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RECENT CONTRIBUTIONS TO FUSION REACTOR DESIGN

FUSION POWER PROGRAM
ARGONNE NATIONAL LABORATORY
ARGONNE, ILLINOIS 60439

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MASTER

FUSION POWER PROGRAM

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RECENT CONTRIBUTIONS TO FUSION REACTOR DESIGN*

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Foreword.

The enclosed collection of papers summarize areas where ANL fusion reactor design efforts have led to significant progress. Many of the insights gained through these detailed studies have been incorporated into the 1977 ANL EPR design.

A review of this segmented water cooled design begins on page of this report. Outstanding features are its provisions for remote replacement of components, its use of a passive impurity control system, and its relatively low cost (~ 600 million dollars).

The remaining papers included in this collection are specific to an area of tokamak reactor design. They can be conveniently divided into seven major areas: blanket design, tritium control, the engineering of plasma heating systems, plasma engineering studies, toroidal field coil design, pulsed coil development, and plasma driving system development.

Major conclusions reached in the four papers dealing with blanket design include:

1. It may be possible to build a fusion reactor with a conducting first wall, thereby avoiding the incorporation of potentially unreliable circuit breakers (page 1).
2. The presence of a divertor leads to much higher allowable wall loadings in stainless steel systems for both lithium and helium cooled systems (page 6).
3. Analysis of likely plasma dump scenarios indicate that while a graphite liner is helpful, it is not essential for the survival of a water cooled stainless steel first wall. If a radiatively cooled graphite liner is used, wall loadings must be restricted to less than 2 MW/m^2 (page 12).
4. Coolant channel geometries can be optimized so that in a water cooled blanket the maximum strain occurring during the burn cycle is less than 0.18% (page 18).

5. A dual coolant system employing a superheated steam cycle has been designed that could allow thermal conversion efficiencies of as high as 39% (page 18).

Tritium control is an area that has received increasing attention at ANL. This work has involved the coordinated use of computer code simulation and experimental bench marks (page 26). A major point of significance discovered by this work was that the conversion of tritium to tritiated water occurred at a higher rate in experimental measurements than predicted by accepted analytic models. This has the effect of sharply increasing the required clean-up time.

In a definitive study of the tritium handling requirements for the GA/ANL TNS it was concluded that current technology is adequate though the pumping requirements of the upgraded version of TNS will require a system costing about 8 times as much as is envisioned for a basic ignition device (page 31).

Both rf and neutral beam systems were engineered for use with the ANL EPR and the GA/ANL TNS. The rf system was designed for lower hybrid heating with an expected cost $\sim 30\%$ less than for comparable neutral beam heating (page 37).

The design effort on neutral beam systems has included three options, two based on D^+ sources and one on a D^- source. A feature of this design study is an in-depth neutronics analysis around the necessary duct penetrations (page 43). Other important features of this work include a careful analysis of the power and gas flow required by each of the three design options (page 50) and the development of a method to prevent arcing of the high voltage electrodes used in the neutral beam sources (page 56).

The plasma engineering studies used as a basis for the ANL 1977 EPR design are presented in a paper starting on page 131. MHD equilibrium calculations for plasma elongation values of 1.3 and 1.65 indicated toroidal beta values of $\sim 10\%$ to be possible. Subsequent stability calculations using the PEST code resulted in critical beta values of $\sim 6\%$. A major result of this plasma engineering effort was the determination that net electrical power could be produced with the ANL EPR design for the worst case situation where β_t was as low as 5%, the magnetic field was restricted to 8 T and thermal

conversion efficiency was only 30%. Thus the ANL-EPR, which is capable of 10 T operation, at thermal efficiencies approaching 39% has considerable design margin.

Work on superconducting magnet systems has been divided between three major areas: steady state toroidal field coils; the pulsed equilibrium field and ohmic heating coils; and the power supplies and energy transfer systems necessary to their operation. Two new ideas being developed for the TF coil design involves the use of aluminum alloy support material to lower weight and cost and the addition of ferromagnetic segments to reduce field ripple while increasing access to the reactor (page 67). Additionally a computational model for predicting the performance and cost of superconducting TF coils has been developed and integrated into the ANL systems analysis code (page 72).

The design effort on the equilibrium field coils found that exact "D" shaped plasmas cannot be formed if the EF coils remain external to the TF coils and if their stored energy is restricted to reasonable limits. Fortunately, relaxation of the "deity" requirement does not appear to have any severe effect on the calculated plasma stability (page 77).

An ohmic heating coil design for the GA/ANL TNS design has been developed (page 81) and a small experimental program on pulsed superconducting coils initiated (page 86). The field limits on such an ohmic heating solenoid have been studied for both graded and ungraded designs (page 90).

In developing a plasma driving system design, several different combinations of central energy storage and transfer devices were considered (page 99). Of these a superconducting energy storage inductor coupled with a homopolar generator appears to be the most economic over the full range of possible ohmic heating ramp times.

Consistent with this analysis, an energy storage and transfer system was designed for the ANL-EPR. The inductor-converter bridge circuit required to transfer energy from the storage ring to the EF coils was developed (page 104). Experimental programs have been initiated to test the performance of both the inductor-converter bridge and the homopolar generator (page 109).

RESISTIVE REQUIREMENTS FOR THE VACUUM WALL OF A TOKAMAK FUSION REACTOR

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Introduction

Most conceptual designs of tokamak power reactors have incorporated a ceramic insulator in the vacuum wall to make the wall electrically non-conducting. The purpose of this "flux breaker" as it is known is to prevent a toroidal current from being induced in the wall by the transformer action of the various poloidal field coils. The flux breaker is typically a small ceramic strip filling a gap between the otherwise metallic structure of the wall. The flux breaker is seemingly a small part of the reactor design. If it is necessary, however, the development of a ceramic member of the vacuum boundary will require a major effort to find one that can operate successfully as part of the large toroidal wall envisioned for commercial tokamak reactors. Such a material will have to be highly resistant to radiation damage at doses up to at least 10 MW-yr/m² while being compatible with a coolant and a first wall whose dimensions change due to thermal cycling and radiation damage. Thus there is considerable incentive to assess the consequences of eliminating the flux breaker from the design and having a conducting boundary instead. In this initial study the question of having a finite wall resistance has been examined in terms of its major implications on both the normal and abnormal operation of a tokamak reactor. This study has been conducted within the framework of the ANL-EPR-77 design⁽¹⁾ although the results should provide some guidance for future reactors as well. The EPR design referred to is a 5 m major radius tokamak with an aspect ratio of 3.5, and with an equilibrium plasma current of 7.3 MA. The vacuum chamber is designed to accommodate a non-circular plasma with a height to width ratio of up to 1.65. The basic vacuum wall design is shown in Fig. 1. It is located about 0.4 M from the plasma boundary and has an irregular polygon shape made of sixteen sections, one per TF coil interval. Variations of this design having a range of resistance values have been used in the analysis.

Effect of Conducting Wall on Normal Operation

The presence of a conducting wall will tend to interfere with the current buildup in the plasma during the startup and will tend to distort the equilibrium field supplied by the EF coil. These points have been investigated by means of the equivalent circuit of the poloidal field coils, the plasma, and the wall shown in Fig. 2. The circuit constants pertaining to the plasma and wall are shown in Fig. 2 and were computed by means of a magnetics code in which the wall was modeled by twelve inductively coupled rings and the appropriate flux linkages between coils summed to get the single-coil-equivalent parameters. Coupling terms involving the IT coil have been treated as a variable. The plasma and wall currents corresponding to Fig. 1 are given by:

$$\begin{aligned} \frac{d}{dt} (L_p I_p) + R_p I_p &= M_{OH,p} \frac{dI_{OH}}{dt} + M_{EF,p} \frac{dI_{EF}}{dt} \\ &+ M_{IT,p} \frac{dI_{IT}}{dt} - M_{w,p} \frac{dI_w}{dt} \end{aligned} \quad (1)$$

Figure 1. Basic Vacuum Wall Design.

$$\begin{aligned} L_w \frac{dI_w}{dt} + R_w I_w &= M_{OH,w} \frac{dI_{OH}}{dt} + M_{EF,w} \frac{dI_{EF}}{dt} \\ &+ M_{IT,w} \frac{dI_{IT}}{dt} - M_{w,p} \frac{dI_p}{dt} \end{aligned} \quad (2)$$

Equations (1) and (2) and the equations for the coil currents were solved by coupling them to a plasma burn cycle code to obtain the complete behavior of the currents and the plasma during the startup. The burn cycle code solves profile average, time dependent, particle and energy balance equations for the constituent plasma particles. It includes models of radiation, transport, and physics relating to the breakdown stage.

The startup of the EPR is accomplished by first establishing currents in the IT and OH coils by means of small power supplies (not shown). At the start of the burn cycle a resistor is connected to the IT coil and a homopolar generator (capacitor) is connected to the OH coil. The IT coil current decays rapidly, in about 8.0 ms, to provide a high voltage "blip" across the plasma. This breaks down the plasma and establishes the initial discharge current. The OH current is reversed from 0-2 seconds by means of the homopolar generator to induce a large plasma current. Beam heating is begun at 2 seconds and turned off at 6 s, at which time the plasma is at ignition. During the

Figure 2. Equivalent Circuit of Ohmic Heating (OH), Equilibrium Field (EF), and Initiation-Trimming (IT) Systems; with Plasma and Vacuum Wall.

startup the current in the EF coil is raised to keep the plasma in MHD equilibrium as well as to induce a share of the plasma current. The effect of the conducting first wall on the initiation of the discharge and on the rest of the startup have been studied using a range of IT coil parameters and wall resistances.

Initiation Stage

For the initiation process, preliminary results indicate that for geometries similar to the ANL-EPR-77 design an IT coil system can be built which is compatible with a conducting first wall. Different geometries of IT coils have been considered with the aim of minimizing the total stored energy and flux swing during the IT cycle. The results so far indicate that the total volt-seconds supplied by the IT coil depend critically on the ratio $M_{IT,W}/M_{IT,P}$, the fraction of the IT flux to the plasma which also links the wall. This dependence is shown in Fig. 3 for a value of $R_W = 700 \mu\Omega$. This figure shows the individual and total components of the supplied volt-seconds, at the end of the 8 ms initiation period. These results were found to be fairly insensitive to the value of R_W in the range studied, that of 300-900 $\mu\Omega$. Since the stored energy is proportional to the square of the supplied volt seconds it is important to minimize this quantity. This can most obviously be done by producing an IT coil geometry which ducts the IT flux through the first wall, however such a system would be comparatively complex and might require coils placed between the wall and the inner leg of the TF coils. Coils in this location would complicate the construction. This problem is undergoing further study.

Figure 3. Initiation-Trimming Coil Requirements, with a Conducting Wall, as a Function of $M_{IT,W}/M_{IT,P}$, for an 8 ms Plasma Initiation Period.

Main Startup

For the analysis of the main startup stage a broader range of wall resistance values were examined. Figure 4 shows the effect of the wall resistance on the wall current and plasma current during the full ~ 6 s startup. The curve of plasma current vs time corresponding to $R_W = \infty$ represents the nominal "ideal" startup using a flux breaker. It seems reasonable to require that the plasma current with a conducting wall should not deviate appreciably from the "ideal" case. The deviation is significant for $R_W = 25 \mu\Omega$ but is small for $R_W \geq 100 \mu\Omega$.

Another effect of the wall current is to heat the wall, because of I^2R losses. The conducting wall also acts as a load on the OH and EF coils and so tends to increase their requirements. By the end of startup the resistively dissipated energy in the wall is 9.4 MJ for $R_W = 100 \mu\Omega$ and this decreases to about 1 MJ for $R_W = 1000 \mu\Omega$. This range poses no significant problems. The effect on the volt second requirements of the OH and EF coils is small for $R_W \geq 100 \mu\Omega$.

Another important consideration is the magnitude of the spurious magnetic fields, across the plasma region, arising from the induced wall current. This field was computed using the 12-segment wall model mentioned above. The normal peak equilibrium field, at the plasma center is 0.53 T. In comparison the maximum wall induced field is only 0.01 T for $R_W = 100 \mu\Omega$. The field becomes even smaller for a higher R_W . Therefore the induced field from the wall should not be a serious problem during the startup. (The effect on the initiation period where a null region is normally used to create the plasma may be more serious and needs additional examination.)

After the startup, during the main plasma-burn period (~ 50 s), the OH and EF coils are quiescent and so the conducting wall does not affect this part of the burn cycle. The shutdown period is basically

Figure 4. Plasma and Wall Currents During the Main Startup Phase. For Different Values of Wall Resistance.

the reverse of the startup, in terms of the various currents so an acceptable wall resistance for startup will be acceptable for shutdown.

Abnormal Operation - Plasma Dump

A major plasma disruption or "plasma dump" is characterized by a rapid loss of plasma thermal energy and the subsequent decay of current. Eddy currents induced in a conducting wall by the decay of plasma current can be large and will cause pressure loading and heating of the wall. To evaluate these problems it is important to have a good estimate of the time scale of the dump. In the present study this estimate was made following the work of STIX⁽²⁾ that leads to the prediction that the electron temperature (and current density) should vary on a time scale

$$\tau_D = \frac{C a^2 \pi R q}{\Delta^2} \sqrt{\frac{M_e}{2T_e}} \quad (3)$$

Fig. 5 plots experimental voltage transient times against the formula with $\Delta = 2 q \rho_i$, $C = 0.1$ and $q = 2.01$, where a is the minor radius and ρ_i is the ion gyroradius, and from this scaling one expects voltage transient pulse widths of $\sim 300 \mu s$ for the EPR.

A larger number of variables seem to determine the plasma current decay time and a review of published data³⁻¹² reveals no obvious pattern; experimentally ΔI_p occurs on a scale of tenths to tens of milliseconds. The important thing about this range of current decay times is that it is shorter than the time constant of the wall, $\tau_w = L_w/R_w$, for any feasible value of wall resistance. (For example for $R_w = 500 \mu\Omega$, $\tau_w = 10 \text{ ms.}$) A worst case analysis of the wall

current induced by a plasma dump can then be made by treating the OH and EF coils as decoupled from the plasma and wall during the plasma current decay time. Under these assumptions Equation (2) reduces to:

$$L_w \frac{dI_w}{dt} = -M_{w,p} \frac{dI_p}{dt} \quad (4)$$

Integrating Equation (4) over the decay time then gives the maximum value of wall current, for this worse case:

$$I_w^{\max} = \frac{M_{w,p}}{L_w} I_p^{\max} \approx I_p^{\max}$$

where $I_p^{\max}$ is the value of plasma current prior to the disruption. In other words the full plasma current is transferred to the wall because of the dump.

Pressure in the Vacuum Wall After a Plasma Dump

After the dump the wall current will interact with the magnetic field to produce a pressure in the wall. This pressure can be estimated using the following simple model.

To first order, the distribution of eddy currents induced in the vacuum wall will be such as to maintain the field unchanged outside the wall. Thus the field outside B_0 will continue to be the resultant field from the previous plasma current and the EF coils, while the field B_1 in the region enclosed by the wall will be the field from the EF coils alone. The pressure P acting in the wall will be

$$P = (B_1^2 - B_0^2)/2\mu_0.$$

Figure 5. Voltage Transient Pulse Widths During a Major Plasma Disruption, for a Range of Tokamak Parameters. Solid Line = Formula; Circles are Experimental Data Where a-j are References 3-12 Respectively.

If the wall were everywhere parallel to the magnetic field vector from the plasma current, the above argument would be correct. Because it is not, we attempt to compensate by using only the components of B_i and B_o parallel to the wall. The resulting pressure distribution in the vacuum wall appears in Fig. 6.

The pressures shown in Fig. 6 are acceptable. The highest pressures are 45 psi, and thus do not appear to introduce any significant design difficulty.

