec (3.66 m/sec) coolant velocity shows turbulent liquid heat transfer at all subcool temperatures at 0.4 MW/m^2 . The only data available where surface boiling takes place over a range of subcool temperatures is at 1 ft/sec therefore it is only case investigated.

In surface boiling the inlet and outlet temperatures to the run, L_R , are fixed by the maximum and minimum subcool temperature. A 100°F (55.6°C) inlet subcool temperature corresponds to an inlet fluid temperature of 44.4°C . A 20°F (11.1°C) outlet subcool temperature corresponds to an outlet fluid temperature of 88.9°C . Corresponding values of the temperature drop between the wall and the bulk liquid are 66.7 and 28.9°C . In surface boiling heat transfer, the wetted wall temperature will vary between 111.1°C on inlet to 117.8°C on outlet. The maximum front wall temperature will be 282.5°C . Using equations 6-16, 6-17, 6-18 with the values of the inlet and outlet coolant temperatures given above, an acceptable design is obtained and given on Table 6-1.

Fig. 6-9. Allowable Coolant Temperature Rise in Turbulent Heat Transfer to a Flowing Liquid Against Water Velocity in Channel, V , for various channel depths.

Table 6-1. ACCEPTABLE COOLANT DESIGN SCHEMES FOR TURBULENT LIQUID
AND SURFACE BOILING COOLANT HEAT TRANSFER.

TURBULENT HEAT TRANSFER TO FLOWING LIQUID						
COOLANT VELOCITY, $\bar{v}$ m/sec	COOLANT INLET TEMP. $T_{in}, ^\circ\text{C}$	COOLANT OUTLET TEMP. $T_{out}, ^\circ\text{C}$	RUN LENGTH L_R , m	NUMBER OF RUNS N_R	COOLANT TEMP. RISE ALLOWABLE $\Delta T, ^\circ\text{C}$	COOLANT FLOW RATE $\dot{V}_c$ GPM
1	0	20	4.04	31	20	58900
2	0	54	21.81	6	54	21810
	30	54	10.10	12	25	47120
	50	54	2.02	62	5	235600
3	0	67	40.58	3	67	17580
	30	67	22.41	6	37	31840
	50	67	10.30	12	17	69290
SURFACE BOILING HEAT TRANSFER						
0.305	44.4	88.9	2.77	45	45	26180

6.3 Fabrication Considerations

The thin double wall titanium vessel concept is in one respect simpler to fabricate than stainless steel vessel in that no bellows or ceramic breaks are required. However, the curved stiffeners between walls which also act as cooling water channels must be manifolded at mid-plane (see Fig.6-8) and around access ports. Moreover, many headers are needed to tie in with the cooling water system. The vacuum vessel is therefore characterized as having a very high density of weldments (or other form of joining) and pre-formed webs. Cost will as a result be greatly dependent on how efficiently these two processes can be accomplished.

The fabrication of the proposed double-walled titanium vacuum vessel for the PITER really entails two problems. First, a practical and cost-effective approach must be developed for fabricating the individual toroidal vacuum vessel sections. Second, a practical and technically sound approach must be developed for joining the individual sections together to form the toroidal chamber. The latter subject is intimately connected with the requirements for remote assembly/disassembly and maintenance and is discussed in Section 7.2.

Each section of the approximately 10 meter diameter toroidal vacuum chamber consists of a pie-shaped segment having a 3.5 by 4.5 meter bore. The proposed double-walled shell of each segment must be structurally rigid and of sufficient strength to support the combined atmospheric pressure loads, normal operating loads, plasma disruption loads, and seismic event loads. The shell may have either simple or compound curvature depending on whether the segment is a truncated right cylinder or a true segment of a torus. The inner wall of the vessel ideally consists of a single, continuous piece to minimize welded joints, and thereby potential vacuum leaks. The outer wall serves to provide a coolant cavity required for operation as well as structural rigidity.

Five fabrication approaches (Fig.6-10) were examined, four of which use standard welding techniques. While the four welding approaches are feasible, detail part fit-up and required tooling may present difficulties. The fifth approach employs a processing technology particularly amenable to titanium, superplastic forming and diffusion bonding (SPF/DB). The SPF/DB approach combines the ability of titanium alloys to undergo extensive elongation or deformation with extremely low stresses at elevated (1700°F) temperatures with the ability of all metals to form a continuous bond across an interface under

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Fig. 6-10. Thin double wall titanium vessel concept, fabrication approaches.

pressure at elevated temperature. When performed concurrently, SPF and DB reduce part count, reduce or eliminate fasteners, improve material utilization, and increase design flexibility. Fabrication costs using this technique are projected by the aerospace industry to be 30-50% lower than conventional techniques.

The resulting structure consists of a truss core. This approach minimizes joints at the inner wall, provides excellent structural integrity and efficiency, and provides integral cooling channels. Since the preferred ratio of skin-to-core thickness in a truss core is 3 to 1, the thermal requirements of the wall may be satisfied as well by a superplastically formed/diffusion bonded structure as by a weld built-up structure. Particular advantages of the SPF/DB approach are that both simple and compound curved structures can be readily fabricated in a single-step operation.

While SPF/DB technology is developing rapidly for aircraft and aerospace structures, specific forming problems must be addressed for the proposed vacuum vessel structure. Table 6-2 gives a qualitative comparison of manufacturing complexity and relative cost between the five approaches.

Table 6-2

SUMMARY

TORUS - DOUBLE WALL ALTERNATES

DRAWINGS	RELATIVE COST	ADVANTAGES	DISADVANTAGES
Sketch #1	1	Limited Sub Assy's Req'd.	Discontinuous inner & outer skins Full machine Machine tooling Billet or Forging (Long Lead) Multiple Leak Paths
Sketch #2	0.6	Continuous inner & outer skins Built-up bulkheads (rolled flanges & cut webs) Ease of MFG	Sub Assy tooling Templates
Sketch #3	0.8	Continuous inner & outer skins No Sub Assy Req'd; Less Welding Reduced Material Cost	Forming Tools Req'd Machine Tooling
Sketch #4	0.6	Continuous inside skin Ease of MFG No Sub Assy's; Fewer parts	Discontinuous outer skin Multiple Leak Paths Templates Req'd
Sketch #5	0.5	Reduced Parts (Less Machining) Reduced Material Reduced Welding Labor, Tooling Reduced Joints (Leak Paths) Simplified Forming Operation	Tooling Costs Requires Higher Cooling Flow Inspectibility (Requires Rigid Control)

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7.0 NEUTRONICS, MAINTENANCE AND TRITIUM

7.1 Shielding Requirements and Performance

7.1.1 Mode of Operation. Although the PITR will initially be operated with pure hydrogen and then deuterium plasmas, its reason for existence is the demonstration of ignition and the sustained burning of deuterium-tritium fuel. It is anticipated that fusion power levels of 400 to 1000 MW can be obtained, with pulse lengths of at least 9 seconds.

The DT neutrons will be distributed over the entire first wall (with an area of 227 m^2) and constitute a surface source for the shield. Most results in this section will be quoted relative to either: a nominal wall load of 2.2 MW/m^2 (625 MW fusion power); or a nominal integrated wall load of 1 MJ/m^2 . The design lifetime of the device is taken to be 10^4 pulses averaging 10 seconds each at $2/3$ the nominal power, spread over 4 years.

The actual wall load will vary around the poloidal circumference of the vacuum vessel. Figure 7-1 shows the distribution of the current (wall load) and flux of DT neutrons, referenced to a racetrack-shaped wall approximating the PITR. At the inner ("hub") midplane the current is about 82% of the average value, while at the outer ("tread") midplane the current is about 118% of the average. This variation has only a slight effect on the shield design, since the thickness varies logarithmically with the load.

7.1.2 Limits and Constraints. There are three shield systems on the PITR. The primary shield between the vacuum vessel and the TF coils is designed to limit the nuclear heating of the coils and to prevent excessive radiation damage. The secondary shield is a pillbox surrounding the device; this reduces neutron leakage to the test cell and provides protection from decay gammas. The test cell walls form a final biological shield for the protection of the public.

Fig. 7-1. Distribution of the DT neutron current (J) and flux (F) around a racetrack-shaped wall approximating the PITER vacuum vessel. J_{nom} is the average wall load.

Since the PITR coils will all be normal copper, not superconducting, the only form of radiation damage of concern is degradation of the insulating material. This is assumed to be glass-reinforced epoxy, capable of withstanding 10^8 Gy (10^{10} rad, 1 Gray = 100 rad) absorbed dose. Ordinary epoxies are good to 10^7 Gy; special formulations such as DDS-Novalac have improved radiation tolerance.

Nuclear heating of the coils should be insignificant compared to the ohmic heating, but heat deposition in the shield itself (and the first wall) will be large. There is no absolute limit on this, but it must be compatible with the cooling capabilities provided.

Health and safety regulations establish limits on allowable radiation exposure near the PITR. At the site boundary, a limit of 5 mrem per year is appropriate. Within the test cell occupational exposure guidelines can be applied, and the following limits are proposed: 1 rem total per year; 2.5 mrem/hr (continuous) after 7 days shutdown; 20 mrem in 4 hours exposure after 24 hours shutdown; and 100 mrem in 1/2 hour exposure after 4 hours shutdown. The design of shielding to minimize activation and to attenuate decay gammas will be controlled by these limits, together with the extent of maintenance required and remote handling provided.

7.1.3 Selection of Materials

In the preliminary PITR design a lamination of iron and boron carbide was proposed for the primary shield, based on its use in a number of other recent reactor designs. However, this combination has several drawbacks, including: the basic cost of the boron carbide; the cost of suitable containers for it; and the difficulty of cooling such a shield.

Consequently several other combinations of materials were investigated for shielding effectiveness in a simple (cheap) 1-D slab configuration. In

addition, the optimization code *SWAN* was used to improve the distribution of materials for a given class of shields. The results of this exercise were surprising. The dose behind a 35 cm thick iron-water shield was only 25% higher, and with Boral (as a convenient thermal poison) only 13% higher, than behind the iron-boron carbide shield. This indicates that the greatly increased cost and complexity of an iron-boron carbide shield would give about a 2 cm saving in thickness compared to a simple iron-water shield. (This is not a contradiction to the conclusions of other investigators; it is a reflection of the minimal shielding thickness required for the Pitr.)

Figure 7-2 shows the composition of the nominal Pitr primary shield. It is 35 cm thick overall, including the vacuum vessel and the coil case. The iron-water region averages 24% water and 71% iron (by volume) with a 5% allowance for clearance gaps. The designation "iron" is used loosely; any non-magnetic steel could actually be used (SS-304 was assumed for the shielding calculations). Mechanically, the primary shield will be assembled from 1 to 2 cm thick slabs of steel, separated by button spacers to provide water-filled gaps. In the hub region, the steel will be integrated into the coil support systems.

The pillbox (secondary shield) occupies very valuable space, since it directly surrounds the device and forces auxiliary equipment farther away. Although the use of high performance shielding does not seem to be cost effective, the bulk of ordinary concrete would be unacceptable. Consequently a special shielding mortar is proposed for this application; Chemtree 1-6-26-5 was used as an example for this shielding analysis.

The test cell wall and roof constitute major elements of the shield, but their size and composition are essentially determined by structural considerations. For the Pitr the roof will be 6 feet thick because of the span required, while

Fig. 7-2. Composition of the nominal PITER primary shield, 35 cm thick overall (including the vacuum vessel).

Fig. 7-3. Nuclear heating of the first wall and primary shield. (The lower curve gives the portion of the heating per unit volume of shield which occurs in the water; i.e. it is not divided by the water volume fraction to give the power density in the water itself.)

the walls will be 5 feet thick to support the roof. Ordinary concrete is assumed to be the structural material, but with 0.5 wt % boron added to a 30 cm layer on the inside.

7.1.4 Primary Shield Effectiveness. The PITER was modeled as a cylinder with vacuum wall radius of 1.55 m and TF coil inner radius of 2.00 m in order to perform 1-D neutron and gamma transport calculations. The performance of the shield was evaluated from the results of these calculations.