Heating in the Vacuum Wall After a Plasma Dump

The current flowing in the wall, just after a plasma dump, will involve a stored magnetic energy of $1/2 L_w I_w^2 \approx 135$ MJ. The current will decay resistively as $I_w = I_w^{\max} e^{-t/\tau_w}$ where t is the time after the dump. Ohmic heating in the wall will therefore decay as $P_w = I_w^2 R_w = (I_w^{\max})^2 R_w e^{-2t/\tau_w}$. Again this calculation ignores the somewhat beneficial coupling to the OH and EF coils which will remove some energy magnetically from the wall. The heating is essentially over in a time $t \leq 2 \tau_w$ during which time the initial stored magnetic energy is converted into heat. The heating time is much shorter than the heat diffusion time of any possible wall design. Effectively, then, the wall will receive and have to survive an impulsive heat load right after the dump.

Engineering of a High Resistance Wall

The vacuum vessel for a tokamak reactor will, in all likelihood, need to be constructed in a manner that will facilitate both assembly and disassembly. Thus most designs are for a segmented wall of sufficient thickness to withstand anticipated normal and off-normal stresses (see Figure 1 for a typical design). In terms of controlling the resistance of the wall the designer has three principal variables at his disposal; the

Figure 6. Pressure Distribution in Vacuum Wall Due to and Immediately Following a Major Disruption. Pressure in PSI. (Center of reactor is to the left)

choice of wall material, the wall thickness (down to some minimum determined by the material's strength, and the design of the joint between adjacent wall segments.

Three possible classes of metals for use in the construction of a vacuum wall are the austenitic stainless steels (such as annealed Type 316), the higher nickel steels (such as Inconel 625), and titanium based alloys (such as Ti-6 Al-4 V). These alloy systems offer resistivities of roughly 100, 135 and 185 $\mu\Omega\text{-cm}$ at 400°C respectively to the designer. Allowable wall thicknesses are a function of the strength of the material selected. Representative yield strength values for the three classes are 115, 450 and 580 MPa respectively, indicating that thinner walls may be possible if titanium alloys are used than if stainless steel is employed. Based on strength and fabrication considerations, minimum thicknesses for designs in which the vacuum wall is removed from the plasma boundary for the three possible wall materials would be about 2 centimeters for stainless steel, 1 centimeter for Inconel 625 and 0.5 centimeter for titanium alloys.

Figure 7. The Design of the Joint Between Adjacent Vacuum Vessel Segments in the ANL-EPR.

Therefore, for designs in which the joint between wall segments does not add significantly to the toroidal wall resistance, the maximum resistance attainable is about 135 $\mu\Omega$ for a stainless steel wall, 365 $\mu\Omega$ for an Inconel 625 wall and 1000 $\mu\Omega$ for a titanium alloy structure. Thus continuous walls without high resistance segment joints may be possible if IN 625 or a titanium alloy is used, but are probably not feasible if stainless steel is employed. The use of a wall structure that has significant inherent electrical resistance is of great benefit in controlling the temperature rise that occurs in the wall due to the electrical currents generated by a plasma dump incident. For example, as discussed above, in the case of the ANL-EPR design, about 135 MJ are expected to be dissipated in the vacuum wall following a plasma dump. This will cause only a small temperature rise ($< 10^\circ\text{C}$) in a high resistance wall such as could be constructed from titanium alloy due to the large mass of the wall. However dissipation of this energy is an important consideration if the overall wall resistance must be achieved by the incorporation of a high resistance joint design.

For example, in the case of a stainless steel wall, to achieve an overall wall resistance of 500 $\mu\Omega$, roughly 350 $\mu\Omega$ must be supplied by the joint between wall segments. One approach to this design problem would be to coat the portions of the wall in mechanical contact (see Figure 7) with an insulating material and provide a vacuum seal for the joint by means of a thin (and therefore resistive) piece of stainless steel that bridges the joint.

A 0.025 cm sheet of 10 cm in width between each of 16 wall segments could provide the necessary increment in wall resistance. Unfortunately, the temperature rise in these pieces due to the ohmic heating following a plasma dump would cause them to fail. A possible solution to this problem, that allows the use of a stainless steel wall is the incorporation of graphite resistors into the joint design. The selection of graphite is based on its relatively high resistivity ($\sim 750 \mu\Omega\text{-cm}$) and high temperature capability. A graphite layer 10 cm wide by 0.2 cm thick is envisioned to be joined mechanically to overlapping portions of the stainless steel wall at each of the 16 joints (see Figure 7). Such an addition would allow an overall wall resistance of $\sim 600 \mu\Omega$ while restricting the temperature rise in the graphite due to a plasma dump to about 1000°C , which should be acceptable.

The use of such an approach probably requires the incorporation of a second vacuum boundary into the design, a precaution that is included in the ANL-EPR.

Conclusion

The effect of having a conducting vacuum wall, instead of one with a flux breaker, has been analyzed from a multi-disciplinary standpoint. There is a good indication that a conducting wall will be tolerable. The most serious problems seem to be in designing an acceptable initiation-trimming coil system and in designing a vacuum wall that can withstand the pressure and heat loads following a plasma dump. There appear to be several promising design approaches for the vacuum wall that can achieve a fairly high resistance and may satisfy the latter constraints. Work is continuing on this important topic to try to further resolve these questions.

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THERMAL HYDRAULIC ANALYSES OF TWO FUSION REACTOR FIRST WALL/BLANKET CONCEPTS*

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Summary

A comparative study has been made of the thermal hydraulic performance of two liquid lithium blanket concepts for tokamak-type reactors. In one concept lithium is circulated through 60-cm deep cylindrical modules oriented so that the module axis is parallel to the reactor minor radius. In the other concept helium carrying channels oriented parallel to the first wall are used to cool a 60-cm thick stagnant lithium blanket. Parallel studies were carried out wherein the thermal and structural properties of the construction materials were based on those projected for either solution-annealed 316-stainless steel or vanadium-base alloys. The effects of limitations on allowable peak structural temperature, material strength, thermal stress, coolant inlet temperature, and pumping power/thermal power ratio were evaluated. Consequences to thermal hydraulic performance resulting from the presence of or absence of a divertor were also investigated. For the case of lithium cooled modules operated in a reactor without a divertor, the following results were obtained: (a) with stainless steel as construction material and a peak structural temperature of 500°C, the neutron wall loading and coolant exit temperature are limited to $\leq 2 \text{ MW/m}^2$ and $\leq 480^\circ\text{C}$, respectively, by thermal stress criteria, and (b) with vanadium as construction material and a peak structural temperature of 650°C, the neutron wall loading and coolant exit temperature are limited to $\leq 8 \text{ MW/m}^2$ and $\leq 620^\circ\text{C}$, respectively, by a combination of constraints involving the peak structural temperature and the minimum coolant exit temperature. For the same cases in a reactor with a divertor, the maximum allowable neutron wall loading increases by from 40% (for vanadium) to 90% (for stainless steel). For the case of a helium cooled stagnant blanket interfaced with a helium cooled stainless steel first wall assembly, the maximum wall loading is limited by thermal stress criteria to $\leq 2.5 \text{ MW/m}^2$ without a divertor and to $\leq 6.0 \text{ MW/m}^2$ with a divertor.

Introduction

In order to shed some light on the question of optimum power cycle concepts for commercial fusion plants, a series of studies was carried out to evaluate and compare alternative breeding/heat-transfer/power-conversion systems. As a first step in providing a basis for parametric investigations of energy conversion systems for commercial reactors,^{1,2} analyses of first wall/blanket thermal-hydraulic response were performed with the life-limiting properties of the structural materials as the primary constraints. The thermal hydraulic calculations are based on a set of reference design parameters as given in Table 1.

In work completed thus far, two generically similar first wall/blanket concepts have been evaluated. One involves the use of flowing lithium as a heat transfer and breeding medium in a modular blanket cell of the type shown in Fig. 1. The other uses pressurized helium as the heat transfer fluid flowing (in channels) through a "sea" of stagnant liquid lithium as illustrated in Fig. 2. Instead of basing the analyses on arbitrarily selected materials about which little is

Table 1. Reference Design Parameters for Comparisons of Stainless Steel and Vanadium Alloy Systems

Reactor Major Radius	8.0 m
Central Core Radius	3.0 m
First Wall Radius	3.4 m
Radiation and Particle Load Factor	
with divertor	0.10
without divertor	0.25
Blanket Thickness	0.6 m
Shield Thickness	0.9 m
Magnet Thickness	0.52 m
Basic Design	Modular

Figure 1. Simplified Model of the Lithium Blanket Cell Used in the Thermal Hydraulic Analyses.

Figure 2. Simplified Model of the Helium Cooled Stagnant Lithium Blanket Used in the Thermal Hydraulic Analyses.

* Work supported by the Department of Energy.

known regarding (a) behavior in the anticipated harsh radiation environment and (b) compatibility with the proposed coolants, solution-annealed Type-316 stainless steel and vanadium-base alloys were selected for initial study as there appeared to be relatively more data available to define life-limiting properties³ under currently conceived fusion reactor operating conditions.

Development of the Computational Algorithms

Mathematical models that described the thermal hydraulic response of the two blanket concepts illustrated in Figs. 1 and 2 were formulated and programmed into a computer code capable of performing steady-state energy balance calculations. The details of these mathematical models and descriptions of the computer code (BLANKET) have been presented in other reports^{2,4} and, hence, will only be summarized briefly herein.

Flowing Lithium Cooled First Wall/Blanket Concept

The use of an electrically conducting fluid, like lithium, in the presence of large magnetic fields gives rise to a number of adverse fluid dynamic effects of which the most serious is the pressure loss due to magnetohydrodynamic (MHD) interactions. In calculating the total pressure drop for the blanket cell concept in Fig. 1, the coolant flow path was divided into four regions^{2,4}: (a) inlet to blanket, (b) return through the blanket cell annulus, (c) inlet and exit through shield, and (d) inlet and exit through magnets. (A schematic representation of the pressure drop terms is shown in Fig. 3.) As the magnetic field is not constant across the blanket, the shield, and the magnet, the magnet field strength, B , was integrated over each of the regions inside the toroidal field coil to obtain a properly averaged value of the gross MHD effect. The pressure loss in each region was then determined^{2,4} using the corresponding average value of B^2 for that region. Because the actual lengths of the coolant flow paths, the sizes of the inlet and outlet headers, and the locations of bends, fittings, etc. for a commercial reactor are subject to considerable uncertainty, a safety factor of two was employed in calculating each pressure drop. The thicknesses of the inlet piping to the blanket cell, the cell wall itself, and the manifolds were calculated based on the stress requirements corresponding to the prevailing operating conditions (temperature and pressure) with the assumptions that (a) the pressure stress cannot exceed one-third the yield strength³ and (b) the wall thicknesses cannot be less than 2.5 mm in the blanket region and 5 mm in the shield and magnet regions due to fabrication constraints.

The heat transfer calculations for the flowing lithium concept (Fig. 1) were performed using standard empirical equations.^{2,4} A one-dimensional steady-state analysis was carried out by dividing the blanket cell into (a) inlet piping, (b) first wall, (c) cell annulus and (d) cell side walls (see Fig. 1). These calculations were used to set the required coolant velocity for a given wall loading and to establish the general thermal characteristics of the system. The overall heat balance was determined based on a given wall loading due to (a) particles and radiation that create a first-wall surface flux and (b) neutrons that create internally generated heat. As the particle and surface radiation power fraction depends on whether the reactor has a divertor or not, two cases were considered -- the particle and surface radiation loading factor was set equal to 25% of the neutron wall loading without divertor and 10% of the wall loading with divertor.

$$P_{\max} = \Delta P_{MI} + \Delta P_{SI} + \Delta P_{BI} + \Delta P_{BO} + \Delta P_{SO} + \Delta P_{MO} + \Delta P_{ext} + P_{\min}$$

$$P_{SC} = \Delta P_{BO} + \Delta P_{SO} + \Delta P_{MO} + \Delta P_{ext} + P_{\min}$$

Figure 3. Pressure Drop Diagram for the Lithium Cooled Blanket Cells.

Helium Cooled Stagnant Lithium First Wall/Blanket Concept

The thermal hydraulic analyses of a helium cooled tubular first wall coupled to a stagnant lithium blanket were based on the configuration shown in Fig. 2. The first wall is assumed to be made of a single row of tightly packed tubes and the blanket is in essence a "sea" of lithium extending radially outward from the back of the first wall and having an appropriately dispersed array of helium carrying tubes. A realistic boundary condition is set up so that there is no discontinuity in the thermal hydraulic analyses at the first wall/blanket interface. The radiation power deposited on the first wall and the nuclear heat generated within zone 1 (see Fig. 2) are removed by the helium flowing through the first wall tube bundle. Zone 2 (blanket) is divided into several regions and the number and size of the coolant tubes throughout these regions are calculated based on the internal heat generation rate so that the coolant exit temperatures for all regions are the same. Since the wall loadings (both radiation and neutron) were assumed to be constant along the coolant flow direction, the temperature difference between the wall and the coolant remains essentially constant, and the maximum wall temperature can be based on the coolant exit temperature. (Internal heat generation in the helium was assumed to be zero.) The thermal hydraulic calculations are carried out for each zone separately, with the assumption of an isothermal boundary at the zone 1/zone 2 interface. The criteria for materials property limitations were the same as those described above for the lithium cooled blanket concept.

Results and Discussion

Thermal Hydraulic Analyses of the Flowing Lithium Cooled First Wall/Blanket Concept

Typical results of thermal hydraulic analyses for four reference cases (stainless steel with and without a divertor and vanadium alloy with and without a divertor) are summarized in Table 2 and are presented in greater detail in references 2 and 4. For stainless steel (with or without a divertor) the maximum neutron wall loading generally occurs at relatively low wall thicknesses (i.e. ≤ 2.5 mm) and is limited by the thermal stress criterion (maximum thermal stress = material yield strength). For vanadium alloys this maximum occurs at somewhat larger wall thickness values

Table 2. Results of Wall Loading Trade Studies for Lithium-Cooled Stainless Steel and Vanadium-Alloy Blanket Systems^a

Criteria	Li-Cooled V Alloy	Li-Cooled SS
Maximum Allowable Temp., °C	650	500
Maximum Thermal Stress, MPa	351	117
Minimum Wall Thickness, mm	2.5	2.5
Maximum Wall Thickness, mm	8.0	8.0
Minimum Coolant Inlet Temp., °C	235	235
Coolant Temperature Rise, °C	325	225
Maximum Neutron Wall Loading, MW/m ²		
$F^b = 0.10$	8 Tesla 11.2 ^c 10 Tesla 7.5 ^a 12 Tesla 5.3 ^e	4.2 ^d 3.5 ^d 3.0 ^d
$F^b = 0.25$	8 Tesla 8.0 ^c 10 Tesla 6.2 ^c 12 Tesla 4.6 ^c	2.0 ^d 2.0 ^d 1.8 ^d

- For a cell diameter of 0.5 m and an inlet tube diameter of 0.1 m.
- F = Fractional power loading on first wall surface due to particle and radiation effects.
- Limited by minimum coolant inlet temperature criterion.
- Limited by maximum thermal stress criterion.
- Limited by MHD pressure stress criterion (i.e. maximum cell wall thickness insufficient to support higher coolant velocity).

(3 to 6 mm) and is limited by the minimum coolant inlet temperature criterion. The values of the maximum allowable neutron wall loading for 8, 10, and 12 T maximum toroidal fields are listed in the lower half of Table 2. The total pressure drops were nominally in the range from 3 to 6 MPa (including the safety factor of 2.0 on all calculated pressure losses).

The effect of maximum toroidal field and cell wall thickness on the maximum allowable neutron wall loading is illustrated more clearly in Figs. 4 and 5 for the four cases described above. As can be seen in Figs. 4 and 5 and in Table 2, there is some advantage (in terms of overall thermal hydraulic performance) to having the lower toroidal fields. For most cases, the actual advantage is in fact even greater than is indicated in Table 2, because the increased cell wall thicknesses required to support the maximum neutron wall loading at the higher toroidal fields have the concomitant effect of reducing the coolant exit temperature.

Early trade-off studies on the coolant temperature rise showed that the values in Table 2 (i.e. 225°C for stainless steel and 325°C for vanadium alloy) are near to optimum for each material based on the thermal-hydraulic model and maximum structural temperature limits used herein. Also, the pumping power to thermal power ratio (based on geometrically averaged values of the actual magnetic field strength inside the reactor) was found to be < 1.0, < 1.5 and < 2.0% for maximum toroidal fields of 8, 10 and 12 T, respectively.

Figure 4. Effect of Cell Wall Thickness and Maximum Toroidal Field on Maximum Allowable Neutron Wall Loading for the Lithium Cooled Stainless Steel Blanket Cell.

Figure 5. Effect of Cell Wall Thickness and Maximum Toroidal Field on Maximum Allowable Neutron Wall Loading for the Lithium Cooled Vanadium Alloy Blanket Cell.

Several cases were analyzed for effect of varying the size of the inlet piping to the blanket module while keeping the blanket cell size constant (e.g. cell diameter = 0.5 m). The thermal hydraulic results were found to be rather insensitive to small changes in inlet tube diameter since only the pressure drop from the inlet tube back is affected by this parameter. It can be easily shown that the size of the blanket cell cannot be varied over a wide range, although a cell diameter of 0.5 m is not necessarily the optimum. Increasing the cell diameter in order to reduce the number of modules required leads to larger cell wall thicknesses with resultant higher thermal stresses and lower coolant exit temperatures. A reduced cell size has the beneficial effect of lower wall thickness and lower thermal stresses, however, it increases the number of cells required for a given geometry. It may be noted that for the reactor size selected for this analysis, more than 5000 of the 0.5-m diameter cells would be needed. Since this is already a very

large number, there is little incentive to go to a larger number of smaller modular units.

For stainless steel, which is usually found to be thermal stress limited, doubling the thermal stress limit tends to double the maximum allowable neutron wall loading and raising the upper operating temperature limit by 100°C increases the coolant exit temperature by an approximately equal increment. Increasing the upper operating temperature for vanadium by 100°C increases the maximum allowable wall loading but not the coolant exit temperature. However, for a given wall loading, it is always possible to achieve a higher coolant exit temperature with a higher upper operating temperature value.

Data of the type presented in references 2 and 4 have permitted the derivation of a series of empirical equations which relate the coolant exit temperature, $T(CE)$, to maximum material temperature, $T(MAX)$, neutron wall loading Q_N , and cell wall thickness, t for each of the material and divertor options investigated. For stainless steel

$$T(CE) = T(MAX) - t [1.2 Q_N + 0.5] \text{ with divertor}$$

and

$$T(CE) = T(MAX) - t \left[\frac{T(MAX) - 400}{100} \right] [1.3 Q_N + 0.25] \text{ without a divertor.}$$

For vanadium

$$T(CE) = T(MAX) - t \left[\frac{T(MAX) - 400}{100} \right] [2.0 Q_N - 3.8] \text{ with divertor and}$$

$$T(CE) = T(MAX) - t \left[\frac{T(MAX) - 400}{100} \right] [2.0 Q_N - 3.8] \text{ without divertor.}$$

Only in the case of stainless steel with a divertor present did the value of $T(MAX) - T(CE)$ show negligible dependence on the value of $T(MAX)$. In the other three cases the empirically derived function $[T(MAX) - 400]/100$ had to be employed to reconcile the calculated $T(CE)$ values.

Thermal Hydraulic Analyses of the Helium Cooled Stagnant Lithium First Wall/Blanket Concept

One of the primary objectives of the thermal hydraulic analysis for the helium cooled stagnant lithium concept was to make the coolant operating parameters and characteristics reasonably consistent with existing HTGR experience.⁵ Table 3 contains a summary of results with stainless steel as construction material and with the following sets of conditions: (a) the presence of or absence of a divertor, (b) maximum allowable structural temperatures of 500° or 650°C, (c) thermal stress limits of 17 or 34 ksi, and (d) heat transfer coefficient and friction factor multipliers of 2.2 and 4.0, respectively.⁵ The asterisked parameters in Table 3 were fixed in the calculation and the primary iteration was done by increasing the neutron wall loading until the thermal stress limit at the first wall was exceeded.

In the thermal hydraulic analysis of the first wall, the coolant velocity is fixed at 200 ft/s, the tube diameter is fixed at 25.4 mm (1 inch), and the tube wall thickness is not allowed to be less than 1.25 mm (50 mil). In the thermal hydraulic analysis of the blanket, the coolant void fraction, VF , is fixed at 5% and the temperature drop through the lithium $\Delta T(Li)$, is set at 25°C. Trade studies on the latter

two parameters have been completed for $VF = 5, 7$, and 10% and $\Delta T(Li) = 25, 50$, and 100°C. These studies show that, in terms of maximum coolant exit temperature, $VF = 5\%$ and $\Delta T(Li) = 25^\circ C$ are closer to optimum than the other values tested. Other parameters that were fixed in these analyses are given in Table 3. The coolant exit temperature, from the first wall region, $T(CE)$, may be represented empirically in terms of the neutron wall loading, Q_N , and the maximum allowable structural temperature, $T(MAX)$, by

$$T(CE) = T(MAX) - 17.9 \cdot Q_N - 21.9 \text{ (with divertor)}$$

$$T(CE) = T(MAX) - 41.3 \cdot Q_N - 21.4 \text{ (without divertor).}$$

The coolant exit temperature from the blanket region is nominally 30 to 50°C below the maximum structural temperature and seems to be relatively independent of wall loading or divertor status.

The values arrived at in Table 3 are based largely on attempts to control thermal stress and peak structural temperature, and as a result do not reflect optimum performance from the standpoint of coolant exit temperature. In particular, the cases where the coolant exit temperature from the first wall or the blanket is < 500°C probably do not represent economically attractive systems. Figures 6 and 7 show how these coolant exit temperatures are affected by backing down from the maximum neutron wall loading values given in Table 3 for the 500°C and 650°C stainless steel operating temperatures. Based on these results the helium cooled stagnant lithium blanket would appear to achieve economic attractiveness at wall loading values considerably lower than those in Table 3 and at structural temperatures $\geq 600^\circ C$.

The principal indications from this study are that (a) $T(MAX) \geq 600^\circ C$ is essential for useful $T(CE)$ with helium, i.e., $T(CE) > 500^\circ C$; (b) without a divertor Q_N is limited to 2 to 5 MW/m², depending on allowable thermal stress level (ignoring, of course, the effects of fatigue and crack propagation for the time being); (c) in principal, considerably higher wall loadings can be achieved with a reasonably effective divertor; (d) fabrication of the first wall out of 50-mil tubing is assumed to be feasible (if the limit were 100 mil, the allowable P_u values would drop by a factor of 2); and (e) the number of tubes and gross tube weights for a reactor with a first wall area of $\sim 1000 \text{ m}^2$ are extremely large.

Application of the multiplier factors for the heat transfer coefficient and frictional pressure drop is based on the assumption that the interior tube walls can be roughened or otherwise modified to augment heat transfer with a resulting modest impact on the pressure drop. Omission of these multiplier factors (i.e., equating them to unity) generally results in substantial increases in coolant velocity (to > 400 ft/s) and channel length (70 to 80%) with somewhat smaller reductions in heat transfer coefficient ($\sim 30\%$) and coolant exit temperature (4 to 5%).

Conclusions

Because studies of the type described in this report tend to "idealize" the complicated geometry and widely-ranging operating conditions of potentially attractive tokamak-type fusion reactors, any conclusions drawn from them should be interpreted carefully and not arbitrarily extrapolated to fusion power systems in general. The key conclusions from the study of the liquid lithium cooled first wall/blanket concept may be summarily stated as follows: (a) The maximum allowable neutron wall loading for a given reactor design

Table 3. Trade Study for Helium Cooled Stagnant Lithium Blanket Using Transverse Cooling Channels Made of Stainless Steel: First Wall Configuration is a Single Layer of Closed-Packed Tubes.
(* Indicates fixed or independent parameter)

<u>Divertor Option</u>	<u>With Divertor</u>				<u>Without Divertor</u>			
<u>General Parameters</u>								
* Maximum Structural Temp., °C [T(MAX)]	500		650		500		650	
* Helium Inlet Pressure, ksi	1.0		1.0		1.0		1.0	
* Pump Power/Thermal Power, %	5		5		5		5	
* Coolant ΔT, °C	200		300		200		300	
* Lithium ΔT, °C [ΔT(Li)]	25		25		25		25	
Neutron Wall Loading, MW/m ²	6.2	12.4	6.0	12.0	2.6	5.3	2.6	5.1
<u>First Wall Parameters</u>								
* Helium Tube Dia., mm	25.4		25.4		25.4		25.4	
* Helium Velocity, ft/s	200		200		200		200	
* Thermal Stress, ksi	17	34	17	34	17	34	17	34
Wall Thickness, mm	1.25		1.25		1.25		1.25	
Channel Length, m	12.2	13.2	17.3	18.5	12.2	13.1	17.3	19.4
HT. TR. Coeff., BTU/hr.ft. ² *F	1693	1980	1510	1729	1679	1968	1507	1720
Coolant Inlet Temperature, °C	171	61	217	105	173	67	220	110
Coolant Exit Temperature, °C [T(CE)]	371	263	517	405	373	267	520	410
Wall ΔT, °C	53	106	30	100	53	107	50	100
No. of Tubes, Hundreds ^a	32	29	22	21	32	30	22	21
Total Tube Weight, Tons ^a	35		35		35		35	
<u>Blanket Parameters</u>								
* Void Fraction, % [VF]	5		5		5		5	
Tube Diameter, mm	4.9	3.4	5.2	3.6	7.7	5.4	8.2	5.8
Wall Thickness, mm	0.41	0.37	0.42	0.37	0.48	0.43	0.49	0.43
Channel Length, m	1.1	0.6	1.5	0.8	2.3	1.3	3.3	1.8
HT. TR. Coeff. BTU/hr.ft. ² *F	2541	2981	2361	2769	2068	2427	1924	2253
Helium Velocity, ft/s	251	276	263	290	220	244	231	255
Coolant Inlet Temperature, °C	259	249	305	295	268	261	316	308
Coolant Exit Temperature, °C	459	449	605	595	468	461	616	608
Wall ΔT, °C	8.0	11.1	8.0	10.0	6.0	8.0	6.0	8.0
Thermal Stress ksi	2.7	3.4	2.6	3.4	2.0	2.5	1.9	2.5
No. of Tubes, Thousands ^a	939	3435	585	2149	174	644	109	398
Total Tube Weight, Tons ^a	74	96	71	91	54	69	52	66

^a Based on a reactor with a first wall area of ~1000 m²

$$T(CE) = T(MAX) - 17.9 P_w - 21.9$$

$$T(CE) = T(MAX) - 41.3 P_w - 21.4$$

Figure 6. Coolant Exit Temperature versus Wall Loading for the Helium Cooled Stagnant Lithium Blanket (maximum structural temperature = 500°C).

Figure 7. Coolant Exit Temperature versus Wall Loading for the Helium Cooled Stagnant Lithium Blanket (maximum structural temperature = 650°C).

can be increased from 40 to ~ 90% by the addition of a divertor. The magnitude of the increase depends primarily on which criteria set the maximum wall loading, e.g., increases tend to be greatest in thermal stress limited systems. (b) With respect to overall thermal hydraulic performance, austenitic alloys will tend to be thermal stress limited, whereas vanadium-base alloys will be limited by the pinch between maximum allowable structural temperature and minimum coolant inlet temperature. (c) Significantly higher neutron wall loadings appear to be achievable with vanadium alloys than with solution annealed 316-SS. (d) Ignoring the application of advanced power conversion cycles, there is little incentive for structural temperatures in excess of 650°C, since the associated coolant (lithium) exit temperatures ($\geq 550^\circ\text{C}$) are more than adequate to drive optimized steam turbine cycles. (e) Overall design and performance objectives for lithium cooled reactors will be more easily met at lower toroidal fields (8 to 9 T). Fields as high as 12 T may preclude the use of circulating lithium in all but the most outboard regions of the blanket.

For the case of the helium cooled stagnant lithium first wall/blanket concept, the key conclusions are as follows: (a) A maximum allowable structural temperature $\geq 600^\circ\text{C}$ will be necessary to assure the attainment of attractive operating conditions (i.e., reasonable thermodynamic efficiency) for the helium coolant. (b) As in the case of the lithium cooled first wall/blanket concept, the presence of a divertor increases the allowable wall loading by nearly a factor of two in thermal stress limited systems. (c) The transverse cooling arrangement employed in the helium cooled concept (Fig. 2) requires an enormous number of individual tubes and is probably less attractive (from the standpoint of fabricability and overall thermal hydraulic performance) than the radially cooled helium blanket concept described by Kearney, et al.⁶. (d) There appears to be some incentive to provide for augmentation of the heat transfer coefficient in helium cooled reactors, even though this will undoubtedly be accompanied by increased pressure losses.

Finally, it must be noted that the studies described herein consider only steady-state performance of the first wall/blanket system, and as a result the derived operating conditions (wall loadings, coolant exit temperatures, etc.) represent upper limits to the performance of an actual system. In the long run, the effects of thermal strain and cyclic fatigue will act to reduce the allowable wall loading and in all likelihood, the peak allowable structural temperature as well. This contention is supported by the more recent work of Majumdar and Misra^{7,8} on the fatigue life modeling of the lithium-cooled module in Figure 1.

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THERMAL RESPONSES OF TOKAMAK REACTOR FIRST WALLS DURING CYCLIC PLASMA BURNS

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Summary

The CINDA-3G computer code has been adapted to analyze the thermal responses and operating limitations of two fusion reactor first-wall concepts under normal cyclic operation. A component of an LMFBF computer code has been modified and adapted to analyze the ablative behavior of first-walls after a plasma disruption. The first-wall design concepts considered are a forced-circulation water-cooled stainless steel panel with and without a monolithic graphite liner. The thermal gradients in the metal wall and liner have been determined for several burn-cycle scenarios and the extent of surface ablation that results from a plasma disruption has been determined for stainless steel and graphite first surfaces.

Introduction

In addition to extensive radiation damage and bulk heating from high-energy neutrons, the first wall of a tokamak fusion reactor will be subjected to high surface heat loads that may affect its integrity. For a tokamak reactor operating without a divertor, a heat flux equivalent to $\sim 25\%$ of the neutron wall loading will be deposited in the surface regions of the first wall during a D-T plasma burn. Under normal operation, approximately 60% of this surface heat flux will be produced by particle transport from the plasma with the remaining 40% coming from electromagnetic radiation, primarily bremsstrahlung. The absorption and removal of these large surface heat fluxes pose a major problem in the design of a high-integrity first wall. In the event of a plasma disruption, essentially all of the thermal energy in the plasma and a fraction (depending on the electrical conductivity of the wall) of the stored magnetic energy in the plasma will be deposited in the surface regions of the first wall. Because of the inherent difficulties in repairing or replacing the first wall, it seems imperative that the first wall be designed to withstand a moderate number of disruptive events.

In the present investigation two models have been developed and existing computer codes have been adapted and used to evaluate the thermal responses and operating limitations of proposed first-wall concepts under normal (cyclic) and off-normal (plasma disruption) operation. The first-wall concepts considered include a forced-circulation-cooled stainless steel wall and a radiatively-cooled graphite liner. Appropriate materials property data used for the calculations have been obtained from the literature. The plasma performance and burn-cycle times considered are believed to be attainable in near-term experimental power reactors that operate without a divertor.^{1,2} Thermally induced stresses in the first wall are a major concern during normal operation, whereas the extent of ablation of the first surface is the factor assessed for off-normal operation. The results obtained permit an evaluation of the relative merits of forced-circulation-cooled and radiatively cooled first walls.

Model Development

Two models have been developed to analyze the thermal responses of proposed fusion reactor first-wall design concepts. One model considers the thermal responses of the first walls during cyclic plasma burns of ~ 60 s. The second model considers the ablation of the wall material in the event of a plasma disruption. First wall concepts of the type proposed for near-term experimental power reactors that operate without a divertor have been analyzed.¹⁻³

First-Wall Geometry

Figure 1 is a schematic diagram of the forced-circulation-cooled metal first wall analyzed in the present investigation. This concept is a general case of the Argonne TEPR vacuum wall design.² It consists of a relatively thin (~ 2 cm) metal slab with internal channels through which a coolant is circulated to remove the heat deposited in and on the wall. The first-wall may or may not also serve as the vacuum wall (the vacuum wall and coolant panel are of similar construction in the Argonne TEPR). In either case it is assumed that the metal wall provides some structural support and serves as a containment for the coolant. The coolant channels in this design are parallel to the front surface. Based on earlier results,^{3,4} the coolant channels are located near the plasma-side surface and are closely spaced to minimize thermal gradients in the wall. The modular units of the metal wall are 0.8-cm wide with the coolant channels covering slightly less than 50% of the equivalent front surface. The irregular shape of the front face of the metal wall, which simulates the panel coil geometry proposed for the first wall, produces only a minor variation from a smooth wall.