Insulator radiation damage was assessed using absorbed dose cross sections (kerma factors) for a high performance epoxy (100 parts Novalac to 35 parts DDS) mixed 2-to-1 by weight with glass fiber reinforcement ("S-glass" with no boron). At the first wall the dose rate for this material is 4200 Gy per MJ/m^2 integrated wall load; just behind the first wall this is reduced to 2800 $\text{Gy}/(\text{MJ/m}^2)$. At the face of the coil, behind 28 cm of shield, the dose rate is only 100 $\text{Gy}/(\text{MJ/m}^2)$. For a lifetime of 10^4 pulses the cumulative dose would be 14.6 MGy, for a safety factor of 7. The overall effective attenuation length of the shield is 8.36 cm; increments of 1.5 cm will cause about a 20% change in the dose.

Calculations for pure epoxy (no glass) show a similar pattern, but with higher values: 6900 $\text{Gy}/(\text{MJ/m}^2)$ at the wall, 4400 just behind it, and 157 at the coil (a lifetime safety factor of 4). Calculations for other epoxy formulations give similar values. At the coil face the neutron total flux is 1.40×10^{17} $(\text{n/m}^2)/(\text{MJ/m}^2)$, with 18% above 1 MeV and 41% above 0.1 MeV. The gamma energy flux is 1.70×10^{17} $(\text{MeV/m}^2)/(\text{MJ/m}^2)$.

As a consequence of attenuating the neutron and gamma flux on the coils, there is considerable nuclear heating of the primary shield. Figure 7-3 shows the heating profile from the vacuum wall to the TF coils. The total

nuclear heating per MJ of DT neutrons is calculated to be 1.31 MJ, distributed 28% in the first wall, 69.7% in the shield, and 2.3% in the coils.

In the wall (20% Ti alloy plus 80% water) the energy is deposited fairly uniformly. The average power density in the Ti is 7.75 J/cm^3 per MJ/m^2 ; in the water it is 8.33 J/cm^3 . The total heating is 0.369 MJ per m^2 of surface MJ/m^2 , but in addition to this there will be a surface heat load of 0.2 MJ/m^2 (from the fusion alpha power). This surface load is the controlling factor in the design of the wall cooling system. Note that 80% of the nuclear heat is generated directly in the water coolant.

In the iron-water shield the heating falls off exponentially from 11.9 J/cm^3 per MJ/m^2 at the front to 0.64 J/cm^3 at the back. Since the heat capacity of this shield mixture is so large, full capacity cooling during a power pulse is not required. For a nominal pulse of 22 MJ/m^2 the average adiabatic temperature rise would be only 19.5°C ; at the front edge of the shield it would be 69.7°C . A water cooling stream of $7.5 \text{ m}^3/\text{min}$ (1980 gal/min) would provide 5 replacement volumes for the water in the shield during a 10 minute cycle. This is comparable to the coolant requirement for the TF coils, but with very much less heat (.35%) to remove. A greatly reduced flow rate could be used, but only if a special small-volume network of coolant channels was built into the shield separate from the shielding water. This would undoubtedly increase the cost of the primary shield substantially.

7.1.5 Effects of Penetrations. A number of large penetrations through the primary shield are planned to accommodate the neutral beam injectors and the vacuum pumps. These provide streaming paths which could lead to radiation hot spots in the adjacent coils. The accurate analysis of this streaming requires at least 2-D and possibly 3-D transport calculations, but since the device layout is still somewhat uncertain an effort of this expense is not warranted.

Instead, the possibility of enhanced fluxes and hence radiation damage at hot spots has been estimated with some simpler models. First, the flux at the wall of a duct at the depth corresponding to the coil is estimated from uncollided and collided components; then this flux is attenuated exponentially through whatever thickness of shield is available between the duct wall and the coil conductor. The resulting total attenuation can be used to scale down the dose as calculated for the flux at the first wall.

The uncollided flux in the duct is calculated for an isotropic flux source at the opening of a rectangular duct. The collided flux is an additional load due to radiation which scatters in the shield and leaks in through the sides of the duct. Assuming the flux falls off exponentially along the duct, the integral of the flux times the area down the duct is equal to the perimeter times the attenuation length times the flux at the wall. Since most of this scatters into the duct close to the wall, this collided source term is simply used to scale up the direct uncollided term (typically by about 75%). The attenuation through the shield beside the duct is enhanced slightly by calculating the first mean free path at the slant angle of streaming from the mouth.

Figure 7-4 shows the results of this type of analysis applied to three types of penetrations: ducts for vertical neutral beam injectors; ducts for horizontal perpendicular NBI; and ducts for horizontal tangential NBI. (The latter are assumed tangent to $R = 3.35$ m and centered between two adjacent TF coils.) The curves are shown for variable duct width and a height of 75 cm (the actual NBI ducts are nominally racetrack shaped, 30×75 cm). The exponential behavior indicates the importance of the local shielding beside the duct, rather than the variation in the actual load at the hot spot.

Fig. 7-4. Attenuation of dose, relative to the value at the first wall, at the "hot spot" of a TF coil adjacent to each of the possible NBI ducts. The dashed line shows the performance of the primary shield.

Also shown in Fig. 7-4 is the level of attenuation provided by the primary shield. Values below this line represent ducts in which a "hot spot" does not actually appear provided local shielding is in place. In fact, this seems to be the case for all ducts of interest.

The bundle divertor is somewhat easier to analyze than the NBI ducts, since the magnetic topology allows little room for shielding. The two coils which divert the magnetic field must be placed very close to the vacuum wall, leaving perhaps only 10 cm for shielding. Worse than this is the fact that the diverted plasma must thread through the coils, requiring ducts which fill the full center bore. Since the coils are so close to the wall, the conductors beside these ducts will be exposed to virtually the full first wall flux with no room for shielding materials.

Some small shielding effect will be provided by the structural components between the plasma and the copper: 5 cm of vacuum wall plus a massive (about 8 cm thick) coil case. It is estimated that the dose at the conductor will be about 30% of that at the wall, i.e., $1260 \text{ Gy}/(\text{MJ}/\text{m}^2)$ for glass-reinforced epoxy. This would lead to a lifetime dose of $1.85 \times 10^8 \text{ Gy}$, about twice the assumed limit. Therefore if the bundle divertor coils are built with conventional epoxy insulation, several will have to be procured, with each one usable for perhaps a year. Alternatively, the use of an inorganic insulation with a higher radiation damage threshold could be explored.

The situation of the passive stabilizing coils is similar to that of the bundle divertor. These will be located close to the vacuum wall, with little or no room for shielding. Since the wall alone provides only a factor of 1.5 attenuation, the lifetime insulator dose in these coils could be $4.1 \times 10^8 \text{ Gy}$. This is substantially above the recommended limit and would require special consideration: either testing to qualify a special epoxy mixture for satisfactory high dose performance, or else the use of inorganic insulators.

7.1.6 Activation. After only a few full power DT pulses the inner parts of the PITR will be activated beyond the levels of casual hands-on maintenance. In fact, only a few days of steady pulsing will bring short half-lived nuclides like Mn-56 or Na-24 almost into equilibrium, and these are the most troublesome from the standpoint of quick access. Furthermore, the planned lifetime of the device is long enough to allow substantial buildup of the longer-lived nuclides (like Mn-54 or Co-60) which will interfere with major repair operations.

Actual estimates were made for activation at the end of the full design lifetime, the worst case. A scenario of 100 nominal pulses (22 MJ/m^2 in 10 seconds) spaced 10 minutes apart repeated 100 times over 4 years was assumed. The usual 1-D cylindrical transport model was used to propagate the neutrons out through the test cell, but with the TF coils represented at a very low density with attenuation equivalent to the average removal in that region (i.e., allowing for the gaps between coils). A special interface code and library was then used to calculate activation rates, total activation from the specified pulse scenario, decay during the cool-off period, and then the distribution of decay gammas. Finally, these last data were used as the source for a transport calculation of decay gammas throughout the test cell.

The resulting gamma flux is the basis for estimating biological dose rates due to the decay of activation products. Table 7-1 gives dose-equivalent rates at several locations for cooling periods of 4 hours, 1 day, and 1 week after the 10^4 pulse lifetime. Also shown are the guidelines for maximum allowable dose rates. One of the curves in Fig. 7-5 gives the dose rate profile after 1 day cool-off.

Table 7-1. Dose-equivalent rates after 10,000 PIR pulses, for several cool-off times.

<u>Location</u>	<u>4 hours</u> (mrem/hr)	<u>1 day</u> (mrem/hr)	<u>1 week</u> (mrem/hr)
Inside Vacuum Vessel	3.78 E + 7	1.75 E + 7	0.35 E + 6
Behind Primary Shield	8.44 E + 5	1.26 E + 5	0.70 E + 5
Just Inside Pillbox	3.91 E + 5	0.48 E + 5	0.31 E + 5
Just Outside Pillbox	4.84	0.39	0.093
At Test Cell Wall	0.56	0.11	0.0070
Guideline Dose Limit	200.	5.0	2.5

Clearly the vacuum vessel is untouchable. Any repairs necessary within the primary shield will have to be performed fully remotely with manipulators and television monitors. This applies also to the insides of major ducts and penetrations, and to the bundle divertor coils. Outside the primary shield (i.e., in the vicinity of the TF and PF coils and structure) the dose rates reach the level of a few tens of rem per hour after a week; this would allow remote handling with (perhaps) direct viewing through leaded glass, and might even permit short (10 minute) direct handling in extreme cases.

The dose rates in the test cell proper assume the presence of a 100 cm thick pillbox of special shielding mortar. The fact that the indicated levels are substantially below the guidelines should be noted with some caution, because: a) the treatment of the TF coils is only a rough approximation; b) additional contributions from equipment in the test cell which might become activated has not been included; and c) some allowance must be made for enhanced neutron and gamma leakage through the pillbox via penetrations for the neutral beam injectors and vacuum pumps.

Fig. 7-5. Dose-equivalent profiles from the first wall to the site boundary: a) the dose rate from activation products after 1 day cool-off following 10,000 nominal pulses; b) the direct dose per MJ/m² wall load; c) damage dose to silicon (e.g. electronic components) per MJ/m².

The transport calculation for the full test cell which gave the neutron fluxes for the activation calculation also provides the basis for an estimate of the direct dose delivered during a pulse. Figure 7-5 shows the dose profile normalized per MJ/m^2 neutron wall load; the value at the site boundary (150 m) is 43×10^{-9} rem. Scaling this by 22 MJ/m^2 (nominal pulse) times $2/3$ (average) times 2500 pulses (per year) gives 1.58 mrem; with a 50% allowance for skyshine, the estimated annual total direct dose is 2.4 mrem, half the allowed limit. Just outside the test cell the annual dose would be 92 mrem, a satisfactory level for occupational exposure.

This value justifies the 100 cm pillbox even if that thickness were not required because of activation. The use of a thinner pillbox would require thickening the test cell walls (the final biological shield). Considering the poorer attenuation and greater area of the test cell wall, the volume ratio of material added to the wall divided by material removed from the pillbox wall would be about 8. For the roof, just this tradeoff has already been made. Since the test cell roof will be at least 30 cm thicker than the walls (1.8 m versus 1.5 m) an allowance of 25 cm has been taken off the roof of the pillbox, making it only 75 cm thick.

7.2 Vacuum Vessel Maintenance

The basic arrangement of major components of the PITR will affect the ability to maintain the installation, and also affect the initial costs. The existing design as conceived is composed of eight modules, each consisting of an eighth of a vacuum vessel and exterior radiation shielding plus two paired water cooled TF magnets. Each module has coolant systems for first wall temperature protection, coolant for the shielding, and the coolant systems for the magnets. It may be advantageous to modularize a section, including its structural supports, to promote simpler detection of faults and the ability to effect repairs in a hot cell in less constricted surroundings. Figure 7-6 illustrates this concept used in the UWMAK-I (Ref.[7.1]), and also shows some of the plumbing complexities to be dealt with.