A schematic diagram of the radiatively-cooled graphite liner is shown in Fig. 2. The liner is 1 cm thick and completely shields the structural wall from the plasma. The forced-circulation-cooled structural wall in this design is identical to the stainless steel wall described previously. Details of the liner support and the perturbations on the thermal gradients caused by the attachment of the liner to the structural wall have not been considered in the present investigation.

Model for Cyclic Plasma Burn

The CINDA-3G computer code⁵ has been adapted to calculate the thermal transients in the first wall during the cyclic plasma burn. CINDA-3G is a general-purpose three-dimensional code that solves the diffusion equation by finite-difference techniques. It is extremely flexible in that the user has available more than 100 built-in subroutines to accomplish a great variety of calculations. The present model incorporates (a) variable surface and bulk energy deposition in the wall, (b) the heat capacity of the wall material, (c) the heat conductance of the wall material, (d) thermal radiation for the case of the graphite liner, and (e) heat transport to the water coolant into the computer code to calculate the thermal response of the first wall.

* Work supported by the Department of Energy.

Normal operation for the present analysis is a cyclic burn based on the calculated plasma performance for the Argonne TEPR.^{1,6} The reference burn cycle consists of a 65-s plasma burn followed by a 15-s off-time for evacuation of the plasma chamber. Figure 3 shows a simplified approximation of the power response that was derived from a complex power curve developed by Brooks.⁶ The neutron power curve is represented by a ramp increase to a maximum wall loading of 1.6 MW/m^2 after 6 s, a ramp decrease to 0.75 MW/m^2 after 52 s, and a sharp ramp decrease to zero power at the end of the 65-s burn. This curve corresponds to an average neutron wall loading of 1 MW/m^2 for the duration of the burn. The total neutron power of $\sim 400 \text{ MW}$ is indicated on the right-hand axis. The radiation and transport power is taken as 25% of the instantaneous neutron power.

The neutron energy is assumed to be deposited throughout the stainless steel wall giving a bulk heating rate that scales directly as the neutron wall loading. The bulk heating rate corresponding to a 1 MW/m^2 neutron wall loading is 10 W/cm^3 for stainless steel. The neutron flux, and hence the energy deposition, is attenuated exponentially through the stainless steel wall according to the results of Abdou.⁷ The neutron heating in stainless steel is reduced to about 80% at the backside of the 2-cm wall. For the case of the graphite liner, a uniform bulk heating rate of 7 W/cm^3 is used for a 1 MW/m^2 neutron wall loading.⁷ No attenuation of the neutron flux is assumed for the thin graphite liner.

In the present analysis it is assumed that all of the radiation and transport power is deposited on the surface of the first wall. This is considered to be a good approximation for the high-Z (atomic number) steel wall; however, the bremsstrahlung radiation from a high temperature plasma will have significant penetration (hundreds of micrometers) in low-Z materials such as graphite. Although this is not expected to be a major effect in the relatively thick monolithic graphite liner, it may have important implications for thin low-Z coatings such as beryllium.^{2,4}

For the case of the forced-circulation-cooled metal wall the heat is simply transferred to the water coolant and transported out of the wall. For the case of the graphite liner, the energy deposited in the liner is thermally radiated to the metal structural wall and the heat again removed by the water coolant.

Model for Plasma Disruption

An existing computer code, which was developed as a component of a larger Liquid Metal Fast Breeder Reactor (LMFBR) accident analysis code,⁸ was appropriately modified and used to determine the extent of ablation of the first wall that will occur after a plasma disruption. This one-dimensional code treats the innermost, i.e., plasma side, region of the first wall as an ablative region. The thickness of this region is chosen such that some material remains under all conditions considered. The ablative region is subdivided into a desired number of subregions whereas the remainder of the wall is subdivided into two larger, equally-sized regions. Although one can account for heat loss from the wall, the time scale for the plasma dump is so short that, for practical purposes, a heat sink is ineffective. The majority of the energy deposited is accounted for by the heat capacity and the heat of vaporization (or sublimation) of the ablated subregions. Calculations have been made for both a stainless steel and a graphite first wall. Because the ablated regions are very thin (typically tens of

micrometers) and the response time is very short (milliseconds), transport of any metal in the liquid form is not considered for the steel. The thin films are assumed to vaporize or resolidify in very short times. Sublimation of the graphite wall is assumed.

Materials Property Data

The materials property data required for the calculation are summarized in Table I for stainless steel and graphite. Average handbook values are used for the densities. The thermal conductivity for stainless steel was taken from the temperature dependent equation for unirradiated material.⁹ The thermal conductivity for graphite is sensitive to the type of graphite and to neutron radiation.¹⁰⁻¹² Data for irradiated graphite at elevated temperature ($> 1000^\circ\text{C}$) range from 4 to $40 \text{ W/m}\cdot^\circ\text{K}$. A mean value of $10 \text{ W/m}\cdot^\circ\text{K}$ has been used for the reference case. This value, which is representative of high grade graphite with 5-10% porosity, is used to account for effects expected from high helium generation. The specific heats given in the table are handbook values for the appropriate temperatures, viz., $\sim 500^\circ\text{C}$ for stainless steel and $\sim 1000^\circ\text{C}$ for graphite. The heats of vaporization (sublimation) are taken from thermodynamic tables and the emissivity of 0.8 is typical of many materials. Bulk neutron heating rates have been obtained from work of Abdou.^{7,13}

Thermal Response of First Wall

The thermal responses of the stainless steel wall and the graphite liner are determined for several burn-cycle scenarios and the extent of surface ablation that results from a plasma disruption is determined for stainless steel and graphite first surfaces.

Response During Cyclic Plasma Burn

Energy deposition rates derived from Fig. 3 and the materials property data from Table I are incorporated into the CINDA-3G computer code to determine the thermal responses of the first walls for cyclic plasma burns. In the present investigation a plane that intersects the coolant channel has been analyzed. This plane is represented by the node points 38, 39, 41, 42, 57 and 60 in Figs. 1 and 2. Node 38 is on the plasma-side surface of the steel wall with nodes 39, 41, and 42 located in the steel wall at the positions indicated. Node 60 is on the plasma-side surface of the graphite liner and node 57 is on the back-side surface.

Figures 4-6 are plots of the thermal history for the four nodes in the steel wall during one burn cycle. Figure 4 represents the reference case with a 1 MW/m^2 neutron wall loading and an 80-s cycle time. Figure 5 shows the effect of increasing the off-cycle time from 15 s for the reference case to 30 s. Figure 6 is for the reference burn cycle but with a 2 MW/m^2 neutron wall loading. In all cases the coolant water temperature is $\sim 317^\circ\text{C}$. Table II summarizes some of the critical temperatures and ΔT 's in the stainless steel wall. Although a stress analysis has not yet been conducted, a qualitative assessment of the thermal stress problem for the various burn-cycle scenarios can be obtained by comparing the variations in the temperature differences between nodes 38 and 39 and those between nodes 42 and 41. The longer off-cycle has very little effect on these temperature variations whereas the higher wall loading (2 MW/m^2) substantially increases the variations in the thermal gradients.

Figures 7-9 are plots of the thermal history for nodes 57 and 60 in the graphite liner during one burn cycle. The thermal response of the liner for the

reference burn cycle (1 MW/m^2 , 80-s cycle) and mean thermal conductivity for graphite ($10 \text{ W/m}\cdot\text{K}$) is shown in Fig. 7. The effect of extending the off-time to 30 s (95-s cycle) is also shown in Fig. 7. Figure 8 shows the effect of variations in the thermal conductivity of graphite, and Fig. 9 shows the effect of increasing the wall loading to 2 MW/m^2 . Critical temperatures and temperature differences obtained from this set of curves are summarized in Table III. The longer off-time slightly reduces the plasma-side surface temperature of liner and the ΔT through the liner. The thermal response of the liner is strongly dependent on the thermal conductivity of the graphite for the range of values considered. For the low conductivity ($4 \text{ W/m}\cdot\text{K}$), the maximum surface temperature approaches 1880°C , which is probably unacceptable because of excessive thermal vaporization, and the maximum ΔT through the wall is nearly 600°C . This low value of thermal conductivity may be reached for highly irradiated graphite with its inherently high helium generation rates. The results are substantially different when the high thermal conductivity is used. Calculations for the 2 MW/m^2 neutron wall loading also give high values for the maximum surface temperature (2123°C) and the ΔT through the wall (516°C). This result indicates that the maximum acceptable neutron wall loading for a radiatively cooled graphite liner is substantially less than 2 MW/m^2 .

Figure 10 shows a comparison of the thermal responses for the surface regions of the forced-circulation-cooled stainless steel wall with and without the graphite liner. Both the maximum temperature of the steel surface and the maximum thermal gradient in the surface regions of the steel wall are significantly decreased by the liner. This indicates that the thermal fatigue problem associated with a stainless steel first wall² would be reduced if a radiatively cooled liner is used. However, the lifetime of the graphite liner relative to that of the steel wall has not been evaluated for the conditions indicated.

Response after Plasma Disruption

The appropriate materials parameters have been incorporated into the plasma dump computer code to calculate the extent of ablation of the first wall in event of a plasma disruption. In the present calculation both the plasma thermal energy and the stored magnetic energy in the plasma, which are about equal, are deposited on the surface of the first wall during the dump. The time required for the plasma dump to occur and the wall area upon which the energy is deposited are not well known and probably vary with types of plasma instability. Therefore, the extent of ablation of the stainless steel and graphite walls has been calculated parametrically in terms of the dump time and the effective wall area over which the energy is deposited. Calculations have been carried out for an energy dump from a plasma that yields a 1 MW/m^2 neutron wall loading. Thicknesses of the ablated regions as functions of the two parameters are shown in Figs. 11 and 12 for stainless steel and graphite, respectively. An instantaneous dump, i.e., zero dump time, does not allow for heat removal from the ablated region and, therefore, gives a maximum ablation thickness for a particular fractional wall area. For dump times up to 0.1 ms, the thickness of the ablated regions do not differ appreciably from the instantaneous dump curves. However, the 10 ms curve shows a substantial decrease in the amount of ablated material.

The fractional area of the first wall over which the plasma energy is deposited is critical. It is believed that the energy will be deposited over a major fraction ($> 50\%$) of the wall area. For the reference

case, i.e., 1.0 ms dump time, less than 50 μm of the steel wall will be ablated if the plasma energy is deposited over at least 50% of the wall. It is assumed that the deposition time is short enough and the depth of the affected region is so small that only vaporization of the steel is important. Significant transport of metal in the liquid phase is not considered for the thin regions and short dump times of interest. The ablation of graphite is generally less than half that of stainless steel for similar conditions of dump time and effective wall area. If the plasma energy is deposited over at least 50% of the wall, the maximum ablated region is less than 30 μm for graphite.

Since all of the stored magnetic energy will probably not be deposited on the surface of the first wall, the curves in Figs. 11 and 12 are conservative. Even so, results of these calculations indicate that first walls of stainless steel or graphite should withstand a moderate number of plasma disruptions if the plasma energy is effectively distributed over more than 25% of the reactor wall. This is true even if no credit is taken for redeposition of the ablated material, which is expected to occur. Also, an estimate of the capability of other first-wall materials to withstand plasma disruptions can be obtained by inserting relevant property data into the models.

Conclusions

The CINDA-3C computer code⁵ has been adapted to analyze the thermal responses and operating limitations of two fusion reactor first-wall concepts under normal cyclic plasma burns. The first-wall design concepts considered are a forced-circulation water-cooled stainless steel panel with and without a graphite liner. For a reference burn cycle with an average neutron wall loading of 1 MW/m^2 and a coolant temperature of 317°C , the maximum surface temperature in the steel wall was 402°C and the maximum ΔT in the first 0.175 cm of the wall was 48°C . These values increased to 479°C and 87°C , respectively, for a 2 MW/m^2 wall loading. For the reference case the maximum surface temperature of the graphite liner was 1543°C and the maximum ΔT through the 1-cm liner was 236°C . However, these values change substantially when the thermal conductivity of graphite is varied over the range of uncertainty, viz., $4\text{--}40 \text{ W/m}\cdot\text{K}$ for irradiated graphite at $> 1000^\circ\text{C}$. The surface temperature of the graphite becomes unacceptably high ($> 2000^\circ\text{K}$) if the lower value of thermal conductivity is used or if the neutron wall loading is increased to 2 MW/m^2 with an average value of $10 \text{ W/m}\cdot\text{K}$ for the conductivity of graphite.

A component of an LMFBR computer code⁸ has been modified and adapted to analyze the ablative behavior of stainless steel and graphite first walls subjected to a plasma disruption. The thicknesses of ablated regions were determined parametrically for the two wall materials in terms of the plasma dump time and the fraction of the wall area over which the energy was deposited. If the plasma thermal energy and stored magnetic energy are deposited over more than 50% of the wall, the maximum ablated region is $< 70 \mu\text{m}$ for stainless steel and $< 30 \mu\text{m}$ for graphite. Since it is expected that the plasma energy will be deposited over a major fraction ($> 50\%$) of the first wall during a disruption, it is concluded that both wall materials should withstand a moderate number of disruptive events.

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Table I. Materials Properties for Stainless Steel and Graphite

Property	Stainless Steel	Graphite
Density, g/cm ³	7.9	2.0
Thermal Conductivity, w/m·K	20 ^a	4,10,40
Specific Heat, cal/g·°K	0.12 ^a	0.45
Heat of Vaporization, Kcal/mole	85	171 ^b
Vaporization Temperature, °K	1700	2500
Emissivity		0.8
Neutron Heating, w/cm ³	10 ^c	7 ^c

^a Average value. See Ref. 9 for temperature dependent value used.

^b Heat of sublimation for graphite.

^c Surface heating rate for 1 MW/m² neutron wall loading

Table II. Thermal Responses of Stainless Steel Wall with No Liner

Wall Loading, MW/m ² Cycle Time, s	1 80	1 95	2 80
T _{max} (38), °C*	402	398	479
T _{min} (38), °C	321	319	322
ΔT _{max} (38-39), °C	48	45	87
ΔT _{min} (38-39), °C	2	1	4
T _{max} (42), °C	428	421	537
T _{min} (42), °C	386	365	454
ΔT _{max} (42-41), °C	39	37	74
ΔT _{min} (42-41), °C	23	16	43

* Numbers in parenthesis refer to nodes.

Table III. Thermal Responses of Graphite for Different Burn-Cycle Scenarios

Wall Loading, , MW/m ² Cycle Time, s K (graphite), W/m·°K	1 80 10	1 95 10	1 80 4	1 80 40	2 80 10
T _{max} (60), °C*	1545	1478	1877	1371	2123
T _{min} (60), °C	1334	1211	1631	1202	1688
ΔT _{max} (60-57), °C	256	236	594	69	516
ΔT _{min} (60-57), °C	122	92	406	24	239

* Numbers in parenthesis refer to nodes.

Figure 1. Schematic Diagram of Water-Cooled Stainless Steel First-Wall Design Concept.

Figure 2. Schematic Diagram of First-Wall Concept With Radiatively-Cooled Graphite Liner

Figure 3. Power Curve for Reference Burn Cycle.

Figure 4. Temperature Response as a Function of Time for Stainless Steel First Wall during Reference Burn Cycle.

Figure 5. Temperature Response as a Function of Time for Stainless Steel First Wall during Burn Cycle with 30-s Dwell Time.

Figure 6. Temperature Response as a Function of Time for Stainless Steel First Wall with 2 MW/m² Average Neutron Wall Loading.

Figure 7. Temperature Response as a Function of Time for Graphite Liner During Two Burn Cycles.

Figure 8. Temperature Response of Graphite Liner Showing Effect of Thermal Conductivity of Graphite.

Figure 9. Temperature Response of Graphite Liner for 2 MW/m^2 Neutron Wall Loading.

Figure 10. Temperature Response of Stainless Steel Wall With and Without Graphite Liner.

Figure 11. Thickness of Ablated Region of Stainless Steel Wall After Plasma Disruption.

Figure 12. Thickness of Ablated Region of Graphite Wall After Plasma Disruption.

MECHANICAL DESIGN AND ANALYSIS FOR A EPR FIRST WALL/BLANKET/SHIELD SYSTEM*

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Introduction

Continuing studies are in progress at ANL to expand upon the design of a first wall/blanket/shield FW/B/S system and power conversion for a tokamak type Experimental Power Reactor (EPR). The FW/B/S system has evolved from an earlier design¹ for a low beta, circular cross section plasma (major radius = 6 m) to one for a higher beta elongated plasma with a 4.7 m major radius. Basic mechanical design and layout features of the old and new EPR designs showing some of the more important design developments are depicted in Figure 1 and listed in Table I. These developments are aimed at simplifying the design, reducing the costs and in addition, improving the plant thermal efficiency and overall maintainability. In the area of the reactor

blanket, significant thermal hydraulic and stress analysis have been performed to substantiate the integrity of the chosen concept. This paper deals with the discussion of these improved features.

Vacuum Vessel

To improve on the multitude of conditions and interface requirements associated with the vacuum vessel, it first was relocated to the outer portion of the reactor blanket a distance of 35 cm from the plasma boundary. A total of 22 cm of stainless steel blanket and first wall materials intercede to minimize the radiation damage to this component. In this position the vessel as shown in Figs. 1 and 2 serves two functions; namely to provide the required plasma vacuum

Table I. Comparison of EPR-77² and EPR-76 Design Features

Reactor Parameters	EPR-77	EPR-76	Design Features	EPR-77	EPR-76
Average toroidal beta, β_t	0.08	0.048	Vacuum Vessel	Outer blanket location, heavy 7-10 cm wall	Inner blanket location, thin 2 cm wall
Major radius, R (m)	4.70	6.25	First Wall Panels	Flat sections structurally attached to blanket	Curved sections separately replaced
Plasma minor radius, a (m)	1.34	2.10	Blanket	Slab type blocks, 128 major pieces	Contoured blocks, 272 pieces
Plasma elongation, b/a	1.65	1.0	Shield	Flat surfaced blocks, 192 pieces	Curved surface blocks, 420 pieces
Aspect ratio, A	3.5	3.0	Maintainability	Single purpose large reactor top closure	Vertical vacuum limited horizontal ports
Vacuum chamber volume, V_c (m ³)	450	711	Power Conversion	Dual cycle system eff. ~ 39%	Pressurized water 25-30%
Blanket thickness, Δ_B (m)	0.28	0.28			
Shield thickness					
inside, Δ_s^i (m)	0.52	0.58			
outside, Δ_s^o (m)	0.97	0.97			
TF coil bore (m)	6.8 x 10.0	7.8 x 12.6

Figure 1. ANL Tokamak Experimental Power Reactor.

* Work supported by the Department of Energy.

conditions, and secondly to support the blanket and first wall panels. It is also the temperature interface boundary between the hot fluids of the energy conversion system and the much cooler reactor shield system.

The vessel is made of flat plates assembled in three basic pieces per section with sixteen sections forming the irregular toroidal chamber whose overall dimensions are 13 meters in diameter by 5.4 meters high. The vessel weighs ~ 260 Mg and has 12 required horizontal beam ports. The vessel plates made from Type 316 stainless steel are nominally 7 to 10 cm thick which are formed by welding into the 3 pieces shown in Fig. 2 forming a section. The sixteen sections are bolted together (see Fig. 3) using a joining bar section and appropriate bolting from the inside of the vessel after which an expansion section covers the bolting seam with a welded seal strip. The top closure is also bolted and seal welded and functions solely for repair and maintenance access of the vessel internals. Each vessel plate contains a series of parallel gun bored coolant channels poloidally oriented in respect to the reactor plasma. Coolant in each plate is distributed through these channels in internally bored headers which are manifolded and carried to upper and lower supply and return rings. Support for the vessel is achieved with four adjustable column units located at the bottom of each segment which are in turn anchored to the lower shield block.

Some of the advantages this design offers are: relative ease of fabrication; modest radiation damage effects; modest cooling problems; accommodation of thermal expansion and insulation; ease of access to internals with a large area single purpose closure; repairability and replaceability with relative ease.

First Wall/Blanket

The first wall panels are integrated with the blanket blocks and line the interior of the vacuum vessel as shown in Fig. 1 following the basic contour thereof. These blocks, 128 in number are supported through the bottom of the vacuum vessel and anchored to its sides. Coolant line connections from each block are made through the top and bottom of the vessel where there is access in a relatively uncongested area. All blocks are readily removed through the vessel closure with special handling fixtures.

The blanket block arrangement is depicted in Fig. 4. It consists of 3 inner blocks, 3 outer blocks a bottom and a "U" shaped upper section. Each block is made from a 20 cm thick Type 316 stainless steel flat plate, with a coolant channel arrangement as shown. The coolant passages are transverse to the reactor and graded to match the energy distribution within the block thickness.

The upper and lower blocks are specially designed for steam cooling. Thermal and stress analysis indicate that steam exiting 410°C at 8.2 MPa is acceptable while the remaining inner and outer blocks operate with pressurized water cooling at 340°C and 152 MPa. This combination of coolant using superheated steam in direct cycle has a 39% thermal efficiency potential.

• **Thermal Hydraulic Analysis** -- The ANL/EPR blanket system utilizes an assembly of modular stainless steel blocks with gun-bored coolant channels. The thermal hydraulic analysis is based on a single modular block that is 0.25-m thick and 1.5-m in length in a plane parallel to the first wall. To simplify the geometric model, co-radial groups of coolant channels

Figure 2.

Figure 3.

with graded cross-sectional areas (to compensate for the exponential attenuation of neutrons in the blanket block) were selected for the initial analysis. A computer code capable of solving a set of three-dimensional thermal hydraulic equations was used to establish the transient, and quasi-steady state temperature distribution within the blanket block. The neutron power profile was provided by neutronic calculations for the ANL/EPR based on a 65-s burn pulse followed by a 15-s dwell period between pulse.

The operating conditions for the three coolant systems are summarized in Table 2. These operating conditions are consistent with those for existing pressurized water reactors, gas cooled reactors and fossil powered steam plants. Also, one of the salient features of this analysis is that it includes low pressure steam as a coolant and direct cycle working fluid for a fusion power plant.

A set of six coolant channels with iteratively adjusted cross-sectional areas and interchannel distances (see Figure 4) was evaluated during the first round of analyses. The analytical results are included in Table 2. Because of significant differences in the physical and transport properties of the three coolant systems, the coolant velocities, Reynolds numbers and heat transfer coefficients are seen to vary over a wide range. The lower heat transfer coefficient for steam results in a need for shorter interchannel spacing in the direction normal to the first wall. Hence, for a given reactor, cooling with steam appears to require more coolant channels per unit volume of blanket than either water or helium. Since the length of the coolant channels is only 1.5 m, the frictional pressure drop across the channel turns out to be negligible for all cases. This indicates that one can minimize the large variations in required fluid velocity by judicious selection of coolant channel cross sectional

Table 2. Summary of Thermal Hydraulic and Power Cycle Analysis

Parameter/Coolant	Water	Helium	Steam
Pressure, MPa	15.17	5.17	4.83
Interchannel Distance, cm	5	5	3'
Inlet Temperature, °C	240	205	260
Outlet Temperature, °C	340	400	410
Maximum Blanket Temperature, °C	500	550	550
Velocity, m/s	0.06-0.18	12-26	2-5
Reynolds Number $\times 10^{-3}$	3-13	10-30	12-50
Heat Transfer Coefficient, $W/m^2 \cdot K$	1200-2550	1100-1900	400-700
Pressure Drop, MPa	< 0.01	< 0.01	< 0.01
Thermodynamic Cycle Efficiency, %			
Direct Cycle	—	13	39
Indirect Cycle	34	36	36

Figure 4.

area and can thereby optimize the heat transfer characteristics.

• **Blanket Stress Analysis** — Consideration of the thermal stress distributions in the blanket blocks influences the mechanical design of the blanket in two significant ways. First, the design of the blanket support structure must permit the overall thermal deformation of the blocks without permitting radial neutron-streaming paths to open between blocks during operation or shutdown of the reactor. Constraint of the overall deformation would result in excessively high stress levels in the blocks. The overall thermal deformation depends on the distribution of heat generation in the blanket and the coolant properties, but it is relatively insensitive to details of the coolant channel arrangement. Second, the temperature distribution within an unconstrained block produces a self-equilibrated stress field having local regions of stress concentration at the coolant channels and hot and cold spots in the block. These stress concentrations depend strongly on coolant channel arrangement; small changes in the channel pattern can correspond to significant alterations of the associated stress levels without affecting the thermal performance of the blanket.

A simple stress analysis procedure was developed to evaluate the relative merits of various coolant channel configurations at the conceptual design level without resorting to the detailed and expensive stress analysis required for a final design. This procedure is based on the separation of the three-dimensional temperature distribution obtained in the thermal-hydraulic analysis described previously into two component distributions which respectively produce the overall deformation, effects and the localized stress-concentration effects. A linear (in three-dimensional cartesian coordinates) temperature distribution is fitted to the given temperature distribution by a least-squares technique. The corresponding deformation field is obtained exactly in closed form and represents the global deformation of the block; the associated stress field is determined by interaction with the support structure since a linear temperature distribution produces no self-equilibrated thermal stress field. Then, a residual temperature distribution is computed as the difference between the given distribution and the least-squares fit. An approximate self-equilibrated thermal stress distribution is obtained for this residual problem. The entire procedure has been automated in a small computer program which accepts the output data from the thermal hydraulic analysis.

Comparisons between the relative merits of coolant patterns can thus be obtained rapidly and inexpensively.

As an example, thermal-hydraulic analysis using pressurized water coolant indicated that channels spaced 5 cm apart in the toroidal direction and located at radial positions of 1, 4, 8.9, 13.8, 18.8, and 23.8 cm from the inner surface would provide a reasonably uniform temperature distribution in the block; the channels varied in cross-sectional area between 1 cm² and 0.5 cm² (see Fig. 5). The associated maximum thermal strains in the block were found by the approximate procedure described above to be 0.35%. Moving the first channel to 1.25 cm from the surface reduced the strain to 0.25%. Moving the first channel to 1.5 cm and the second channel to 4.5 cm further reduced the maximum strain to 0.18%. The improvements in strain levels were accomplished without any significant change in thermal-hydraulic performance. The results indicate that preliminary thermal-mechanical optimization of blanket block design can be accomplished at the conceptual design level using relatively simple computational methods.

The overall unconstrained thermal deformation is 0.6% of the room-temperature dimensions, and the blocks become convex toward the plasma. These overall dimensional changes are virtually independent of the channel pattern variations discussed above. Stress analyses performed for temperature distributions at several times during the burn cycle indicate that the large thermal inertia of the blanket prevents any significant cyclic strain variations.

• **First Wall** — The first wall panels are shown in Fig. 6, they are made from 316 type stainless steel and form the same contour as the mating blanket block to which they are attached. The panels are similar to those described in the earlier ANL EPR design,¹ having a close array of transverse coolant channels connected to integral return and supply headers which are fed from lines common to the mated blanket block. The plasma face of the panel will be coated with a low-Z material. The blanket blocks are arranged in the reactor in stacks three high on the inner and outer walls of the vessel. At each segment junction a small 10 cm wide segment filler block (Fig. 7) is used for "packing" the block system. Each small block, 96 in all, is similar in construction to the first wall/blanket units with a first wall panel section integrated into a blanket block. The first wall panels in this case will have additional cooling capacity and may serve as plasma limiters. These corner blocks are removed first, freeing the sides of the major block to be removed. Since the major blocks are stacked, it may require removal of eight pieces to gain access to a lower vertical blanket block.

Some of the advantages of this first wall/blanket design are: simple and inexpensive construction of blanket blocks and first wall panels; small numbers of components and fewer coolant connections; superheater blocks for high thermal plant efficiency; relatively simple and quick maintenance and replacement function; experimental testing of superheater modules or large portions of the first wall with relative ease; accommodation of eddy currents effects.

Shielding

The shield is divided into an inner and outer block denoting a high efficiency B₄C-SS composition for the inner shield and less expensive SS-graphite-boron-lead mortar-aluminum composition in the outer shield. There are 192 blocks that envelope the vacuum vessel (see Fig. 8). The configuration is

Figure 5.

Figure 6.

Figure 7.

Figure 8.

designed to provide adequate shielding in a minimum number of easily fabricated pieces with special emphasis on simple and rapid access to the vessel closure. The shield is supported on columns from beneath the reactor and in turn supports the vacuum vessel/blanket/first wall complex. A low pressure water system cools the shielding blocks. The advantages of this design are: ease of manufacture (small numbers of blocks with flat surface); ease of support and handling; quick and simple access to the reactor vessel; favorable magnetic permeability characteristics.

Maintainability

Of prime importance in this reactor design was to obtain simple and quick access to the interior of the reactor vessel for inspection, maintenance repair to obtain the maximum data and availability on this first round power reactor. In addition, experimental modification and total replacement and repair of all major components should be possible with reasonable pieces of remote handling equipment performing in a reasonable time span. In this regard the design of this reactor accomplishes a number of important functions towards satisfying these goals.

• **Vessel Access** -- Access to the interior of the vacuum vessel has been simplified over earlier concepts. A single purpose large area closure for access to the vacuum vessel has been incorporated whose sole function is inspection, maintenance, and repair of internal reactor components. This replaces the dual purpose limited area vacuum/experimental port access used

previously.¹ The current design has the advantage that access is gained in any given zone by the removal of 4 wedge shaped top shield blocks without disturbing any of the reactor components. A simple lifting fixture is used with hands on operations until actual removal of the block (see Fig. 9). This feature saves considerable time and reduces the risk of damage as in the case of dual purpose ports.

SHIELD BLOCK REMOVAL

Figure 9.

Once access to the vessel has been made adequate space and provisions are available for uncoupling coolant lines and instrumentation through the vacuum barrier to the blanket and first wall components. In addition the reactor closure flange wall provides a very substantial base from which to mount and index inspection and handling fixtures as shown in Fig. 10. Special consideration has been given to handling of the upper and lower superheater blanket blocks since these may require attention at an earlier interval and because they represent an experimental volume where examination and testing may be easily carried out.

OUTER BLANKET REMOVAL

Figure 10.

• **Beam Ports** — In simplifying the reactor layout the number of major reactor service ports has been greatly reduced from 48 to 16 in the current design of which only 12 are committed to normal operation. By the arrangement shown in Fig. 1 each port serves two functions as a vacuum exhaust port and as a neutral beam injection port. Two cryosorption pumps and 2 neutral beam lines use a single port complementarily with each component having its own independent isolation and shield valve. All units are easily maintained and under most circumstances failure of a single component will not lead to a reactor outage. It is the compactness of this vacuum/neutral beam port arrangement that allows the vacuum vessel access system concept to be achievable.

• **Secondary Vacuum System** — A secondary vacuum system whose boundary envelops the entire reactor shield at the TF coil periphery has also been incorporated in the design of the reactor. The purpose of this feature is to provide a backup system for the primary reactor vacuum systems such that atmospheric contamination of the plasma due to single small vessel leaks is not possible, and secondly; as a safety feature to provide a secondary barrier for detection and entrapment of tritium. These functions serve to minimize reactor outages due to small defects, warn operations personnel of impending leak problems, provides monitoring for tritium contamination prior to handling reactor components, and introduces a reasonable volume in which to promptly handle a massive tritium release.

The secondary vacuum containment which has been called the "bell jar approach" in this design is formed by adding additional vacuum cover plates to the existing torque shell used to restrain the TF coil system designed for ANL by the McDonnell Douglas Corporation.³ Since the location of these panels is in a very low radiation field, seals may be made with neoprene gaskets using "hands on" maintenance operations.