The PITR has a double-walled vacuum vessel that provides a challenge to assemble in the field, especially if the installation is "hot" and must be assembled with remote equipment. There are advantages in reducing the number of segments to four, rather than eight, (or seven if the segment containing the divertor coil presents a unique problem). The reduction in parting joints is beneficial. Additionally, space for interfacing equipment is precious because of the limited space between coils and shear structure. Floor mounted equipment like neutral beams, pumping ducts and diagnostics tend to be attached to the torus on the horizontal centerline. Pillbox floor space is covered by fluid and electrical lines and possibly vertically mounted neutral beams. Figure 7-7 shows an octant and a quarter section of the vessel. It can be seen that little space is available for equipment in a bay with a parting joint, and that the cooling paths and shielding are complex in that bay. Figure 7-8 illustrates how quarter sections have less impact on the vessel construction, provide simpler cooling and plumbing patterns, and improve reliability. The penalty for these benefits however are a larger hot cell, larger handling fixtures and larger entrance doors. Floor loading is not impacted, and devices like air cushions can effectively be used to transport heavier sections. Remote handling equipment costs should have only a minor impact because of the larger segments.

The ability to isolate and test modular sections, eliminate many assembly connections, and achieve higher reliability and reduce downtime should be weighed against the apparent advantages of smaller segments.

MODULAR SEGMENT CONCEPT

Fig. 7-6

Fig. 7-7

Fig. 7-8

7.2.1 Parting Joint Location Considerations

The major interfacing items affecting the design are:

- Neutral beams (3 configurations)
- Vacuum pumping ducts (assume 2)
- Divertor coils (2) and special segment
- Diagnostics (TBD)
- Fueling Device (TBD)

The Neutral Beams are drivers, and their number, size and functional requirements significantly influence the configuration. Three neutral beam configurations have been considered:

1. (Basic) - five beams aimed near normal to PITER centerline. (Fig. 7-9)
2. Five beams aimed vertically and located on the 5.60 M plasma diameter (mounted on pillbox roof) plus five tangential beams mounted horizontally. (Fig. 7-10)
3. Basic + five beams mounted horizontally and aimed tangent to plasma centerline. (Fig. 7-11)

Figures 7-9, 7-10, and 7-11 indicate arrangements of N.B.'s and other equipment. Detail and definition of these items is lacking, hence the space allocations are based on similar installation studies.

The basic installation (5 normal neutral beams) is the installation having minimum impact on shielding, access and igloo fluid service manifolds. This configuration permits an eight-segment construction (2 TF coils and 1/8 vacuum vessel and shielding) as proposed. Parting joint cutting and welding can be accomplished through either side or top access hatches.

Configuration 2, with the neutral beams supported on the igloo roof structure is also feasible with an eight-segment vacuum vessel assembly.

Its disadvantages are:

- Additional support structure for the roof-mounted N.B.'s and adequate margins for environmental safety.
- Poor access for installation and maintenance of neutral beams.
- Impact on fluid, electrical services and radiation shielding design and installation.

7-26

VACUUM DUCT (2)

NEUTRAL BEAMS
(PORTS CAN BE
ACCESS OPENINGS
FOR WELDER/CUTTER

5 NORMAL NEUTRAL
BEAM CONFIGURATION

Fig. 7-9

Fig. 7-10

Fig. 7-11

Configuration 3, with (10) floor mounted neutral beams aimed tangentially and normally at the plasma, presents more design difficulty. The intersections of the tangential NB ducts impact the primary radiation shielding, first wall cooling passages, and pillbox shielding. More importantly they complicate the parting joint, making it more difficult to manufacture and field assemble. These design difficulties make a four segment configuration far more attractive, and absence of the intermediate joint permits sections to be manufactured in more economical fashion. Parting joints are assumed to be assembled using welders inserted through top hatches.

The width of the parting joint limits usable hatch space, and hence also leads to the consideration of a four segment configuration. Figure 7-7 shows a quarter section of a vacuum vessel and the effect of a parting joint in one segment. Figure 7-8 shows how the NB tangential and normal beam ducts can be accommodated on a quarter section.

7.2.3 Parting Joint Design

The construction of the vacuum vessel and the wedge angle of the inboard edges of the TF coils requires that the segments be withdrawn radially. Manufacturing of the double-walled wedge segments of the vessel and the radiation shielding would become more difficult if a skewed withdrawal angle were selected. Some of the manufacturing options of the vacuum vessel segments have been explained in a previous section, as well as some of the concerns of joining the segments on final assembly.

Connection of double-walled segments is not simple. Conceptual joints usually entail mass concentrations for end plates and structural ties. Thermal gradients may occur when wall thickness varies, or coolant passages are constricted, and these gradients may induce stresses which lead to fatigue failures. Welding seems to be the only feasible method of producing vacuum and water tight seams. Mechanical seals, especially those of elastomeric materials, are difficult to assemble and would not survive the environment. There are also difficult problems in welding of contour matching, gap control, and weld shrinkage. Resistance welding requires intimate contact of metals, good back-up contact, and access to the weld electrodes.

7.2.4 Parting Joints

Subassemblies of vacuum vessel assemblies can be made using various techniques, but they universally require a local inert gas or vacuum environment to produce

sound welds of acceptable metallurgical quality. Ultimately, such joining must be done remotely due to the radioactivity of the operating vessel. Welds must be made in both walls, the inner one of which must be vacuum tight, in all positions from downhand, through vertical, to overhead. Cooling channels must be connected to provide the required coolant flow path.

While a number of welding processes may be applicable, each requires some development to join double-wall vessel sections. Some processes, like electron-beam (EB) or laser may be capable of welding both walls simultaneously from inside the vessel, but specific techniques and procedures must be developed. Shielding, by either inert gas or vacuum must be addressed in any case. Other processes, like plasma-arc or gas-tungsten arc (GTA) are particularly suited to sheet gage welding, but definitely are limited to welding one wall at a time.

The problem of welding position is one of gravity. Depending on position, gravity acts in different directions and affects both weld profile or shape and crown and roof head appearance. Pulsed-current welding processes such as pulsed-ETA or pulsed-plasma arc offer the best alternatives. For these processes, as well as EB and laser, shielding of the root head still presents a problem.

The weld crown, being accessible, is readily shielded with inert gas. Root head shielding, on the other hand, must be accomplished with either an integral gas groove (Fig. 7-12) or by welding into a backup strip (Fig. 7-13). In both cases, detailed joint designs, welding techniques, and joint efficiencies (i.e., properties) must be established.

The problem of fit-up and mismatch may be particularly difficult for large double-walled sections. The logistics of welding double-walled vessels one wall at a time presents other problems in handling, particularly when welding must be performed remotely.

Three conceptual joints using ITG welding are shown in Figs. 7-14, 7-15, and 7-16. Resistance welding is an accepted technique that can be performed in ambient air. It requires high local contact pressure between mating parts, clean contact surfaces, and adequate clearance for electrodes on both surfaces.

Common requirements of welds using inert gases and fusion of metals are:

1. Gap control: The permissible gap between meeting (butting) surfaces must not exceed .030 in. This gap must be preserved even if a filler strip is employed.

Fig. 7-12. Shielding gas groove.

Fig. 7-13. Integral backup strip.

WELDING JOINT

INERT GAS

COOLING
FLUIDS

MISMATCH
(0.03)

WELD GUIDE TRACKS

GAP TOLERANCE 0.007-0.03 IN

VACUUM

WELD:
• TIG
• ELECTRON BEAM
• LASER BEAM
REQUIRE
VACUUM
ENVIRONMENT

WELDING JOINT

7-26

INFLATABLE
BELL

DETAIL A

SEAL INFLATED TO SEAL
INERT GASSES FOR
WELDING

Fig. 7-14

Fig. 7-15

SPLICE PLATE
SEGMENT ASSY,
PITR, FIG

Fig. 7-16

2. Contour mismatch: Contours must not exceed .03" mismatch.
3. Inert gas atmosphere: Inert gas must displace atmosphere and completely surround both sides of the surfaces being welded. Vacuum can replace inert atmosphere if the unit can be confined in a chamber.

Figure 7-14 shows the simplest parting joint which requires that only a single weld be made. Gap and contour match must be controlled. The reduction of coolant in the local area may result in a hot spot. Figure 7-15 shows a parting joint in which a key section is inserted between segments. The key is slightly tapered to ease insertion. The joint preserves vacuum vessel continuity, but the problem of controlling tolerances are formidable and welding both walls simultaneously requires extensive development. Figure 7-16 illustrates a design in which walls are welded sequentially. The outer wall is first connected, then the inner wall is made with a splice strap and two welds. Installing and fitting the splice strap and weld shrinkage are the principal concerns. Figure 7-17 illustrates a type of joint that is more tolerant of mismatch problems. This joint employs flexible bellows rather than depending on close fitting of adjacent sections; gaps and contour mismatch can easily be accommodated. The principal problem is structural continuity between vacuum sections which the bellows do not provide. This can conceivably be accomplished by installing spacers, fasteners, and fittings on the outer wall. Another possibility is supporting the vacuum vessel segments independently and permitting the bellows joints to compensate for deflection as well as mismatch. This joint is more complex than a solid wall, but total program costs may be lower. The joint is far simpler to assemble than fitting segments, possibly distorted by mechanical loads or thermal cycling, by remote means.

Quality control and integrity of the vessel walls is a subject to be explored. There are conceivable means of checking water tightness of the outer wall and vacuum tightness of the inner walls, but repair of a leaking outer wall requires a sophisticated means of pin-pointing the leak, and extensive removal of the inner wall to effect outer wall repair. For this reason, joints similar to those shown in Figs. 7-14 and 7-17 should be considered for their potential of simplifying repairs.

7.2.5 Parting Joint Welder/Cutter

The welder/cutter for making parting joints will probably have to be a sophisticated manipulator type of machine with adaptive (feed - lock) controls.

CONCEPT #2 DOUBLE-WALLED WELD TECHNIQUE

1ST WELD - OUTER WALL

Fig. 7-17. 2ND WELD - INNER WALL

It will have to trace measurements on edges, trim on assembly when required, accurately control weld parameters (speed, feed, current, nozzle gap and gas feed) and possibly exert moderate forces to close gaps and match contours. Figure 7-18 shows a floor-mounted version concept of a unit; a similar version concept of a unit; a similar version could be mounted on the pillbox structure or positioned by an overhead traveling bridge. The weld head must be able to accurately follow vacuum wall contours and adjacent segment contours and compute weld parameters at the local area for parting joint assembly.

Cutting can be performed in several ways, but a sophisticated wall severing device, like a laser, rather than a mechanical cutter, will probably produce cleaner cuts and minimize preparation for new welds. It is estimated that a hatch opening of .5 x 1.0 meter will be required to insert the welder cutter. In spite of its sophisticated requirements, it is probable that a combination of commercial welders and manipulators can be assembled to weld parting joints for reasonable cost. Hand welding can be employed for initial assembly, or for hydrogen-only vessels, but remote welding must be used for radioactive machine repairs. The remote welder-cutter can also be usefully employed for initial assembly where its versatility may supplant special purpose tools and provide weld consistency.

Fig. 7-18

7.3 Tritium Considerations

Aspects of tritium control for the PIR have been considered in some detail in Ref. [1.2], which is summarized below.

7.3.1 Tritium Processing and Containment

It is anticipated that the tritium experiments to be conducted in the proposed PIR could be conducted with an on-site inventory of approximately 2.3 Megacuries. This is based on a maximum use of 20 k Ci/pulse and a maximum daily pulse rate of 50 pulses. A yearly operation involving 1,000 to 3,000 pulses would require as much as about 60 megacuries. Using conservative estimates of tritium removal efficiencies, tritium releases associated with the proposed yearly operations are estimated to be approximately 100 Ci/year. The associated dose equivalent at the exclusion boundary resulting from such releases would be less than 3 mrem/year. The largest single proposed inventory (2.3 Megacuries), if released into the Tritium Source and Getter Room (an extremely unlikely event), would result in a release to the environment of no more than 2,300 curies, resulting in an exclusion boundary dose of less than 1 rem under the worst meteorological conditions.

To minimize the potential release of tritium to the environment surrounding the PIR, the following design objectives have been established:

- (a) Containment - As much of the tritium inventory as is practicable will be contained in the Tritium Source and Handling Complex (TSHC). That amount of tritium present outside of the TSHC will be contained within three physical barriers (see Fig.7-19).
- (b) ALAP - Routine tritium releases will be reduced to levels that are "as low as practicable" (ALAP). In all cases, releases will be less than the appropriate DOE guidelines.

Fig. 7-19

Systems design techniques will emphasize the prevention of tritium releases to large volumes in order that clean-up systems may operate on the high tritium concentrations for which they are most effective. All tritium containing components will be designed to high standards of reliability in order to minimize the probability of accidental releases.

In addition, administrative procedures will be specified that will require controlled recycling techniques (vacuum vessel bakeout, neutral beam cryopanel regeneration, discharge cleaning, gettering, etc.) in order to minimize the accumulation outside the Tritium Source Complex. Clean-up systems located in the TSHC will minimize releases from the various systems located there.

The proposed Tritium Source and Handling Complex will consist of the following spaces:

- 1) Tritium Source and Getter Room
- 2) Tritium Recycle Room
- 3) Gas Holding Tank Room
- 4) Clean-up Systems Room
- 5) Waste Handling Room
- 6) Tritium Control Room
- 7) Uninterruptible Power Supply Room

The entire Tritium Source and Handling Complex and the Test Cell will be designed to structure withstand the most intense natural phenomena defined for the TFTR site.

The proposed Tritium Control Room will be located outside the main tritium seal boundary of the Tritium Source and Handling Complex to assure accessibility under all conditions.

7.3.2 Tritium Systems - Overview

The PITER Tritium Systems will be based upon a design philosophy of providing redundant and/or interchangeable systems with multiple levels of containment. All high tritium concentration activities will be confined to the Tritium Source and Getter Room, the Tritium Recycle Room and the Gas Holding Tank Room located within the Tritium Source and Handling Complex.

Delivery of tritium gas to the Torus will be accomplished by a dedicated Tritium Storage and Delivery System (TSDS) housed within an inert atmosphere glove box located in the Tritium Source and Getter Room. Component redundancy within the TSDS will be provided and the inert atmosphere storage and delivery glove box will be serviced by a clean-up system.

Two identical all-purpose systems, which will provide total system redundancy for each other, are proposed to provide clean-up for the Torus/Neutral Beam Lines - the Torus Clean-up System (TCS) - and for the containment between the double walls of the Torus - Annulus Clean-up System (ACS). A third system provides clean-up for the Tertiary Containment located within the Secondary Shield - the Tertiary Containment Clean-up System (TCCS).

Emergency clean-up capability will be provided for the Tritium Source and Getter Room, the Gas Holding Tank Room, the Tritium Recycle Room and Tritium Clean-up Systems Room. The Test Cell will have a dedicated Clean-up System.

In all cases, tritium will be contained by multiple levels of containment with the containment effluent processed by redundant or back up clean-up systems. The holdup of tritium in the proposed titanium alloy vacuum vessel has been found acceptable after a preliminary analysis, as discussed in Section 6. Further study will be required to confirm this.

Tritium recycle capability is proposed in order to reclaim the Torus/Neutral Beam waste gases and as much of the inventory as practicable. On-site recycle

will minimize the shipment of source tritium to and packaged tritium waste from the PITR site. Demonstration of the safe and routine functioning of the D-T fuel cycle at near prototypical conditions will also be realized.

Figure 7-20 is a schematic representation of the proposed tritium cycle for PITR.

7.4 References

- [7.1] "Developing Maintainability for Tokamak Fusion Reactors", Interim Timeline Analyses, McDonnell-Douglas Astronautics Company-East, June 1977.
- [7.2] Lind, K., Howe, H., Yemin, L., Rosmassler, R., and Pierce, C., "Tritium Control and Processing Systems for Large Tritium Inventories at the TFTR Site", PPPL-TN-309.

NOTE: RECYCLE LINES FOR CLEAN-UP SYSTEMS NOT SHOWN

ACS ANNULUS CLEAN-UP
 BECS BUILDING EMERGENCY CLEAN-UP SYSTEM
 GCS GLOVEBOX CLEAN-UP SYSTEM
 RCS RASH CLEAN-UP SYSTEM
 TCCS TERTIARY CONTAINMENT CLEAN-UP SYSTEM
 TCS TORUS CLEAN-UP SYSTEM
 TRS TRITIUM RECYCLE SYSTEM
 TDS TRITIUM STORAGE AND DELIVERY SYSTEM

a TORUS ANNULUS
 b INSERTED TERTIARY CONTAINMENT
 c TEST CELL / PASSENGER
 CLEAN-UP ROOMS
 e GAS HOLDING TANK ROOM
 f TRITIUM SOURCE AND GUTTER ROOM
 e TRITIUM RECYCLE ROOM

CONCEPTUAL FLOW DIAGRAM - TRITIUM SYSTEMS FOR THE PROPOSED PITA
 FIGURE 7-20.

8.0 USE OF PPPL FACILITIES

8.1 PITR Test Cell

Figures 8-1 and 8-2 depict the layout of the ITR Test Cell in plan and elevation, respectively. The concrete roof and wall thicknesses of 1.8 m and 1.5m are dictated by the structural requirements for a clear span. Together with the pillbox concept, these thicknesses of concrete will provide adequate shielding against the anticipated neutron yield.

Apart from providing a radiation shield, the pillbox will also serve as a support structure. Sufficient structural integrity to support up to seven vertical neutral beam injectors, each with a weight of approximately 73 Tonnes, will be built into a pillbox framework consisting of 16 main beams and 32 columns. The estimated weight of this pillbox, including shielding and structural members, is approximately 1273 Tonnes. The pillbox however, does not completely shield the device. It may be convenient to use the same shielding composition as that used for the pillbox for the additional local shielding that will be required to protect against neutrons back-streaming through neutral beam lines.

The inside dimensions of the Test Cell (46m x 55m) will accommodate the proposed device and the various schemes for horizontal neutral beam injection. Furthermore, there is enough space for placement of the various neutral beam arc, grid, and filament supplies. This will be necessary because the Test Cell basement must be devoted entirely to diagnostics and tritium systems. The inside height of the

View North through East/West centerline

Figure 8-2

Test Cell (20 m) is the minimum which will accommodate the device, its shielding pillbox, a 180 Tonne bridge crane with a 12 foot vertical span, vertical neutral beam injectors, plus clearance for services and remote handling manipulators.

The Test Cell basement occupies the space beneath the 2 m thick concrete Test Cell floor (it is 3 m thick directly below the device to provide enhanced shielding). The basement's height (10 m) is determined by the height of the distillation columns for the tritium recovery system. Due to the large tritium inventory anticipated for Pitr operations and related tritium supply and recovery systems, major areas of the Test Cell basement will be devoted to their use. Therefore, the Test Cell basement must be designed to Most Intense Earthquake criteria. The remaining basement space will be adequate to contain diagnostic systems and device services only.

8.2 Site Modifications

The Pitr Test Cell is located west of the TFTR test cell and adjacent to the utility corridor. To reduce the cost of Pitr, extensive use will be made of TFTR equipment and supporting facilities. The general site layout (Figure B-3) indicates a method for achieving this goal. Since this approach mandates shutdown of TFTR operations prior to initial Pitr operations, supporting utilities, such as cooling water and power, will be installed to the point of final connection to TFTR sources during TFTR's operative lifetime. Final tie-in can be expected to take about six months following TFTR's shutdown.

Figure 8-3

Basement connection of electrical busways and connections for cooling water are simplified by the use of the utility corridor that runs adjacent to the west wall of the TFTR Test Cell basement. This corridor connects the nexus of the power conversion buildings to the utility tunnel from the central control room. Field Coil Power, Neutral Beams Power, Cooling Tower Water, Chilled Water, Cryogenics, and Controls will all be taken from the existing facilities with modifications as described below. The HVAC for the new facilities will require some additional utility services due to the sophisticated tritium ventilation system.

Mechanical Equipment Basement Area

HVAC for the Test Cell must be enclosed in a negative pressure environment, and, since it is an integral part of the tritium containment and clean-up systems, it will be housed in a basement area adjacent to the PITR Test Cell basement. Closed circuits within the containment area must be used for the cooling water systems, which will be radioactive. The associated heat exchangers will also be housed in this new basement space.

Basement space in the Test Cell area has been chosen for most economical design to meet Most Intense Earthquake criteria, and also to take advantage of earth covering as a radiological shield, should additional shielding become necessary.

Connection of services to the PITR Test Cell and interface with existing facilities will be made through the basement to eliminate above grade Test Cell wall penetrations. Figure 8-3 shows a flexible arrangement for linking new with existing facilities. In order to

bring existing services into the Test Cell basement, the north-south wing of the Mechanical Equipment Basement will have a split level. This arrangement will allow access to the Test Cell basement and prevent undermining existing TFTR foundations during construction.

Additional Field Coil Power

The TFTR facilities will not be able to supply sufficient Field Coil power conversion equipment to operate PITR. A new east-wing on the Field Coil Power Conversion Building (See Figure 8-3) will house the necessary additional equipment.

8.3 Cooling Systems

8.3.1 Water Cooling System

The heat load per pulse of the Pitr machine is more than double that of TFTR. The bulk of this increase is due to the field coils which dissipate 8.2×10^6 Btu's per pulse as compared to 3.9×10^6 Btu's for TFTR (refer to Table 8-1). In addition, 2.7×10^6 Btu's must be removed from the vacuum vessel for each ignition pulse. Average heat load, however, is less than double since the time between pulses for the Pitr machine is also twice that of TFTR. The field coils use chilled (50°F) water coming from 4 chillers and a 45,000 gallon storage tank in the TFTR configuration. Two chillers must be added, costing \$300 K, to meet the Pitr requirement. Storage capacity is adequate. To meet the higher flow rate requirement two pumps must be added at a cost of \$200 K, and piping and valves costing \$100 K.

It is currently estimated (see above) that a total of 93 double Power Conversion units will be needed to supply the Pitr field coils, whereas TFTR uses 33. This means adding a new cooling system consisting of a pump, a heat exchanger, and piping and valves, at a cost of \$250 .

Pitr will use 5 neutral beam lines to deliver 60 MW of D⁰ to the plasma; TFTR uses 4 lines to deliver 20 MW. The resulting additional flow rate means adding 3 pumps, 3 heat exchangers and plumbing at a cost of \$200 K. Provisions already exist in TFTR to supply cooling water to the NB power supplies for the 5th beam line. Added pumps are part of the power supplies and are included in the NB costs.

Table 8-1

WATER COOLING SYSTEM
(SHEET 1 OF 3)

PARAMETER	TFTR CAPAB	PITR-3 REQUIREMENT	A EQUIPMENT NEEDED	A COST
<u>FIELDS COILS</u>				
AVG. HEAT LOAD (Btu/hr)	57×10^6	50×10^6	none	
- EQUIV. SQ. WAVE (SEC)	- 2	- 12	none	
TIME BETWEEN PULSES	300 SEC	600 SEC	none	
THERM ENERGY/PULSE (Btu)	3.9×10^6	8.2×10^6	none	
FLOW RATE (GPM)	3300	2585	none	
CHILLER CAPACITY (ton)	4400	4200	none	
SUPPLY TEMP/PRESS. (DEG,F/PSIG)	50/270	50/270	none	
<u>POWER CONVERTORS</u>				
PEAK HEAT LOAD (MW)	10	28		
- EQUIV. SQ WAVE (SEC)	10	10		
TIME BETWEEN PULSES (SEC)	300	600		
THERM ENERGY/PULSE (Btu)	9.48×10^5	26.6×10^5		
FLOW RATE REQ'D (GPM)	1500	3760	ADD NEW SYSTEM: 2 PUMPS, HEAT EXCHANGER, PIPING, VALVES.	\$250 K
COOLER CAPACITY REQ'D	NEGL	NEGL		
SUPPLY TEMP/PRESS (°F/PSIG)	77/175	77/175		

Table 8-1

WATER COOLING SYSTEM
(SHEET 2 OF 3)