• **Major Components** — Maintenance of major components has been simplified to a great extent. In particular by the ability to replace the entire or any part of the vacuum vessel under reasonable conditions in a relatively short schedule insofar as reactor component disassembly, manipulation and storage is concerned. Adequate space has been provided in the reactor building for complete laydown and storage of reactor components. It is also possible to remove a TF coil by local disassembly, removing only portions of the adjacent vessel and inner and outer shielding blocks. Replacement of this component with this degree of modest difficulty is an asset to the early generation of power reactors.

Hot cells have been provided for the more routine functions of examination, repair and replacement of first wall panels, blanket, and superheater blocks. These cells also serve the needs of the experimental program.

Power Conversion System

One outstanding development in the EPR design has been the upgrading of the plant power conversion efficiency from ~ 30% to a potential 39%. This was accomplished by using the upper and lower blanket as superheater sections while the inner and outer vertical blanket zones are cooled with pressurized water. To accomplish this, the blanket blocks in the superheater region require a greater number of and different sized coolant channels which were determined by the aforementioned thermal hydraulic analysis. The overall thermal expansion of the blocks will differ and the superheater section will be at a higher overall

temperature, thus the geometrical arrangement of the blanket assembly was designed to accommodate these differences by placing the superheat zones in the upper and lower regions of the vacuum vessel. In these extremities additional shielding was added to compensate for voids and streaming gaps.

Two distinct cooling systems are used; the pressurized water system is a closed cycle system consisting of a circulating pump and steam generator identical to that of the current commercial Pressurized Water Reactor (PWRs); and a direct cycle superheated steam system wherein the steam goes directly from the reactor to turbine similar to those used in Boiling Water Reactors (BWR). The dual cycle system is shown in Fig. 11. Pressurized water from the first wall/blanket assemblies of the inner and outer blanket delivers thermal energy to an evaporator operating at 13.8 MPa (2000 psi). The steam thus generated in the evaporator is fed into the upper and lower superheater blocks where it is heated to 410°C at ~ 8.6 MPa (1250 psi) and fed directly into the turbogenerator with a resultant overall cycle efficiency of ~ 39%. Radioactive transport problems of the direct cycle are being analyzed, but they do not appear to be appreciably more severe than those encountered in BWRs. The power plant rating for the EPR is 270 MWth, 120 MWe with a full capacity cooling tower 315 MWth. Cyclic temperature effects on the coolant leaving the reactor are minimized < 2°C due to the large thermal inertia of the stainless steel blanket.

Further analysis using higher strength materials and configurations shows promise for higher cycle efficiencies in excess of 40% where use of modern turbines vs the saturated units now employed in the Light Water Reactor nuclear plants.

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Figure 11.

SIMULATION OF LARGE SCALE AIR DETRITIATION OPERATIONS BY COMPUTER MODELING AND BENCH-SCALE EXPERIMENTATION*

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Summary

Although some experience has been gained in the design and construction of 0.5 to 5 m³/s air-detrition systems, little information is available on the performance of these systems under realistic conditions. Recently completed studies at ANL have attempted to provide some perspective on this subject. A time-dependent computer model was developed to study the effects of various reaction and soaking mechanisms that could occur in a typically-sized fusion reactor building (~10⁵ m³) following a range of tritium releases (2 to 200 g). In parallel with the computer study, a small (~50 liter) test chamber was set up to investigate cleanup characteristics under conditions which could also be simulated with the computer code. Whereas results of computer analyses indicated that only ~10⁻³% of the tritium released to an ambient enclosure should be converted to tritiated water, the bench-scale experiments gave evidence of conversions to water greater than 1%. Furthermore, although the amounts (both calculated and observed) of soaked-in tritium are usually only a very small fraction of the total tritium release, the soaked tritium is significant, in that its continuous return to the enclosure extends the cleanup time beyond the predicted value in the absence of any soaking mechanisms.

Introduction

The need for large capacity air handling and detrition systems to service ambient operated fusion buildings is well recognized.¹⁻³ The cost of such systems should scale roughly linearly with the volumetric air flow rate which is in turn determined by (1) the permissible length of time to complete a cleanup operation following the largest conceivable tritium release to a given enclosure and (2) the overall volume of that enclosure. Recent fusion reactor design and safety analyses¹⁻³ have uncovered at least two dominant incentives for achieving the shortest possible cleanup times. Firstly, short cleanup times would permit timely access to the reactor building when manual maintenance or repair is required, thereby reducing reactor downtime in most cases. Secondly, the amount of tritium soaked onto and into the surfaces in the reactor building is expected to increase with increasing cleanup duration and, as a consequence, the subsequent release of this "soaked" tritium could further extend the time period required to reduce the atmospheric tritium concentration to breathable levels.

Some experience has been gained in the design and construction of 0.5 to 5 m³/s air detrition systems.⁴⁻⁷ What has not been investigated in any detail is the actual performance of fully integrated cleanup systems during massive detrition operations on large enclosures. In particular the impact of various chemical reaction and soaking mechanisms on the progress of the cleanup operation is not well understood. A series of studies to evaluate some of these factors was carried out at ANL during 1977.

The approach taken in these studies involved (1) the development of a time dependent computer model to simulate anticipated physical and chemical responses in typical enclosures following a large tritium release and (2) a series of bench-scale tests with tritium in a relatively small enclosure under conditions that were intended to emulate a reactor building but on a much smaller scale. The following sections of this paper summarize in order (1) the computational model for cleanup analyses, (2) the results of computer studies of cleanup operations on conceptual fusion reactor buildings, (3) the methodology for and results of bench-scale tests with the small enclosure, and (4) attempts to analyze the bench-scale data using the computer model.

Description of the Computer Model

This section summarizes the important features of the model developed to simulate hypothetical air-detrition scenarios under conditions anticipated in near-term fusion reactor containment buildings. The logic of the resulting code (TSOAK) is based largely on (1) correlations of existing experimental data for pertinent chemical and physical processes (involving tritiated species) with the type of phenomena likely to occur in a large enclosure, (2) derivation of a set of time dependent simultaneous equations to describe the cleanup operation, and (3) selection of an efficient algorithm to solve the resulting set of equations. A detailed description of the model is given in reference 8. In brief, the steps built into the model are as follows:

Cleanup Operation

The principal pathway for removal of tritium bearing species from a reactor building atmosphere following an accidental release is the cleanup operation itself. In the model it is assumed that the air in a room of volume V is circulated through a detrition system at a volumetric flow rate Q and that the efficiency of the detrition system for removal of tritium-containing compounds in the air (regardless of their chemical form) is ϵ . The rate of reduction in the concentration of tritiated species, N_T, with time, t, by this mechanism is, therefore, given by

$$\left(\frac{\partial N_T}{\partial t}\right)_A = -\left(\frac{\epsilon Q}{V}\right) N_T = -A N_T \quad (1)$$

$$\text{i.e., } A = \frac{\epsilon Q}{V} \quad (2)$$

Reaction of T₂ with H₂O and O₂

There is ample evidence^{2,8} to support the assumption that T₂ released to an atmosphere containing an appreciable amount of oxygen and moisture will react with the O₂ and H₂O to form HTO, T₂O, and HT.

* Work performed under the auspices of the U. S. Energy Research and Development Administration.

Accordingly, a term to accommodate the production of tritiated water is included in the model. The basis for the development of this term is described in reference 8.

Permeation of T_2 and HT into Building Surfaces

The permeation of molecular tritium species (T_2 and HT) into building surfaces is dealt with in an approximate manner by summing the permeabilities of all structural surfaces based on an assumed steady-state condition⁸ where the driving force is proportional to the half-power of the T_2 and HT concentrations in the building atmosphere.

Adsorption and Release of HTO and T_2O

The adsorption and release of tritiated water (referred to hereafter as TO) also had to be treated in a highly approximate manner. The rate of adsorption of TO was taken to be proportional to the concentration of TO in the enclosure, and the corresponding proportionality constant was determined by summing the estimated adsorptivities of the different surfaces in the enclosure. A second proportionality constant is included in this part of the model⁸ to correlate the rate of release of adsorbed tritiated water with the total amount adsorbed on the building surfaces at any given time during cleanup.

Soak Effect and Amount Soaked

The soak effect is defined as the difference between the amount of tritium in the enclosure under ideal cleanup conditions, i.e., those strictly obeying equation (1), and the amount calculated by TSOAK when reaction and soaking mechanisms are active. The soak effect and the amount of soaked tritium are determined in a stepwise fashion⁸ along with the rest of the integration required to determine ambient concentrations of T_2 , HTO, and HT.

Application of the Soaking Code (TSOAK) to a Typical Reactor Building

In order to evaluate the application of TSOAK to a typical reactor building, a set of representative parameters were developed based on current projections for near-term tokamak reactors.^{3,9} Results of soaking calculations for a "base case" scenario involving the cleanup of tritium releases in the range 2 to 200 g to a 1.6×10^5 m³ room by a 5 m³/s air detritiation are presented in Table 1. Also shown in Table 1 are the results obtained by varying selected "base case" parameters. As indicated in the table, the amount soaked and the cleanup time are most sensitive to air flow rate. However, inclusion of 20 m² of cold surface, which is simulated by a sticking probability near unity coupled with a somewhat higher long term release coefficient (+20 m² at <0°C in Table 1), led to the prediction of a considerably extended cleanup time due to the slow back-release of condensed HTO.

Graphical computer output for the "base case" and the case involving the simulated cold surface are given in Figs. 1 and 2. The two solid curves in Fig. 2a correspond to the ideal cleanup response (lower plateauing curve) and the TSOAK calculated response for the base case (upper plateauing curve). In Fig. 1a both the ideal and TSOAK curves are identical and, hence, overlapping. The plateauing of the cleanup curves (solid lines) in Figs. 1a and 2a results from the inclusion of a 4 $\mu\text{Ci}/\text{m}^3$ asymptote to more realistically represent the kinds of terminating effects (due to background

signals) that may be encountered in actual monitoring operations. The need for this asymptote is more clearly evident from the results reported in the next two sections. The abrupt termination in the "soak effect" and "amount soaked" curves in Fig. 2a is a result of oscillations that develop in the differential equation solver when the amount of soaked tritium is large.

Bench-Scale Tests of Air Detritiation

In order to provide a test of the computer model for air detritiation analysis, a bench-scale experiment was set up to simulate, at least in part, the conditions anticipated for larger enclosures following a tritium release. The experimental assembly consisted of an ~50-liter cubical plastic desiccating box with a flat plexiglass door clamped to one face of the box and sealed by a circumferential rubber gasket. This box (hereafter referred to as the enclosure) was attached to two separate gas circulation loops. One loop (monitoring loop) was connected directly to a flow-through type tritium monitor (Triton Model 955B) having its own internal circulation pump. The other loop (processing loop) consisted of a flow meter, an oxidizer bed (Hopcalite) operated at ~550°C, a moisture trapping bed (4A molecular sieve) operated on 0°C, and a diaphragm-type circulation pump. Typical experiments were carried out by (1) injecting enough tritium as HT to raise the tritium level in the enclosure to from 1000 to 3000 $\mu\text{Ci}/\text{m}^3$, (2) circulating the enclosure atmosphere through the monitoring loop until a stable tritium signal was achieved, and then (3) starting up circulation through the processing loop.

To a first order of approximation, a cleanup operation carried out in the manner described above should proceed with a simple exponential dependence on time as given by equation (1). In actual practice, the uncertainties in measuring flow rate and enclosure volume (at best a few percent in each case), coupled with the fact that the efficiency is near unity, make it difficult to determine the extent to which equation (1) is obeyed. This is important because, when the trapping efficiency is only slightly less than unity, a significant amount of tritiated moisture can be transported back to the enclosure. The consequences of these uncertainties should be borne in mind when considering the results described below.

Figure 3 shows the results of typical detritiation simulations on the enclosure. The apparent obedience of equation (1) persists from the starting point ($t = 0$, $N_T = 1300 \mu\text{Ci}/\text{m}^3$) to an N_T value roughly 40 times lower, after which a pronounced tailing of each cleanup curve is observed. In order to perform the experiments shown in Fig. 3, an electrical feed-through was adapted to the enclosure to permit operation (inside the enclosure) of a circulation fan and a calrod heated stainless steel plate with ~200 cm² of surface area. As indicated in Fig. 3, the curves therein represent results of cleanup operations performed on the enclosure for various sets of operating conditions involving the fan and heated plate. Operation of the fan during detritiation (Case B) seems to enhance the tailing effect, possibly by accelerating the rate of the reaction/adsorption/release mechanisms involving HT and, more importantly, HTO. Operation of the hot plate at ~200°C together with the fan (Case C) demonstrated only moderately different detritiation characteristics relative to Case B. Although the hot plate might be expected to cause an increase in the rate of HTO formation, it also raises the enclosure atmosphere temperature by ~20°C, which would tend to reduce the amount of adsorbed HTO.

Fig. 1. Graphical representation of TSOAK calculations for a "base case" scenario of a 20-g T_2 release (see Table 1 and text). The total tritium concentration, soak effect, and amount soaked as functions of time are given in Fig. 1a. The concentrations of T_2 , HTO, and HT as functions of time are given in Fig. 1b.

Fig. 2. Graphical representation of TSOAK calculations for the "base case" scenario of a 20-g T_2 release, modified to include 20 m² of condensing surface (i.e., at $\leq 0^\circ\text{C}$). The total tritium concentration, soak effect, and amount soaked as functions of time are given in Fig. 2a. The concentration of T_2 , HTO, and HT as functions of time are given in Fig. 2b.

Fig. 3. Observed cleanup curves (solid lines) for selected experiments with the bench-scale air decontamination apparatus. Enclosure conditions are indicated on the figure. Also shown are ideal cleanup curves for cases A and B (lower dashed curve) and case C (upper dashed curve.)

Table 1
SELECTED RESULTS OF AIR CLEANUP CALCULATIONS USING TSOAK

Base Case Parameters:

Building Volume	= $1.6 \times 10^5 \text{ m}^3$	Reaction Rate	= $1.5 \times 10^{-11} \text{ m}^3/\mu\text{Ci}\cdot\text{min}$
Process Efficiency	= 1.0 (100%)	HTO Adsorption Rate	= $1.0 \times 10^{-7}/\text{m}^2\cdot\text{min}$
Air Flow Rate	= $5 \text{ m}^3/\text{s}$	HTO Release Rate	= $1.0 \times 10^{-6}/\text{min}$
Building Surface Area	= $2 \times 10^4 \text{ m}^2$	BPT ^a	= $0 \text{ } \mu\text{Ci}/\text{m}^3$

Conditions	Release Size (g)	Amount Soaked (Ci)	Cleanup Time (hr)
Base Case	200	6.3×10^{-1}	142
	20	6.8×10^{-3}	125
	2	7.0×10^{-5}	100
Base Case + Increase Air Flow Rate by 10-X	200	7.6×10^{-3}	14
	20	7.7×10^{-5}	12
	2	7.7×10^{-7}	10
Base Case + Increase Reaction Rate by 100-X	200	6.0×10^0	142
	20	3.6×10^{-1}	125
	2	6.5×10^{-3}	100
Base Case + Increase Adsorption Rate by 100-X	200	$<5.0 \times 10^1$	Tailing
	20	6.8×10^{-1}	125
	2	6.7×10^{-3}	100
Base Case + 200 m ² of SS at 300°C (Permeation Test)	200	2.1×10^1	142
	20	6.4×10^0	125
	2	2.0×10^0	100
Base Case + 200 m ² of SS at 300°C and BPT = $50 \text{ } \mu\text{Ci}/\text{m}^3$ (Back Permeation Test)	200	2.1×10^1	142
	20	6.0×10^0	125
	2	1.6×10^0	100
Base Case + 20 m ² at $\leq 0^\circ\text{C}$	20	6.3×10^1	Tailing

^aBPT = the average equivalent concentration of tritium in the high-temperature stainless steel (SS) that is contributing to back-permeation of tritium into the building. (See reference 8 for details of the calculations.)