PARAMETER	TFTR CAPAB	PITR-3 REQUIREMENT	Δ EQUIPMENT NEEDED	Δ COST
<u>NB LINES</u>				
PEAK HEAT LOAD (MW)	24	30	ADD 3 PUMPS, 3 HEAT EXCHANGERS, PIPING	\$200 K
- EQ SQ WAVE (SEC)	0.5	0.5		
TIME BTWN PULSES (SEC)	300	600		
THERM. ENERGY/PULSE (Btu)	1.14×10^4	1.43×10^4		
FLOW RATE REQ'D (GPM)	4400	5500		
COOLER CAPACITY (ton)	negl.	negl.		
SUPPLY TEMP/PRESS (°F,PSIG)	<95/70,150	<95/70,150		
<u>NB POWER SUPPLIES</u>				
PEAK HEAT LOAD (MW)	28	35	ADDED PUMPS ARE PART OF NB SYSTEM.	8-10
- EQ SQ WAVE (SEC)	1.0	1.0		
TIME BTWN PULSES (SEC)	300	600		
THERM ENERGY/PULSES (Btu)	6.8×10^4	8.5×10^4		
FLOW RATE REQ'D (GPM)	4200	5250		
COOLER CAPACITY (ton)	negl.	negl.		
SUPPLY TEMP/PRESS (°F,Psig)	95/100	95/100		
<u>VACUUM VESSEL</u>				
PEAK HEAT LOAD (MW)	N/A	286	ADD 2 CHILLERS	\$300 K
- EQ SQ WAVE (SEC)	N/A	10	AT 1150 TON EACH	

Table 8-1

WATER COOLING SYSTEM
(SHEET 3 OF 3)

PARAMETER	TFTR CAPAB	P1TR-3 REQUIREMENT	Δ EQUIPMENT NEEDED	Δ COST
<u>VACUUM VESSEL</u> (cont'd)				
TIME BTWN PULSES (SEC)	N/A	600	ADD 2 PUMPS AT 15 GPM EACH (115 FT HEAD)	\$200 K
THERM ENERGY/PULSE (Btu)	N/A	2.7×10^6	ADD PUMP & VALVES	\$100 K
FLOW RATE REQ'D (GPM)	N/A	30,000	+ 2 TANKS @ 10 K GAL. EACH	
COOLER CAPACITY (MW)				
SUPPLY TEMP/PRESS (°F/Psig)	N/A	50/50		

8.3.2 Cryogenic System

- Helium (see Table 8-2)

The TFTR helium refrigerator has a capacity of 1070 watts at 3.8°K (to satisfy H₂ pumping) and 4.5°K (for deuterium pumping). Approximately 250 watts (based on seven neutral beams plus target) is estimated for heat leak into facility piping, valves, etc., leaving about 820 watts for other heat loads. Original estimates showed 63 watts required for each neutral beam and test cell, and 12.6 watts for the neutral beam target chamber. Currently, the neutral beam helium refrigerator load is estimated at about 40 watts, of which about 80-90% is thermal load from the nitrogen chevrons. The balance can be accounted for in heat load to the neutral beam dewar and manifolds and a small amount of helium superheat at the return line interface. Estimating the thermal load at about 34 watts, the balance is about 6 watts. The refrigerator load, exclusive of thermal load on the helium panels, is about 295 watts (feed lines, dewars, target), leaving 775 watts for neutral beam thermal loads.

Neutral beam thermal load is proportional to the cryopanel's surface area, which are sized to maintain base pressure during neutral beam injection. Any increase in gas load, i.e., pumping surface throughout, without a corresponding increase in base pressure would dictate larger cryopanel surfaces, and consequently an increase in refrigeration. Current estimate for P1TR is that the gas load is doubled; however, the tolerable base pressure is also doubled (TFTR base pressure requirements are conservative). Therefore, TFTR cryopanel sizes are appropriate (pumping speeds are about equal) for P1TR estimating, and TFTR refrigeration requirements are adequate. Using the TFTR estimated cryopanel load of 34 watts per beam line, the P1TR load for five beamlines is 170 watts (34 x 5), which allows about 355% growth (775 watts compared to 170 watts).

In summary, then the P1TR helium refrigeration requirement is:

Neutral beam line (5 x 40)	200 watts
Lines, valves, etc.	250 watts
	<hr/> 450 watts
10% Contingency	<hr/> 45
	495 watts

TABLE 6-2

CRYOGENIC SYSTEM (NBIS)

<u>PARAMETER</u>		<u>TFTR CAPABILITY</u>		<u>PITR-3 REQUIREMENT</u>
He FLOW RATE	(l/hr)	576	767	7
He TEMP	°K	3.8(H ₂)	4.2(D ₂)	4.5(D ₂)
CRYO PUMP SEED Per BEAM	1/s	3.5x10 ⁶	2.5x10 ⁶	5x10 ⁶
He STORAGE CAP.	(l)	5678	5678	5678
He REFRIG. CAP.	(W)	1070	1070	450
He STORAGE	(l)	226,560	226,560	226,560
BASE PRESSURE (OPER)	(torr)	10 ⁻⁵	10 ⁻⁵	2x10 ⁻⁵
N ₂ FLOW RATE	(l/hr)	3118	3118	2618
N ₂ STORAGE CAP.	(l)	41,635	41,635	41,635
N ₂ STORAGE PRES.	(PSIG)	20-25	20-25	20-25

• Nitrogen

The TFTR liquid nitrogen system is "open loop", that is the LN_2 is trucked in and the expended nitrogen is vented to atmosphere. The TFTR nitrogen capacity and delivery rate is adequate for Pitr because the maximum demand (neutral beam cryopanel chevrons) is comparable. It is expected that any increased demand because of increased cryopanel size (if needed) would be accommodated by more frequent deliveries with no additional capital investment.

The TFTR nitrogen system is based on a 140kW demand at 77°K (3118 liter/hr). The neutral beam line thermal load is about 200 liter/hour per beam line, and the TFTR nitrogen has capability to supply seven beam lines plus one test cell (equivalent to eight beam lines total). The nitrogen supply budgeted for beam lines on TFTR then is 1600 liter/hr. (72kW). The Pitr demand for five beam lines is assumed to be 1100 liter/hour (including 10% contingency), so that the total Pitr demand is estimated at 2618 liter/hour (118kW).

8.4 Vacuum System

The TFTR Torus Vacuum Pumping System (TVPS) should be adequate for the Pitr without significant upgrading (other than new ducting) for the following reasons:

- 1.) Although the pumped volume is higher for Pitr (see Table 8-3) the pumping efficiency can be markedly improved by reducing ducting losses. Ducting losses are high on TFTR because of access restrictions and the requirement for hands-on maintenance of the Turbo-Molecular Pumps (TMP's). The latter restrictions means no line of sight paths from the plasma to the pumps. On Pitr, access should be better and a higher level of remote maintenance is planned.
- 2.) Vacuum Vessel base pressure is limited by outgassing surface area. In TFTR a large percentage of this surface area is due to protective plates, which must be large since the Neutral Beams are being swiveled through 15 deg. arcs. In Pitr no swiveling will be done, so the protective plates can be very localized. As a result total outgassing area is actually

TABLE 8-3

VACUUM SYSTEMS

Parameter	TFTR Capability	PITR-3 Requirement	Equipment Needed	Cost
<u>TVPS</u>				
Pumped Volume (i)	101,000 (Incl. Ducts)	250,000 (Incl. Ducts)	New Ducting and Manifolding Only	\$1.0 M
Pumped Volume (Mat'l)	760 ss	230 Ti		
Surface Area (m ²)	37 Gv	300 ss		
Pump Down Time (hr)	1	1-2		
Pumping Capacity (i/s)	25,200 at TMP 4,500 at vv	5,600 at vv		
Base Pressure (torr)	9×10^{-9} (After Bakeout)	9×10^{-9}		
Recovery Time (sec)	250 H ₂ 120 T ₂	500	Possible Backing Pump	\$55 K
Tritium Charge (C _i) Per Pulse	400	20,000	See Tritium Section	
<u>NBEVS</u>				
Pumped Volume (l)	208,000 (4 Beams)	312,000 (5 Beams)	One TMP + Assoc. Equip.	\$75 K
Pumped Volume (Mat'l)	400 ss	~480 ss		
Surface Area (m ²)	370 Cu 260 Mylar	~370 Cu ~260 Mylar		
Pump Down Time (hr)	1.5 (2 Rough Pumps)	2-3		
Pumping Capacity (l/s)	12,600 4 TMP's w/screen Losses only 1100 at Beam	1100 at Beam		
Base Pressure, Non-Oper (torr)	$< 1 \times 10^{-4}$	$< 1 \times 10^{-4}$		
Recovery Time (s)	N/A	N/A		

smaller than TFTR. Outgassing properties of titanium, however, are not really known at this time. If comparable to steel, a base pressure of 10^{-8} torr should be attainable with the TFTR system. 3.) Recovery time between discharges is directly proportional to pumped volume and inversely proportional to pumping speed. Pitr has 2.5 times the volume of TFTR but should achieve at least twice the pumping speed due to improved ducting as mentioned above. This should result in a recovery time well within 500 sec, which gives a good margin relative to the 600 sec between pulses. Even if pumping speed is as low as in TFTR, recovery time can be reduced by adding a low cost backing pump to the system.

The Neutral Beam External Vacuum System (NBEVS) requires only one added TMP and associated equipment for the fifth Neutral Beam to be used on Pitr (TFTR uses four). Although the Pitr beams can be up to 20% larger in volume the outgassing surface area will be about the same, and the base pressure requirement is the same so the pumping capacity of the TFTR TMP's is adequate. Cost for the added equipment is approximately \$1.1M. The two TFTR roughing pumps, shared with the TVPS, are adequate as long as pump-down time is allowed to increase to 2 - 3 hours.

8.5 ELECTRICAL POWER SYSTEM

The primary electrical source for TFTR is two motor-generator (MG) sets, fed by a 50 MVA/138 kV line, which provide 950 MVA peak power and a total of 4.5 GJ of energy. Sixty six convertor units (33 double units) supply rectified DC power to the TFTR TF and PF coils.

Table 8-4 shows a comparison of Pitr requirements, and the added equipment and costs needed to upgrade the TFTR Power System for Pitr.

The addition of a 1000 MVA line is needed to meet the added power requirements and has been discussed with the New Jersey PSE & G.

It is currently estimated that a total of 121 (single) Power Convertor units must be added to supply all field coils, at a cost of \$24.2 M. The number of PC's needed for the TF and Bundle Divertor coils is a reasonably accurate estimate. The number needed for the PF coils, however, is based on the Pitr-1 configuration and is subject to some change. Figures 8-4 and 8-5 show the coil configuration

Table 8-4

ELECTRICAL POWER SYSTEM
(SHEET 1 OF 3)

PARAMETER	TFTR CAPAB	PITR-3 REQUIREMENT		Δ EQUIPMENT NEEDED	Δ COST
<u>EDS</u>					
MAX COINCID PWR (MW)	920	2700*		NEW 1000 MVA UTILITY INTERFACE, TRANSFORMER, ETC.	\$25 M
MAX ENERGY (MJ)	4500	13000			
<u>TF PECS</u>					
PEAK CURRENT (KA)	73.2	177.6		TFTR CONVERTORS = 36	\$12.8 M
NL DRIVING VOLTAGE (KV)	8	5	10	PITR CONVERTORS* = 100	
PEAK POWER RATING (MW):	470	710	1420	Δ CONVERTORS = 64	
I ² Rt LOSSES (MJ)				* FOR 10 KV DR. VOLT.	
- DURING RISE	1939	2092	689	NOTE: THESE ARE SINGLE CONVERTORS TWO OF WHICH MATE WITH ONE TRANSFORMER	
- " FLAT TOP	585	4060	4060		
- " FALL	391	609	352		
- TOTAL	2915	6761	5101		
STORED ENERGY/PULSE (MJ)	1568	2470	2470		
RECOVERABLE ENERGY/PULSE (MJ)	977	1860	2120		
<u>PF PECS</u>					
LOH: (COIL #2)					
PEAK CURRENT (KA)	24	100		TFTR CONVERTORS = 18	\$6.4 M
DRIVING VOLTAGE (KV)	18	8		PITR " = 50	
PEAK POWER (MW)	350	844		Δ CONVERTORS = 32	

Table 8-4

ELECTRICAL POWER SYSTEM
(SHEET 2 OF 3)

PARAMETER	TFTR CAPAB	PITR-3 REQUIREMENT	Δ EQUIPMENT NEEDED	Δ COST
I^2R_t LOSSES (MJ)				
- RISE	70	1560		
- FLAT TOP	64	-		
- FALL	<u>25</u>	<u>160</u>		
- TOTAL	159	1720		
STORED ENERGY (MJ)	54	1600		
RECOVERABLE ENERGY (MJ)	29	314		
EF:				
PEAK POWER (MW)	100	392	TFTR CONVERTORS = 12 <u>PITR CONVERTORS = 31</u> Δ CONVERTORS = 19	8-18 \$3.8 M
I^2R_t LOSSES (MJ)	66	1479		
STORED ENERGY (MJ)	48	495		
RECOVERABLE ENERGY (MJ)	32	120		
<u>BUNDLE DIVERTOR</u>				
PEAK CURRENT (KA)	N/A	47		
NL DRIVING VOLTAGE (KV)		2		
PEAK POWER RATING		75.2	6 CONVERTORS	\$1.2 M
I^2R_t LOSSES/PULSES (MJ)				
- RISE		58.4		
- FLAT-TOP (5 SEC)		271.2		

Table 8-4

ELECTRICAL POWER SYSTEM
(SHEET 3 OF 3)

PARAMETER	TFTR CAPAB	PITR-3 REQUIREMENT	Δ EQUIPMENT NEEDED	Δ COST
- FALL		13.9		
- TOTAL		343.5		
STORED ENERGY (MJ)		51.3		
RECOVERABLE ENERGY (MJ)	N/A	37.4		
<u>NEUTRAL BEAMS</u>				
PEAK POWER (MW)	67	133	COSTED AS PART OF NB SYSTEM	
TOTAL ENERGY/PULSE (MJ)	34	67

Figure 8-4

Figure 8-5

and the compression timeline used to establish the coil current and power requirements. In the PITER-3 design the PF coils must move radially outward to accommodate the oversized TF coils and Bundle Divertor coils. This will tend to increase the PF power requirements even further. It is clear that the PF coils need some cost optimizing.

Another limitation that requires further study is illustrated in Fig. 8-6. To stay within the thermal limits of the TF coils and a flat-top near the nominal 10 sec, it is necessary to use a forcing voltage of at least 10kV. With a 10kV forcing voltage for the TF coils and 8 kV for the PF coils, the power capacity of the 2 MG sets plus the 1000 MVA line is exceeded.

To resolve this problem several options need to be evaluated:

1. Reduce PF power requirements by more efficient placement and/or current timelines;
2. Improve TF coil cooling;
3. Add two more MG sets to meet all power requirements;
4. Reduce flat-top time.

These options are listed in order of most to least desirable.

Power requirements for the neutral beams do not add significantly to the field coil requirements. NB power supplies are considered part of the NB systems and therefore are costed as such. Other auxiliary systems such as pumps and refrigerators draw negligible power as compared with the field coils.

8.6 Installation of 500 KV Substation

The installation of a pulsed power tie line to the utility grid instead of motor generator flywheel units is being pursued for PITER because of the long pulses, and hence large energy per pulse. Trade off studies are required for the various energy system factors: TF ramp and flat top times, PF requirements, MLS capacitor, and direct intertie costs.

8-23

LOH/TF FORCING VOLTAGE (KV)

FIELD COIL TRADE-OFFS

Figure 8-6

Public Service Electric and Gas Company has conducted a preliminary review of providing a tap line from their 500 kV main bus for the use as a tie to the utility groups in the area. The PSE&G engineers have agreed that 1000 MVA pulse at unity power factor is obtainable from their system. The voltage dip in their substation will not exceed 1% which is believed to be acceptable. The utility engineers have no experience with systems having large dips at intervals of many minutes. They do not know the maximum voltage fluctuation which can be tolerated by their customers for this type of pulse interval.

The harmonics produced on the system, due to the phase controlled rectifiers and the stability of the system under the pulse loading, have been reviewed. There is no major concern in either case. The amplitude and frequency of the harmonics expected under the worst case operation have been computed and provided to the utility engineers. They do not believe there will be any adverse effects, but if problems do occur, relatively simple harmonic filters can be installed. The system stability is no problem if the utility has its minimum desirable spinning reserve in operation. When they reach peak loads, or have equipment outages, our pulsing could cause the system to become unstable; hence, we would be limited in magnitude or frequency of pulse during critical periods.

We have requested PSE&G to consider establishing a special installation and charge rate, because of the strange nature of the proposed load; large pulses, long intervals at no load, ability to feed back the PITR stored coil energy into the system, restricted use during critical periods, and the lack of predictability for future use. In addition, PSE&G - at present - has no customers on their 500 kV line and no customers with two services (138kV and 500kV) as PPPL would have. If the utility company agrees to a new charge rate, it will have to be approved by the Public Utility Commission.

The installation of a 500kV tap will be a long lead item, not only in obtaining a new billing rate agreement, but because their standard estimate for installation time is seven years. If the installation is completed by way of an overhead line, this estimated installation

time is not likely to be substantially reduced. To reduce this time, an alternate approach of providing the service by way of an underground 500 kV line is being pursued. There are two concerns with the underground approach. One is the increased cost and the second is the unknown reliability with 500 kV cable. (The estimated repair time for a faulted cable is about three [3] weeks.)

Preliminary engineering is progressing on the 500 kV tie and substation. Figure 8-7 shows a schematic of the system from the PSE&G 500 kV main bus at Deans through to the 13.8 kV Forrestal bus. The layout of the 500 kV substation is shown in Fig. 8-8. The diagram of "C"-Site in Fig. 8-9 indicates a suitable location for this substation and for the main power conversion equipment building.

Figure 8-7. Schematic of the top line for Pitr to the PSE & G main bus.

Figure 8-8. Layout of the 500 kV Substation.

Figure 8-9. Site Plan With New 500 kV Substation.

8.7 TFTR Neutral-Beam-System Conversion

Four TFTR neutral beam units are designed to deliver to the plasma a total of 20 MW of 120 keV D^0 and another 15MW at 80 keV and 40 keV [8.1]. Increasing the ion source D^+ fraction from an assumed 65% to 90% would increase the full energy power to 28 MW, and drastically reduce the fractional energy power.

The modification of these units to accommodate direct recovery [8.2] and negative ions [8.3] has been considered. For the purposes of this PITER study, it is concluded from these modification considerations that the basic boxes, cryopumps, electrical power systems and controls of the TFTR system can be used for PITER after relatively minor modifications. Because of the larger acceptance angle of PITER, larger ion sources and therefore more gross ion current per beamline was allowed, but limited to twice the TFTR design values by physical size considerations and by the increase in reionization losses occurring because the cryopump size is fixed. The cost savings incurred by making use of the TFTR systems is about \$15 M. Modifications as well as additions to the TFTR system are included in the costs.

A schematic of the basic PITER unit is shown in Fig. 8-10. The TFTR high voltage power supply will provide 840 A at 150 keV, so the PITER costing includes additional power supplies when the 840 A is exceeded. The TFTR supplies are reversible so that D^+ or D^- sources can be accommodated. The specification for the supplies is 5 sec at a 3% duty factor, although they are judged capable of handling longer pulses, with minor cooling modifications, if the 3% duty factor is not exceeded.

The direct recovery power supply, if used, must be purchased. It is judged that this would be 16 kV supply, capable of isolation from ground to 150 kV, for the 160 keV system, and would be a 10 kV supply, capable of isolation from ground to 80 kV, for the 90 keV system.

Figure 8-10. SCHEMATIC DIAGRAM OF MODIFIED TFTR INJECTOR.

The TFTR switch tubes and circuits can be used until their capability is exceeded. Their pulse length capability will probably be 30 seconds with at least a 3% duty factor.

The TFTR ion source supplies are judged to be oversized by about a factor of two. Because of this, no additional ion source supplies would be required for most cases studied. The TFTR control system will require minor modification.

All ion sources would be replaced. For the positive-ion based systems there would be 12, 150-A D^+ sources. For the 160-keV negative ion based system 12, 75-A D^- sources are required.

For the systems with no direct recovery, larger bending magnets and *ion dumps* must be purchased. *An alternative to magnets and dumps, a primitive direct recovery unit which would not recover appreciable amounts of power but would ease power handling requirements, might be feasible.*

The TFTR cryopumps would be sufficient except for the impractical 90-keV D^- case.

It will be necessary to replace the tops and possibly put extensions on the ion source ends of the large TFTR beamline boxes. The changes are needed to allow for room, mounting, and access for the new bending magnets and beam dumps, or the direct recovery system, and/or the negative-ion generation and acceleration units.

Although the TFTR absolute valves and fast valves appear to be marginally acceptable, the uncertainty in the direct recovery and/or the negative-ion system layout and the loss in design flexibility brought on if one is forced to focus the beam through the valves, suggests that new larger valves will have to be purchased.

Larger calorimeters will need to be purchased. High beam power densities will restrict the beam pulse length to 0.5-s when using the calorimeter.

8.8 References

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- [8.2] Hamilton, G. W., University of California Radiation Laboratory
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9.0 COSTING AND PROGRAM PLAN

A high Ignition Margin with respect to confinement times appears to be cost effective for the PITER device. Although only a limited number of point designs have been examined within the PITER class (i.e., low aspect ratio/small major radius), the trend in the cost analyses indicates that the higher performance device analyzed in this report, here called PITER-3, is cost effective.

Budget limitations may, of course, be an overriding factor. In fact, the real decision on machine size must be made in the context of the relationship of that machine to EPR (or whatever the following machine will be) rather than as an isolated experiment. To illustrate this point Appendix C develops a simplified comparison between five program scenarios leading to an EPR. The comparison shows that a smaller machine such as PITER-2 (c.f. appendix C), may be just as valuable as PITER-3 as a precursor to EPR when considering total expected cost including EPR. This is not necessarily the deciding criterion, but only serves to illustrate the need for planning a long term fusion program.

9.1 Cost Savings at PPPL

The reason why large cost savings are obtained by building the next device at the TFTR site is that the cost of the Tokamak device itself represents only about 25% of total program costs. The supporting systems and facilities, representing the other 75%, are to a great extent adaptable from the TFTR project, as shown for PITER-3 in Section 8. Although it is true that certain supporting equipment such as the Neutral Beams might be transferable to another site, experience with such equipment cannot be easily transferred, and the impact of a handover between programs at different sites would be difficult to minimize.

Table 9-1 gives a summary of estimated cost savings obtainable by using TFTR equipment and facilities. The \$110 M total includes use of the four TFTR Neutral Beams, two MG sets and associated power substation and distribution equipment power conversion systems, and several utility tunnels. Nearly all of the cooling and vacuum systems can be used in addition to major portions of the diagnostic and CICAIDA systems. About half of the Remote Handling components are also felt to be usable on the PITER program.

TABLE 9-1
COST SAVINGS DUE TO COMMON USAGE ITEMS
(1977 DOLLARS)

<u>ITEM</u>	<u>COST SAVING</u>
NEUTRAL BEAM SYSTEMS (4)	\$ 31 M
POWER SUPPLY & CONVERSION EQUIPMENT	\$ 25 M
BUILDINGS & UTILITIES	\$ 18 M
LIQUID He & N COOLING SYSTEMS	\$ 2.4 M
WATER COOLING SYSTEM	\$ 3.0 M
TORUS VACUUM PUMPING	\$ 1.9 M
REMOTE HANDLING	\$ 1.5 M
DIAGNOSTICS & CICADA	\$ 5.0 M
TRITIUM SYSTEMS	0
	<hr/>
SUBTOTAL	\$ 88 M
PLUS 25% EDIA	\$ 22 M
	<hr/>
TOTAL	<u>\$110 M</u>

9.2 Cost Estimate

Total program cost for the PITER-3 concept is estimated in Table 9-2. This table is based on either scaling to TFTR (latest Dec '77 estimate) or a new estimate, depending on the system element. For example, field coil costs were scaled to TFTR as the ratio of copper weight, whereas the 1000 MVA power line was a new estimate provided by the New Jersey PSE&G Company. Neutral Beam cost is derived from a new cost model described in Appendix B.

The PITER configuration that was costed is described in Sections 2 to 5. The baseline Neutral Beam configuration was used; five horizontal, near normal beams producing 60 MW of 160 keV (D^0). The Vacuum Vessel is thin double-walled titanium, and a single set of Bundle Divertor coils is assumed.