During other experiments, similar to those in Fig. 3, one or the other, and in some cases both, of the processing steps (oxidation and water stripping) were temporarily by-passed. When both processing steps are by-passed at a point in time after tailing has set in, the tritium level in the enclosure usually tends to rise slowly with time, sometimes increasing overall by as much as 50 to 100%. Further, most of the residual tritium that is present after the tailing point has been reached can be removed from the enclosure simply by reinserting the molecular sieve bed alone. This result implies that, near the end of the simulated cleanup operation, a large fraction of the tritium present in the enclosure is in the oxidized form (HTO)—perhaps (as mentioned previously) the result of incomplete trapping of water by the molecular sieve bed.

Computer Fitting of the Bench-Scale Data

Following the experimental studies described in the preceding section, an attempt was made to derive reaction rate constants, HTO adsorption coefficients, and HTO desorption coefficients for the enclosure cleanup curves using TSOAK. By properly adjusting these three parameters it was possible to obtain exact fits to the experimental curves. The observed cleanup curves for two experimental cases are compared to the corresponding ideal cleanup curves in Fig. 4. Also shown in the figure are the data points for the TSOAK fit to the observed curve. (An asymptote of $4 \text{ } \mu\text{Ci}/\text{m}^3$ was used in both the ideal and TSOAK calculations, and it is this asymptote which causes the curvature in the ideal curve.)

The experiment plotted on the left-hand side of Fig. 4 was performed by pulling clean air into the enclosure and exhausting the exit gas to a fume hood. This experiment, therefore, represents the condition of having a 100% efficient scrubbing system. The experiment plotted on the right hand side of Fig. 4 was performed under identical conditions (release size, flow rate, etc.) except that the enclosure atmosphere was circulated in a closed loop through the oxidizer bed and molecular sieve bed. The increased departure from ideal behavior in the latter case (compared to the once-through flushing case) is again attributable to a less than 100% HTO scrubbing efficiency of the molecular sieve bed. While on first through this appears to be the result of having an inadequately designed scrubbing system, it does not take much of a departure from 100% efficiency to create a sizeably increased apparent HTO production rate in the enclosure, i.e., less than 1% reduction in efficiency would be more than adequate to explain the differences in Fig. 4. Table 2 lists the TSOAK-derived fitting parameters for the two experiments in Fig. 4 and for two other interesting cases as well. Particularly worthy of note are the substantial increases in all three parameters when the fan and hot plate are both operating. The 400-fold increase in the required reaction rate parameter is considered to be well above the uncertainties associated with the computer model and with the experiment itself.

Conclusions

Although the results of the soaking studies described above are considered to be largely of semi-quantitative significance only, several conclusions can be drawn from the work carried out to date. These

Table 2
SUMMARY OF CURVE MATCHING PARAMETERS OBTAINED FROM
TSOAK FITS TO ENCLOSURE DETRITIATION EXPERIMENTS

Condition	Reaction Rate Parameter ^a (m ³ /μCi-min)	Adsorption Rate Parameter ^a (μCi/m ² -min)	Release Rate Parameter ^a (%/min)
Typical Literature Data	10 ⁻¹² -10 ⁻¹⁴	--	--
Enclosure: Fan Off, Plate Cool	1 x 10 ⁻⁵	3 x 10 ⁻⁴	3
Enclosure: Fan On, Plate Cool	5 x 10 ⁻⁵	2 x 10 ⁻⁴	3
Enclosure: Fan On, Plate Hot ^b	4 x 10 ⁻⁴	5 x 10 ⁻⁵	12
Enclosure: Once-Through Flushing	1 x 10 ⁻⁶	2 x 10 ⁻⁴	3

^aSee reference 8 for a discussion of the derivation and implementation of these parameters.

^bPlate heated to ~200°C.

may be stated summarily as follows: (1) When the relative humidity is greater than a few percent, the total amount of adsorbed tritiated water is greatly reduced due to surface swamping by H₂O. (2) Exposed elevated-temperature metal surfaces (≥200°C) represent a major reaction and soaking pathway. All thermally hot metal surfaces should be insulated and preferably isolated from the building atmosphere. (3) Exposed low temperature surfaces (≤0°C) are also undesirable. Such surfaces (simulated by using sticking probabilities near unity) need not be large in area to condense large quantities of tritiated water. (4) For rooms in the range 10⁵ to 10⁶ m³, a 50 m³/s processor is desirable and an 0.5 m³/s processor is inadequate. With judicious selection of building materials, surface preparation procedures, and atmosphere operating parameters, a 5 m³/s system (near to state-of-the-art system) could be acceptable.

The principal value of predominantly empirical studies of the type described in this report is that they represent a medium with which results of actual detritiation experiments can be compared and correlated. Perhaps even more importantly they can (if properly used) provide a basis for the conception, design, and operation for meaningful large-scale air detritiation tests.

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Fig. 4. Comparison of observed versus ideal and TSOAK calculated results for two air detritiation experiments (flow rate = 1 liter/min, enclosure volume = 50 liters)

TRITIUM HANDLING TRADE STUDIES AND DESIGN OPTIONS FOR THE GA/ANL TNS[†]

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A comprehensive effort involving members of both the General Atomic Company (GA) and the Argonne National Laboratory (ANL) has been undertaken to define the objectives, criteria and potential systems design solutions that accrue to the tritium handling systems for the next logical step in tokamak reactor development (TNS). A primary focus of these activities has been a systematic analysis of fuel cycle parameters and trade studies on the sensitivity of these parameters to reactor design and operating conditions. Principal results of these analyses and an assembly of potentially useful design concepts for various subsystems of the fuel cycle are presented.

Introduction

TNS tritium handling systems represent significant extrapolations in both size and scope over those planned for the TFTR, primarily because for the first time a complete fuel cycle, including impurity removal and isotope separation, will be required. Indeed, as presently conceived, the TNS tritium handling systems will be functionally much closer to those of a power reactor. Design solutions proposed for these systems, due to the present conceptual nature of TNS design, must be applicable over a wide range of machine dimensions and operating scenarios. In order to determine the sensitivity of tritium system parameters to reactor engineering, plasma, and operational characteristics, a tritium systems code (TCODE) was developed at ANL and used to carry out a wide range of parametric analyses. A discussion of TNS tritium systems and the major results of parametric analyses using TCODE is presented below.

System Design Description

This section presents a discussion of tritium systems designs. The designs presented are intended to be general, that is, qualitatively applicable to a wide range of operational concepts. In order to establish quantitative reference points over the range of possible TNS scenarios, two specific designs are defined: TNS (referred to as Ignition Test Reactor - ITR - in other general Atomic papers on this program) - a relatively small, low duty factor machine designed to operate as an ignition test reactor; and TNS-UP (referred to as the Upgradable Ignition Test Reactor - UITS - elsewhere) - a larger upgradable machine capable of ultimately operating at high duty factor and demonstrating the feasibility of producing electrical power. Significant tritium systems parameters defining these two designs are presented in Table 1.

Fuel Cycle

Most tritium handling systems comprising the tritium facility of a non-breeding tokamak fusion plant are directly involved with the fuel cycle. These include systems for torus evacuation, fuel purification, isotopic enrichment, plasma fueling and fuel storage. Dependent only indirectly on the fuel cycle

Table 1
TRITIUM FACILITY AND VACUUM SYSTEM PARAMETERS
FOR THE GA/ANL TNS and TNS-UP

	TNS	TNS-UP
PLASMA AND BURN CYCLE		
Burn time (s)	30.0	90.0
Dwell time (s)	270.0	30.0
Lag time (s)	2.0	2.0
Duty factor	0.100	0.750
Plant availability factor	0.200	0.500
Power per burn pulse (MW)	390.0	465.0
Ion density (particles/m ³)	1.48E+20	1.40E+20
Plasma volume (m ³)	255.0	335.0
Particle confinement time (s)	1.63	1.54
Reflection coefficient	0.970	0.970
Fractional burnup	0.124	0.187
TORUS EVACUATION SYSTEM PARAMETERS		
Evacuation volume (m ²)	314.0	443.0
Surface area (m ²)	349.0	449.0
Number of ducts	3	12
Duct diameter (m)	0.50	0.70
Duct length (m)	8.00	8.00
Number of pumps,		
Operating	3	12
Total	6	24
Speed/pump (m ³ /s)*	25.0	50.0
Pump capacity* (Pa-m ³)	3332	6666
Pump loading factor*	0.83	0.83
Post burn gas temp (K)	373.2	373.2
Post burn gas pressure (Pa)	5.51E-01	9.27E-01
Preburn pressure (Pa)	4.00E-03	4.00E-03
Minimum duct diameter (m)	0.411	0.652
Fixed regeneration time (h)	4	4
Required pump capacity (Pa-m ³)	2766	4105
Capacity required pump speed*	20.75	30.79
Required pump speed (m ³ /s)	3.84	23.48
TRITIUM AND FUEL PROCESSING PARAMETERS		
Tritium exhaust/day (g)	42.18	232.4
Annual tritium consumption (kg)	0.44	9.74
Annual cost of tritium (\$M)	3.05	68.16
Total tritium inventory (g)	258.3	1794.8
Mean pump inventory (g)	4.7	22.6
Storage [30 days burn] (g)	178.9	1599.6
Surge tank (g)	2.8	3.9
Liquefaction-still (g)	5.4	7.3
Distillation columns (g)	6.1	10.8
Fuel preparation (g)	60.5	150.7
Helium exhaust per day (g)	7.950	71.092
Protium exhaust per day (g)	0.321	1.905
Carbon exhaust per day (g)	1.926	11.430
Oxygen exhaust per day (g)	0.134	0.914
Nitrogen exhaust per day (g)	0.135	0.800
Argon exhaust per day (g)	1.284	7.620
Neutral beam duration (s)	5.00	5.00
Total gas load/injector (Pa-m ³)	33.60	33.60
Number of injectors	6	6
Total beam power (MW)	60.00	60.00

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Table 1 (Continued)

	TNS	TNS-UP
TRITIUM AND FUEL PROCESSING PARAMETERS (Continued)		
Beam energy (keV)	150.00	150.00
Deuterium injected per day (g)	11.95	29.88
Deuterium in D ⁰ beam pumps (g)	47.67	119.17
Tritium in D ⁰ beam pumps (g)	0.57	1.46
Number of burns per day	288	720
Tritium gas fill (g/day)	27.17	84.42
Tritium fueling (g/day)	20.97	201.33
Total deuterium feed (g/day)	20.14	160.62
Deuterium gas fill (g/day)	11.37	47.45
Deuterium fueling (g/day)	8.77	113.16
EMERGENCY CLEANUP SYSTEM PARAMETERS		
Volume of reactor bldg. (m ³)	1.00E+05	1.00E+05
Flow rate to EDS (T/min)	0.41	0.45
Flow rate to EDS (m ³ /s)	6.86	7.43
Removal efficiency	0.99	0.99
Cleanup time for major spill. (h)	48.00	48.00
Unit cost for EDS (\$/m ³ -s ⁻¹)	1.06E+06	1.06E+06
Cost for EDS (\$)	8.27E+06	8.88E+06

* Cryosorption option.

are systems for air handling and detritiation and for tritiated waste disposal. These are discussed separately below.

Torus Evacuation System. The principal duty of the torus evacuation system is the pumpdown of the plasma chamber between discharges such that impurity concentrations are reduced to levels acceptable for initiation of the following discharge. Analyses have shown that a pumpdown pressure between discharges of 4.0 mPa is sufficient to accomplish this requirement. Secondary requirements specify that the torus evacuation system be capable of handling the final stages of a pumpdown from atmospheric (e.g., following up-to-air maintenance) and be adequate for plasma chamber conditioning operations. This latter requirement indeed, could be limiting under some circumstances. The ultimate pressure is set by conditioning requirements to about 0.1 μ Pa.

Figure 1 provides a schematic overview of the fuel cycle, including identification of major components and interfacing logic.

Starting at the torus the first major system in the direction of flow is the torus evacuation system. Gas exits the plasma chamber through a system of ducts which vary strongly in number and requisite diameter with overall machine design and operational choices (Table 1). In the present TNS design the vacuum penetrations to the torus are integral with the neutral beam ducts, thereby minimizing the total number of penetrations of the first wall. A right angle bend, a neutral beam shield plug, and an ~ 8-m length provide adequate protection of downstream vacuum components from neutron streaming. The first active component of the torus evacuation system encountered by the exiting gas stream is the conductance limiter, a device designed to isolate the main pumps from the torus during startup. The extent to which particulate debris can be transported along the vacuum ducts is unknown, but it may be necessary to have some form of debris separator (see Fig. 1) in order to prevent contamination of downstream fuel cycle systems with gamma-active particulate matter from the first wall.

REACTOR BUILDING

TRITIUM FACILITY BUILDING

1. TORUS	14. D ₂ STORAGE
2. DEBRIS SEPARATOR	15. D ₂ SUPPLY
3. VACUUM PUMPS	16. NEUTRAL BEAM INJECTOR
4. SAFETY SURGE TANK	17. BEAM PUMPING SYSTEM
5. REGENERATION PUMPS	18. FUEL LIQUIFIER
6. CONSOLIDATION MANIFOLD	19. PELLET INJECTOR
7. TRANSFER PUMPS	20. T ₂ RECEIVER/STORAGE
8. COMPRESSOR	21. HELIUM REMOVAL
9. IMPURITY REMOVAL	22. HOLDING TANK
10. ISOTOPIC ENRICHMENT	23. T ₂ SUPPLY HOOKUP
11. D-T STORAGE	24. PURGE/EFFLUENT PROCESSING
12. FUEL BLENDER	25. WASTE CONSOLIDATION
13. COLD FUEL INJECTOR	26. WASTE DISPOSAL

Fig. 1. TNS fuel cycle layout

The next major components encountered are the main vacuum pumps. Considerable attention has been devoted to the selection of the most appropriate vacuum pump for TNS and the results to date may be summarized as follows: Cryopumping was found to be the most satisfactory choice over the entire range of possible pumping requirements and, indeed, is the sole choice at the very high speeds characteristic of TNS-UP and subsequent generations of fusion reactors. (For a small, low duty factor device, turbomolecular pumping is a reasonable alternative.) Of the many types of cryopumping a cryocondensation/gas bearing turbopump hybrid is currently the leading choice for TNS. The most promising option over a longer time scale appears to be a compound cryosorption pump of the type described by Watson.¹ Both types of pumps will be tested on the TSTA at Los Alamos² and one or more options should be qualified prior to a final design decision for TNS.

Vacuum pump regeneration must be accomplished in as short a time as possible in order to minimize the relatively vulnerable tritium inventory deposited on the cryopump surfaces. A regeneration cycle period (including time for cooling down) of four hours was determined to be reasonable for both TNS scenarios. A large vacuum isolation valve located in advance of the pumps, allows their regeneration and protects them from high pressure events such as up-to-air maintenance. The size and lifetime requirements

of the isolation valve represent an extension of present day technology, hence this valve has been earmarked as a high priority development item.

The methodology for intermediate compression of the fuel stream (10^{-1} to 10^3 Pa) required for cryopump regeneration has also been identified as a major development item. Turbomolecular pumps are probably the best choice at this time, but the turbo-option suffers from the disadvantageous possibility that oil or bearing gas can contaminate the fuel stream and that tritium contamination of bearing fluids can occur. As indicated in Fig. 1, component 6, the multiple gas streams from the regeneration turbopumps are then consolidated into a single stream which is passed out of the reactor room and into the tritium facility. Because the bulk of the total plant tritium inventory, including all inventory in fuel processing and storage, is located within it, the tritium facility is a lined containment structure. This arrangement serves to reduce the magnitude and the effects of both normal and accidental releases.

Fuel Processing System. Further compression of the gas stream exiting the reactor room and entering the tritium facility is achieved by a series of metal bellows (or wobble) pumps followed by a diaphragm compressor. Pressures of 0.1 - 0.4 MPa suitable for entry into the fuel purification subsystem, are attained. The first stage of fuel purification, item 9 of Fig. 1, is designed to remove all chemical impurities except He from the fuel stream and, further, to recover as completely as practicable the tritium associated with those impurities. The condensable impurity levels must be reduced to ≤ 1 ppm so that proper operation of the cryogenic distillation cascade will be assured. Impurity removal is accomplished either by cryogenic trapping at 30 K followed by hot gettering of the trapped species on Nb at 1300 K, or by a series of cryochemical processes.² The probable impurity species, amounts, and origins³ are shown in Table 2. Removal of helium is accomplished by a cryogenic stripping column.