The Vacuum Vessel estimate of \$25 M and the Bundle Divertor estimate of \$9 M are subject to the most uncertainty. Both of these items would involve additional R & D costs since they are beyond current technology, as discussed in Sections 4 and 6.

A cost spread by fiscal year is given in Table 9-3. Escalation at 6% per year results in total hardware cost of \$268 M. With EDIA and A & E the total becomes \$338 M.

TABLE 9-2
 TOTAL PITR - 3 PROGRAM COST
 (1977 DOLLAR)

<u>ITEM</u>	<u>COST SAVING</u>
TOKAMAK SYSTEM	\$ 65 M
ELECTRICAL POWER SYSTEM	\$ 45 M
EXPERIMENTAL AREA SYSTEMS	\$ 53 M
FACILITY CONSTRUCTION	\$ 23 M
NEUTRAL BEAM SYSTEM	\$ 29 M
DIAGNOSTIC SYSTEM	\$ 1 M
CICADA	\$ 1 M
	<hr/>
TOTAL Hardware	\$217 M
EDIA @ 25%	\$ 54 M
A & E	\$ 2 M
Contingency	<u>\$ 24 M</u>
TOTAL	<u>\$297 M</u>

TABLE 9-3
PITR-3 COST SPREAD BY FY*
(M\$)

SYSTEM ELEMENT	FY 80	'81	'82	'83	'84	'85	TOTAL
03 TOKAMAK SYSTEM	0	2.8	2.4	31.2	51.0	0	87.3
04 ELECT PWR SYSTEM	0	0.8	5.9	21.7	5.0	0.3	33.8
05 EXPR AREA SYSTEM	0	0.2	4.3	25.8	12.7	31.5	74.5
07 FACILITY CONSTR	0	0.3	13.4	14.1	1.4	0.2	29.4
08 NEUTRAL BEAMS	0	0	4.2	21.9	11.0	2.8	39.9
09 DIAGNOSTIC SYSTEM	0	0	0	0.3	1.1	0	1.4
10 CICADA	0	0	0.9	0.4	0	0	1.3
<hr/>							
TOTAL HARDWARE	0	4.1	31.1	115.4	82.2	34.8	267.6
EDIA (25% HARDWARE)	6.0	20.4	19.3	10.8	7.5	3.4	67.4
A & E ($\frac{1}{2}$ x TFTR)	0.2	1.8	0.8	0.2	0.1	0.2	3.3
<hr/>							
TOTAL	6.2	26.3	51.2	126.4	89.8	38.4	338.3

* Note: This schedule has not been coordinated with TFTR operation schedule, and is meant only to illustrate funding profiles.

9.3 Program Plan

The PITER Program Plan shown in Fig. 9-1 is a workable schedule of events leading to ignition demonstration in 1986. All program events are low risk with the exception of the Bundle Divertor. Methods of impurity control will be developmental issue in any high-Q or ignition machine and must be given considerable R & D attention in the next few years.

The double-walled titanium vacuum vessel is also moderate risk item but can be easily backed up with a conventional stainless steel design. The material properties of titanium, the first wall material, are not currently well known, particularly hydrogen interactions. A plan has been identified (refer to Section 4.4) for determining these properties so that a decision can be made in FY80 or 81 to proceed with the Ti vessel or revert to a stainless steel design.

The plan is based on maximum utilization of TFTR facilities and hardware but does not start impacting TFTR operations until late FY84. Use of a separate PITER test cell, needed anyway due to machine size, allows installation of all lines, plumbing and equipment to the point of final hook-up with TFTR facilities at the latest possible date. Modification of TFTR neutral beam lines will probably be the earliest item to impact TFTR operations depending on specific "hand-over" planning and modification time.

The new 1000 MVA power line requires at least two years of activity related to zoning easements and right of way planning, and hence must start immediately in FY80.

It is recognized that the schedule shown in Fig. 9-1 may be incompatible with TFTR operation; it is meant only to illustrate the time sequence of reactor installation. The starting point and details of the timeline would be adjusted once the operation schedule for TFTR in the period 1983-1987 is established.

PRINCETON IGNITION TEST REACTOR-LEVEL I MASTER SCHEDULE

8-8

Fig. 9-1

APPENDIX A.

CONSIDERATIONS FOR A BLANKET POWER CONVERSION TEST MODULE

Since the PITER will produce wall loadings comparable to those of full-scale reactors, it is conceivable that a power producing blanket module could be mounted on this device. Although the low duty factor would probably rule out any "prototypic" tests, some engineering experience would be gained. Furthermore, the actual extraction of usable energy might be an electrifying event for the fusion R & D program.

A nominal PITER pulse might release 5000 MJ of neutron energy onto 225 m^2 of wall during a 10 second burn. Allowing for a 15% peaking factor, the wall load on the outer wall would be 2.5 MW/m^2 . However, a cycle period of 10 minutes would limit the duty factor only to 1/60. The access region between TF and VF coils on the midplane would allow a test module of perhaps 1 m^2 face area to be moved up to the vacuum vessel. Given an 80% recovery of neutron energy as heat, this module would receive 20 MJ per pulse, or 33 kW average power ($= .114 \text{ MBtu/hr} = 44.7 \text{ hp}$). Conversion of this energy to electricity through a process with an efficiency of 15% would generate 5 kWe during a continuous series of pulses. Certainly this would energize a few light bulbs; in a more practical vein, it might be enough power to run the module coolant circulators, thus making the power module "self-sufficient".

The major elements of a blanket power test system would be the module itself, the coolant, the energy conversion system, and a "flywheel" to smooth out the energy release.

A steam engine would be the most suitable prime mover for the electrical generator. The power might be too low to use a turbine, but a steam piston engine should be quite adequate. (The efficiency would not be high, but that is a common feature of small engines.) Given the steam cycle, a thermal

flywheel can easily be obtained by suitably sizing the water reservoir of the steam boiler. Indeed, the use of a flash steam generator with pressure throttle has been suggested for this same function in tokamak power plants.

There are four classes of candidate coolant fluids: gas (He, CO₂, or steam), liquid metal (Li, Na, or K), molten salt (flibe or other industrial coolant), and water. Since the test module must attenuate radiation as well as the shielding which it displaces, gaseous coolants are not desirable. Furthermore, the use of a pressurized gas would complicate the module design. Liquid metal coolants, while frequently selected for power reactors, would be downrated for this experimental device because of the handling difficulties and fire hazards. Many of the advantages of liquid metals as coolants are also exhibited by molten salts (which have also been considered for real reactors); a problem with both is the elevated melting point and the consequent risk of freeze-up of the system. Water would probably be the most convenient coolant with which to operate, but it would require the use of a pressurized module. Note that even if the module coolant were water, a heat exchanger to a secondary steam loop would still be desirable to obtain the flywheel effect (and probably for tritium containment).

Based on these considerations, gas and liquid metal coolants should be avoided. A molten salt loop would simplify the module structural design, since only a few atmospheres' internal pressure would be required. On the other hand, the need for continual trace heating or special drain tanks would complicate the external systems. The problems would, however, be related to those of real reactors. Pressurized water would be comparatively easy to handle. However, an exit temperature of 350°C would require 160 atm, while 50 atm would give only 260°C.

The flowrate of the coolant need not be large. The short pulse length will cause nearly adiabatic heating of the structure regardless of flow rate, and rapid removal of the pulse energy could induce thermal shock in the rest of the system. A lower flow rate and higher coolant ΔT will minimize the pumping requirements. A high flow rate would rapidly cool the module and then circulate essentially constant temperature coolant.

Given a low flow rate, the volume fraction of coolant in the blanket need not be large. However, both water and the molten salts would be neutronically acceptable in large amounts. The structural material would probably be stainless steel, since unconventional performance would not be required. A suitable configuration for the module might consist of coolant flowing radially (out from the plasma) through a canister filled with slabs or rods or balls of steel, which would provide extra attenuation of neutrons and extra thermal inertia. The shape and thickness of the canister would depend, of course, on the pressure required.

Estimates of the performance of such a module were obtained from a simple numerical model. This computed temperature profiles through the module and in the steam generator by stepping small slugs of coolant around the circuit. The numerical algorithm calculates the heat transfer across coolant passage walls during a single time step, adjusts temperatures correspondingly, and then moves the coolant ahead. Transient heat flow in the surrounding media is ignored: in the module because coolant passages are assumed to be closely spaced; in the steam generator because mixing of the water is assumed to provide the thermal inertia.

The following parameters were used: The neutron wall load was taken to be 2.5 MW/m^2 onto a 4. cm thick vacuum wall in front of a module 40 cm deep with a 1 m^2 face. The neutron energy was deposited exponentially with an 8 cm attenuation length. Coolant flow was taken to be radial outward through

passages with $.2 \text{ m}^2$ cross sectional area and 60 m^2 side-wall (e.g., corresponding to the spaces between 2 cm dia. rods in a 2 cm square lattice). The module was assumed to be 80% steel, with a heat conductance of $2000 \text{ W/m}^2\text{K}$ to water. The steam generator (and thermal flywheel) was modeled as 1 m^3 of water; channels for the coolant were taken to be 2 m long with $.1 \text{ m}^2$ flow area. The heat transfer surface between the coolant and the water was assumed to be 70 m^2 (e.g., representing finned tubes); the same surface heat conductances were used as for the blanket module.

Temperature histories were calculated for a 600 second cycle with a 10 second burn, assuming that steam was continuously generated at the cycle-average module power level. With the coolant, blanket module, and steam generator all preheated, only one cycle was needed to establish regular time-varying temperature patterns. (The nominal base temperature was 200°C , but any value could be used; only the variations are significant.) For the conditions assumed the steady power was 25 kW and the water (and steam) temperature varied less than 2°C .

The accompanying Fig. A-1 shows temperatures for a module cooled with water flowing at 5 kg/s. The curves plotted are: W - the water in the steam generator; CI - coolant at blanket inlet; CO - coolant at blanket outlet; MI - inboard (inlet) end of the module; MO - outboard (outlet) end of the module. The time axis has been compressed: the burn time is scaled into one time unit while the remainder of the cycle is scaled into three time units.

The inboard end of the module, which receives the greatest neutron heating, rises by about 30°C during the pulse. The adiabatic temperature rise would be about 50°C , so the coolant has some effect. However, the outboard blanket and coolant outlet temperatures hardly rise at all during the pulse, since the heat removed from the front of the module is transferred

Fig. A-1

back to heat the rear. Eventually the coolant temperature rises by 10° or 15° , and after an additional lag the steam generator temperature rises slightly. Because of the large heat transfer surface in the steam generator (probably more than is required) the coolant temperature at the exit, i.e., at the blanket inlet, is pegged at the steam temperature.

This system is capable of supplying nearly constant quality steam, at a temperature which is appropriate to the coolant and the blanket module. The smallness of the output temperature fluctuation is due to the temperature leveling between the front and back of the blanket and the large heat capacity of the steam generator. Of course, the system must be preheated; even with no extraction of energy, the steam generator temperature will rise only 2.4°C per pulse. However, drift of the temperature from the preheat value will be an adequate index of self-sustained operation.

Whether a system like this would represent in some way a prototype power reactor blanket is a matter of subjective opinion.

Appendix B

COST OF NEUTRAL BEAM SYSTEMS

B.1 Introduction

The PITER neutral beam system has been conceptualized to the point where costs were obtained for 160-keV tangential and 90-keV perpendicular (ripple injection), D^0 , 60-MW, neutral beam systems. Comparisons were made between positive-ion based systems with and without direct recovery and negative-ion based systems with and without direct recovery. The costing was done assuming the TFTR neutral beam system would be available for conversion. For the direct recovery cases, costs have been determined as a function of recovery efficiency. For the positive-ion cases, costs have also been determined as a function of the ion source D^+ fraction.

For the 160-keV tangential case, negative-ion based systems are cost competitive. However, it is judged that direct recovery technology is more likely to be available in 1985, so costs for a positive-ion based system with direct recovery were used for the PITER point design.

In order to establish neutral beam system costs, it was necessary to project neutral beam system efficiencies and then to combine these efficiencies with a costing model. Section B.2 describes the projection of efficiencies, Section B.3 mentions the costing model, Section B.4 discusses the results, and Section B.5 summarizes the results.

B.2 Efficiencies

Neutral beam system power efficiencies were obtained from calculations which included:

- a. transmission losses,
- b. losses to fractional energy components,
- c. reionization losses,
- d. less-than-equilibrium neutralization cell,
- e. losses due to charge-exchange,
- f. losses due to ionization in grid gaps,

- g. losses due to secondary electrons in grid gaps,
- h. losses due to stripping of negative ions.

Figure B-1 shows efficiencies for delivering full energy neutral power as a function of energy. Shown are "no-direct recovery" and "80% efficient direct recovery" cases for a positive-ion system using a 90% D^+ source and for a direct extraction negative-ion system using a gas neutralizer. The positive-ion system with 80% recovery was selected as the point design for this study.

B.3 Costs

Costing was done by considering the costs of individual components of the TFTR system, then for those components which must be modified or replaced, scaling the costs in power, power loading, current, or gas loading, as appropriate. The base TFTR costs did not include prototype costs, which contain the system engineering costs. Thus engineering costs are not included in the cost estimates presented here.

B.4 Discussion

Shown in Fig. B-2 are the costs of 60 MW of D^0 delivered to the plasma for 160-keV and 90-keV, positive- and negative-ion based systems, as a function of direct recovery efficiency. It can be seen that 90-keV negative-ion systems are unattractive from a cost viewpoint, while 160-keV negative-ion systems are cost-competitive. The 160-keV point design selected for this study uses positive ions and direct recovery, because it was judged that direct recovery technology is more likely to be available (than negative-ions) in 1985.

It can be seen from Fig. B-2 that considering just direct neutral beam system costs, there is incentive for going to direct recovery mostly for the case of 160-keV, positive-ions. However, there are advantages other than cost in using direct recovery or negative-ions. This is illustrated in Fig. B-3, which shows the primary power requirement on the left vertical axis, and the beamline power handling requirement on the right vertical axis, as a function of direct recovery efficiency. From the standpoint of primary power and power handling requirements, Fig. B-3 shows that there is

Fig. D-1

Fig. B-2

Fig. B-3

incentive for going to efficient direct recovery, particularly for the 160-keV, positive-ion case. Large beamline power handling requirements will be reflected in large water flow rate requirements and in engineering design difficulties.

In the cost and power considerations of Figs. B-1 and B-2, it is assumed that the fraction of D^+ from the ion source is 0.9. Although there is ongoing development towards increasing the source D^+ fraction, its present value in ORNL and LBL development program sources is 0.8 to 0.85. The effect of variation of this parameter is shown in Fig. B-4 for the 160-keV system and in Fig. B-5 for the 90-keV ripple-injection system. Curves for zero percent, 50%, and 80% direct recovery are shown in each figure. Also shown in the figures is the number of converted TFTR-style beamlines required assuming these units can be upgraded to twice the gross ion-current of the present design. The required primary power and the required beamline power handling capability are shown in Fig. B-6. It can be seen that there is a need to maintain and hopefully increase the source D^+ fraction, although efficient direct recovery makes this less necessary. It should be noted that in present direct recovery concepts, high efficiency requires a high D^+ fraction.

B.5 Summary

Table B-1 summarizes costs, number of beamlines required, primary power required, and power handling required for the various combinations: 160 keV or 90 keV; positive-ions or negative-ions; and zero percent, 50%, or 80% recovery. Considering as a baseline a 160-keV, D^+ , no recovery, 4-unit system as the minimum extrapolation from the present, demonstrated heating technology, the following conclusions are drawn:

- Efficient direct recovery would substantially ease cost, power, and power handling requirements.
- Ripple injection would substantially ease cost, power, and power handling requirements while doing away with the need to extrapolate from the present 100-keV systems.
- Negative ions would reduce cost, power, and power handling requirements.
- Negative ions are not attractive for ripple injection.
- The source D^+ fraction should be at least 90% to minimize injector cost and power handling requirements.

Fig. B-4

Fig. B-5

Fig. B-6

Table B-1

	Cost with TFTR Units, M\$	Number of Converted TFTR Beamlines	Primary Power Required, MW	Beamline Power Handling Required MW
160 keV p^+ No recovery	43	4	301	241
160 keV D^+ 50% recovery	34	4	209	149
160 keV D^+ 80% recovery	30	4	172	112
160 keV D^- No recovery	30	4	126	66
160 keV D^- 50% recovery	29	4	104	44
160 keV D^- 80% recovery	28	4	98	38
90 keV D^+ No recovery	22	4	153	93
90 keV D^+ 50% recovery	19	4	118	58
90 keV D^+ 80% recovery	17	4	107	67
90 keV D^- No recovery	50	>4	132	72
90 keV D^- 50% recovery	48	>4	110	50
90 keV D^- 80% recovery	47	>4	104	44

APPENDIX C

RELATIONSHIP OF PITR TO AN EPR

A complex question that we face in the Fusion Energy program is: "What steps should be taken between the early experimental devices, such as PLI, and a prototypical power reactor such as EPR?" TFTR was a logical step to take since it is still at the level of addressing fundamental plasma physics issues. After TFTR, a transition must be made between the experimental devices and an early power producing reactor.

Going directly from TFTR to EPR intuitively seems very risky since there are too many physics as well as technological uncertainties that affect even basic machine variables such as sizing. On the other hand, a very conservative series of small steps would be very costly and bring us well into the next century before a power demonstration can take place. Determining the best compromise between these two extremes involves many considerations such as the projected state of technology, the value of money over the next couple of decades, and the degree of urgency of the Fusion Energy program. This appendix does not attempt to answer the broad question but gives the beginning of an answer in terms of one consideration—total expected cost.

Econ, Inc. (Figure C-1) examines methodology for determining probabilistic cost distributions for long term fusion test programs. The analysis given here makes use of a simplified version of that methodology. Expected cost is determined for five programs leading to EPR:

1. Go directly from TFTR to EPR;
2. A small relatively inexpensive ITR, specifically PITR-2, prior to EPR;
3. PITR-3 prior to EPR;
4. PITR-3 plus LPX prior to EPR, and;
5. the ORNL TNS-4 (Nb_3Sn) prior to EPR.

The procedure used in the analysis is briefly the following: The basic variable used in the approach is Ignition Margin, which in turn is based on the empirical scaling law. Probability of success and cost are projected as

SUCCESS
PROBABILITY
DENSITY

IGNITION MARGIN

CUMULATIVE
SUCCESS
PROBABILITY

SUCCESS PROBABILITY VS. IGNITION MARGIN ASSUMING NO OTHER
UNCERTAINTIES

Fig. C-1

a function of Ignition Margin for each device in each of the 5 programs. "Decision trees" are then made for each program to arrive at total expected cost.

Projecting probability of success versus Ignition Margin is, of course, highly judgemental at this time, and will be very dependent on other considerations such as impurity control capability. Figure C-1 shows that we consider a reasonable relationship between probability of success and ignition margin, assuming no other technological uncertainties. It says that the most likely minimum ignition margin that will result in ignition is 3.5. This, again, is judgemental and can be varied as new inputs are received. Figure C-2 shows projections of EPR success probability considering the technological uncertainties remaining just prior to EPR for each of the five representative programs considered. In each case prior devices are assumed to be successful. For example, where no ITR or proto-EPR (PEPR) is involved we project a success probability no greater than 20%. This is an arbitrarily selected low value reflecting high risk items such as the superconducting magnets, impurity control requirements, and high duty cycle operation.

Building a small, low cost (\$156 M) ignition reactor (PITR-2) which is successful with an ignition margin of 2.5 prior to EPR increases the EPR success probability to 40%. Although PITR-2 demonstrates ignition it is considerably smaller than EPR ($R_0 = 2.1$ m, $a = 0.45$ m), does not have a divertor, and is low duty cycle. Another example of a small ignition device in this category is the MIT HFITR which has an ignition margin of 1.5 and costs about \$120 M.

PITR-3 has a plasma size closer to EPR, with an ignition margin of 6.0, and employs a bundle divertor for impurity control, thereby increasing projected success probability for EPR to 60%. PITR-3 capital cost is \$217 M.

PITR-3 plus LPX removes uncertainties related to superconducting magnets and long pulse operation which, if successful, further improves the chances for EPR success.

Finally, a proto-EPR, which we represent as the ORNL TNS-4, brings EPR success probability up to 90%, again if it is successful.

Table C-1 summarizes success probability and cost for each device used in the five programs. The PITR and LPX costs are from this TFTR-2 study. EPR costs are based on the ANL EPR, extrapolated as a function of ignition margin as shown in Fig. C-3 (baseline cost from Figure C-2).

FIGURE C-2

EPR SUCCESS PROBABILITY WITH
TECHNOLOGICAL UNCERTAINTIES

Table C-1
SUMMARY OF ALTERNATE PROGRAMS

PROGRAM	ITR/PEPR	IGN MARG	P _S SUCCESS	CAPITAL COST	EPR	IGN MARG	P _S SUCCESS	CAPITAL COST
1	none	-	-	-	EPR-A	6.0	0.2	\$1.26 B
2	PITR-2	2.5	.3	\$156 M	EPR-B	2.5	0.4	\$725 M
3	PITR-3	6.0	.8	\$217 M	EPR-C	3.5	0.6	\$875 M
4	PITR-3 + LPX	6.0*	.6	\$371 M	EPR-C	3.5	0.8	\$875 M
5	PEPR	2.4	.4	\$550 M	EPR-B	2.5	0.9	\$725 M

* PITR-3 ONLY.

FIGURE C-3

EPR COST PROJECTION

Figures C-4 through C-8 show the "Decision trees" for each program. In each case the program is driven to at least 80% success probability by spending up to half the original cost of a device for "upgrading" if a failure takes place.

The resulting expected cost comparison is as follows:

<u>Program</u>	<u>ITR/PEPR</u>	<u>Expected Cost</u>
1	none	\$1.71 B
2	PITR-2	\$1.28 B
3	PITR-3	\$1.29 B
4	PITR-3 + LPX	\$1.27 B
5	TNS-4	\$1.63 B

Programs 2, 3 and 4 yield essentially the same expected cost and are the lowest. Obviously some other criterion, such as peak annual funding, must be used to choose between them.

Another consideration in comparing programs is time scale. Program 1, which goes directly to an EPR, could yield a power reactor as early as 1987-1988 if successful. However, unless public awareness of the energy crisis increases greatly the chances of funding a program having a success probability around 20% are very poor.

Programs 2, 3 and 4 would result in a power reactor in the early to mid-90s. The chances for an early EPR success, however, are greater with Program 4, less with Program 3, and so forth.

Program 5 involves a TNS which would become operational in the late 80's at best. This pushes the EPR into mid to late 90's and is the lowest urgency program.

Although the success predictions used in this analysis are debatable, the overall trend of the results is relatively insensitive to them. In fact, the results are more dependent on the philosophy represented by each program than specific numbers, within limits. Finally, it is emphasized that this analysis does not imply a decision in itself, but only one factor which should be weighed against many others. Also, other program sequences will undoubtedly be considered.

$$.2(1.26B) = 252M$$

$$(.8 \times .1)(1.26B) = 101M$$

$$(.8 \times .9 \times .8)(1.89B) = 1.1B$$

$$(.8 + .9 \times .2)(1.89B) = 272M$$

$$\text{EXPECTED COST} = \$1.71B$$

FIGURE C-4

PROGRAM -1

EXPECTED COST \$128 B

PROGRAM 2

Fig. C-5

FIGURE C-6

17R06RAM 3

EXPECTED COST = \$1.29B

Fig. C-6

FIGURE C-7

PROGRAM 4

EXPECTED COST = \$ 1.27 B

Fig. C-7

FIGURE C-8

PROGRAM 5

EXPECTED COST = \$1.638

Fig. C-8

References

- [C-1] ERDA Rept. No. COO/4181-1, "The Economics of Fusion Research," ECON. INC., Oct. 1977.
- [C-2] ORNL/TM-5813 "ORNL Fusion Power Demonstration Study: Interim Report," March 1977.
- [C-3] Grumann, D. W., "Parametric Investigation of TMS Conceptual Designs," General Atomic Corp., Proc. of 7th Symp. on Eng. Prob. of Fusion Res., Oct. 1977.