The fuel stream is now contaminated only by protium but is also strongly depleted in tritium (from addition of neutral beam deuterium). Protium is

removed and the fuel stream enriched in tritium by the cryogenic distillation subsystem (Item 10 in Fig. 1). Protium removal and isotopic enrichment of the purified fuel stream is achieved by a 4-column cryogenic distillation cascade (with one equilibrator) that operates between 750 and 1500 torr as described in Ref. 2. Since tritium and protium will tend to accumulate in the neutral beam recycle stream, a portion of that stream is also processed by the distillation cascade. The output streams from the cascade are: a waste HD stream containing low levels of tritium, a $> 99\%$ pure D_2 stream for the neutral beams, and a DT stream enriched in tritium for refueling.

The next major component, Item 2 in Fig. 1, is the fuel blender where D-T makeup is added from storage and final D/T ratio adjustments are made. Storage is as a solid tritide, possibly UT_3 (though a less pyrophoric material is desirable). The storage vault will be compartmentalized and barricaded, have an inert atmosphere and be equipped with redundant controls, monitors, and fail-safe valving.

Fuel is transported as a gas through automatically actuated, isolation valving on each side of the tritium facility boundary. The stream then divides, one portion leading to a small, low pressure, gaseous fuel delivery tank (Item 13) and the other to a fuel liquefier (Item 18) followed by a pellet manufacture and injection subsystem (Item 19). These units complete the fuel cycle.

Gaseous Detritiation. All the fuel cycle subsystems and waste disposal systems, all interfacing hardware and all piping will be secondarily contained. The associated secondary atmospheres (air, N_2 , Ar and He) will be processed continuously with separate detritiation systems designed specifically for the carrier gas involved. Where practicable stripped tritium from these streams will be recovered.

Two emergency detritiation systems (EDS), one for the reactor room and one for the tritium facility are planned. The larger of these is the reactor building EDS. It is sized for a 2-day nominal re-entry time following a major spill. Details are given in Table 1. The tritium facility EDS is smaller by a factor of ~ 10 but may be expected to operate more frequently. Small subsystems for the disposal of low level gaseous, liquid and solid tritiated wastes will be located in the tritium facility. Techniques appropriate for these duties, involving solidification for tritiated water (tritium in gases will be converted to water) and absorption on vermiculite for tritiated oils, have been developed.⁴ Disposal of tritium by impregnation in concrete has also been reported.⁵

Parametric Trade Studies

Systematic analyses of tritium facility requirements have been carried out for a wide range of possible TNS scenarios. Base case parameters for the two specific scenarios addressed in this report, TNS and TNS-UP, are listed in Table 1. Analyses of the sensitivity of tritium facility requirements to plasma and reactor parameters were performed using the tritium systems code (TCODE).³ A significant fraction of the FY 1977 effort was put into this code development activity in order to provide a tool for broad range parametric analysis. TCODE has the capability of varying any of 50 input variables and calculating the result of these variations upon any of the 60 output variables. Numerous parametric studies of TNS parameters were completed with TCODE. Out of these, three

Table 2
ANTICIPATED IMPURITY SPECIES

Elemental Species (Atom %)	Principal Molecular Species	Possible Sources
He (5-20%)	He	Fusion reactions
H (1%)	DH, TH, water	Outgassing, permeation, leakage
C (0.02-1%)	Methane and acetylene	First wall or limiter erosion, pump oils, initial construction
O (0.3%)	water, CO, O ₂	Air leakage, outgassing, coolant leaks (water)
N (0.06%)	N ₂ , ammonia	Air leakage, outgassing
Ar (< 0.25%)	Ar	Air leakage, flushing, welding, plasma shutdown
Si (0.02%)?	Silanes?	First wall or limiter erosion

plasma related parameters were identified which had a profound influence upon the required performance of the tritium handling and vacuum systems. These parameters were the particle confinement time, the reflectance coefficient, and the dwell time.

Particle Confinement Time

Figure 2 illustrates the sudden sharp drop in achievable fractional burnup at particle confinement time, τ_p , values less than 2 seconds and also shows the expected corresponding increase in tritium exhaust rate (the base case reflectance coefficient used in these analyses was 0.97). Because the anticipated particle confinement time for both TNS and TNS-UP is in the range 1.5 to 2.5 seconds and in light of the fact that vacuum system size and cost scale roughly linearly with tritium exhaust rate and fractional burnup, potential deviations of τ_p from the projected values can have a profound effect on vacuum system design requirements. This point is further illustrated in Fig. 3 where the required total pumping speed and individual pump capacity are plotted against τ_p .

Fig. 2. Sensitivity of fuel burnup and flow rates to τ_p

Two other important parameters, the required speed per pump and the minimum allowable duct diameter are plotted against τ_p in Fig. 4, for cases where all other independent parameters were fixed at the values given in Table 1. The most sensitive result here is in the required pump speed for TNS-UP which results mainly from the shorter dwell period for this scenario. (As indicated in Table 1 a pump regeneration time of 4 hours was assumed in these calculations.) Since it is proving desirable from the standpoint of nuclear design requirements to minimize both the number of ducts and the duct cross-sectional area, the torus evacuation systems are designed so that the duct conductance is a minimum value and the pump speed is sufficiently high to produce the required total evacuation speed. As a result a small change in duct diameter, d , is found to cause a much larger change in individual pump speed, in terms of achieving the required overall pumping speed at the torus. Therefore, the required

Fig. 3. Effect of τ_p on pumping speed (at the torus) and pump capacity

Fig. 4. Impact of τ_p on pumping station design

pump speed is a strong function of τ_p (for a fixed duct diameter), while the required duct diameter, d , is a rather weak function of τ_p . (Duct conductance is a function of d^2 and d^3 .)

Because particle confinement time significantly affects the tritium throughput rates, tritium inventories (particularly those in the reactor building) are also very sensitive to τ_p . The sensitivities of total and pump-located tritium inventories as a function of τ_p are shown in Fig. 5. While a sizable

Fig. 5. Tritium inventories as a function of τ_p

fraction of the total inventory can be retained safely within multiple levels of containment, it is difficult to multiply-contain the cryopumps so as to be reasonably sure that an accident will not release the pump-stored tritium to the reactor building. (For example, the pump inventory is considered to be much more vulnerable than is the tritium inventory in storage.) Comparison of the TNS and TNS-UP cases in Fig. 5 shows the sizable advantages of a longer dwell time (lower duty factor) device from the standpoint of pump-stored tritium when particle confinement times are less than 2 seconds.

Reflectance Coefficient

The reflectance coefficient, R , is defined as the probability that an escaping plasma particle will eventually recycle back into the plasma. In a reactor such as TNS, which does not have a divertor, R is expected to be very close to unity, i.e., greater than 0.90. On the other hand, a reactor with a 90% efficient divertor would have $R = 0.10$. The fractional burnup is a reciprocal function of $1-R$ and the tritium exhaust rate is a linear function of $1-R$. The effect of R upon required pumping speed at the torus and tritium inventory is shown in Fig. 6. Pumping speed is moderately sensitive to R , and tritium inventories depend upon tritium throughput rates and are, therefore, linearly dependent on $1-R$ as seen in Fig. 6.

Dwell Time

Since the TNS might be upgraded to produce high temperature sensible heat, the consequences of operating the reactor at high duty factor (TNS-UP) must be evaluated. In a reactor of this type the burn time is limited so there is strong incentive to operate with as short a dwell time as is possible. Dwell time has the greatest impact upon the torus evacuation system. As shown in Fig. 7, required pumping speeds become very sensitive to the dwell period when the dwell time is less than 40 seconds. Also, pumping speed and associated costs are profoundly affected as the dwell time is shortened. At short dwell times, conventional

Fig. 6. Effect of reflectance coefficient on required speed and tritium inventory

Fig. 7. Dramatic effect of dwell time on vacuum system requirements and tritium inventory

vacuum pumps such as turbo-pumps would become prohibitively expensive and cryo-pumps would have to be used. In addition to effects on the required pumping speed, the number and size of the vacuum penetrations must dramatically increase as the dwell time becomes shorter. Other key tritium facility parameters such as inventories (see Fig. 7) are also significantly affected because higher duty factor operation requires higher daily tritium throughput rates.

General Conclusions

Tritium systems for a non-breeding, low duty factor tokamak ignition test reactor having passive impurity control can, for the most part, be designed with techniques and components available today. Proof testing, with detailed design and experimental verification, is required, and the ISTA as it is presently conceived² should adequately serve this need.

Substantial enhancement of the duty factor and/or addition of a divertor would require further development in technology, particularly in vacuum component design.

Large uncertainties in plasma parameters, likely to persist through all TNS design stages, make a versatile, conservatively scaled fuel cycle design mandatory.

The size and cost of the vacuum system are much more sensitive to reactor design and plasma performance parameters than are those of the tritium systems.

Specific Conclusions and Recommendations

1. High duty factor operation requires a dramatic increase in pumping speed and in the number and size of penetrations. Comparing TNS to TNS-UP, the required speed per pump rises from 4 m³/sec to 24 m³/sec, the number of penetrations rises from 3 to 12 and the required mean diameter of the penetrations increases from 0.50 m to 0.70 m. The resulting costs of the vacuum system will rise from about \$0.8 × 10⁶ for TNS to about \$6 × 10⁶ for TNS-UP.
2. While a higher duty factor results in substantial increases in tritium throughput rates and inventories, overall costs and technological requirements for the fuel cycle systems are not significantly affected.

3. Cryo-based pumps were identified as the best vacuum pumping option for near-term (TNS-type) tokamak reactors and the only satisfactory option for an upgraded TNS and for post-TNS reactors.
4. Plasma related parameters such as particle confinement time, reflectance coefficient, and dwell time were found to have substantial effects upon tritium and vacuum system requirements.
5. Cryogenic trapping followed by hot-gettering of the trapped species is the preferred option for impurity removal because it appears to accommodate a wide range of impurity concentrations and species.

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A LOWER HYBRID HEATING SYSTEM FOR AN IGNITION TOKAMAK*

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Summary

We have attempted to design a complete Lower Hybrid Resonance Heating System (LHRH) that could be used for TFTR, TNS, EPR, or a reactor. In addition to plasma physics constraints, we have considered those imposed by neutron radiation, surface heating of waveguides, sputtering, multipactoring, vacuum systems, materials, window design, engineering, maintenance and assembly. The system uses a Lallia - Brambilla grill which is fed by a number of waveguides entering the reactor by means of a labyrinth.

Plasma Physics Considerations

It has been shown^{1,2} that plasma heating to ignition should be possible with power levels of the order of 50 MW, with heating times from 4 to 10 s. Additional constraints are imposed by the requirements that this power must penetrate to the center of a plasma which has, for most ignition machines, a 1 to 2 m minor radius. The heating system must also be compatible with whatever means might be selected to raise the plasma density to 2 to $3 \times 10^{20}/m^3$ in the center of the plasma. Heating by means of radio frequency (rf) power in the lower hybrid frequency range was studied because 1) the availability of power in this frequency range (1 to 5 GHz), 2) the experimental success of the grill launching structure proposed and studied by Lallia and Brambilla³, 3) the overall simplicity and flexibility of the resulting system, and 4) the general compatibility of this system with the high-radiation environment. We have assumed for the purposes of this study that the LHRH system would be the only supplementary heating system.

Recent experimental evidence from experiments at Princeton and Grenoble has shown that the phased slow wave array of waveguides is efficient in coupling power into the plasma, and the behavior of these structures is well described by the theoretical work of Brambilla³. Bulk plasma heating has also been demonstrated⁴. Experimental evidence also seems to show, however, that the frequency and parallel refractive index of the system are important variables. The recent ATC data⁵ have shown that parametric instabilities in the plasma itself are excited when the driving frequency f_0 is near the lower hybrid frequency, f_{LH} . These parametric instabilities can limit the power absorption of the plasma and lead to power deposition near the edges of the plasma. Both theoretical and experimental evidence indicates that the effects of parametric instabilities should be negligible for $f_0 > 2f_{LH}$. At these frequencies the primary mode of power absorption will be Electron Landau Damping (ELD). The range of parallel refractive indexes ($n_{||}$) appropriate to ELD depends on the plasma temperature. For efficient coupling, the phase velocity of the wave should be comparable to three or four times the electron thermal speed. An additional constraint is imposed by the requirement that $n_{||}^2 > 4\omega_{pe}^2/\omega^2$. Figs. 1 and 2 show the lower hybrid

Fig. 1. Lower Hybrid Frequency for D^+ Plasmas

Fig. 2. Thermal velocities and $n_{||}$ values vs T_e

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frequency f_{LH} , and the phase velocities and $n_{\parallel}$ values appropriate to ignition machines presently considered.

The properties of grill structures in this frequency range can be taken from Fig. 3. The element width is quite narrow for high frequency high $n_{\parallel}$ configurations. Structural constraints (considered below) make widths (waveguide and wall) of less than 1 cm unattractive. The power radiated (Fig. 4) by a grill is a function of the phase difference between waveguides or the equivalent value of $n_{\parallel}$. Here optimized for a given $n_{\parallel}$ value. Radiated power increases as $E_{\max}^2$.

A possible scenario for heating to ignition is shown in Fig. 5. The density is quickly raised to near its final value and the temperature is raised smoothly over about 7 s. The power required to do this need not be constant. As shown in Figs. 2 and 4, the low temperature plasma will require $n_{\parallel}$ values which will be rather inefficiently radiated by the grill structure. Fortunately, the conduction losses of the low-temperature plasma will also be small. The grill structure should be fairly well matched to the plasma.

Fig. 3. Maximum $n_{\parallel}$ as a function of grill width

Fig. 4. Power Radiated vs "Optimized $n_{\parallel}$ "

RF Engineering

The launcher for supplying 50 MW of supplemental heating consists of ~40 narrow-height WR-430 waveguides forming a rectangular array, 4 waveguides high and 10 waveguides wide. The narrow dimension of the guide is parallel to the toroidal magnetic field direction. In order to reduce the probability of electrical breakdown and reduce the cost of the feed waveguide system, the major portion of the waveguide system is pressurized with SF_6 . A ceramic window isolates the plasma from the pressurized gas.

In addition, a dc break, as shown, is required in the feed waveguide either before or after the window to prevent possible ground loop interference with sensitive instrumentation and protection of personnel and equipment from ground faults. A vacuum pumping system is placed between the window and face of the grill to achieve a safe operating pressure of 10^{-6} torr at turn-on of the rf power.

Fig. 5. A possible scenario for Heating

Details of the high-power rf system are shown in Fig. 6. Each vertical column of the grill is fed by a 500 kW CW klystron amplifier, the X3070. The X3070 is a high-power modulating anode klystron tunable from 2.320 to 2.456 GHz with a gain of 55 dB and conversion efficiency of 53%. The high-power 4-way splitter is constructed of three high-power matched loads to ensure proper power division and provide some additional isolation of the source from the load in the event of arc-down of one or two guides. Isolators rated at 125 kW CW are also provided for maximum system protection. Isolators rated at 500 kW CW have already been built and tested at 3.1 GHz.

The rf system has been very conservatively designed to demonstrate what is achievable with more readily available components with large safety margins. The windows are similar in design to the klystron window but only rated 125 kW CW. The only component in the waveguide system that is not standard is the dc break. This could be readily designed around a contact-less resonant choke flange arrangement in which the space between the choke and flange, as well as the groove, is filled with ceramic. The gap and groove are sized to hold off the expected dc fault voltage and present a short circuit at the waveguide junction at rf.

The system 1 requires 120 high-power klystrons and 10 grills to supply 50 MW of supplemental heating to the plasma.

The overall dc to rf efficiency is 41%, without thermal recovery, assuming a transmission efficiency for the grill of 90% (95% has been achieved in a four-element grill on ATC), 53% as the conversion efficiency of the klystron, 0.4 dB as the insertion loss of the waveguide transmission line, and 95% as the ac to dc conversion efficiency of the dc power supply.

System Engineering

The overall plan of the rf system is based on the following arguments: 1) The major part of the waveguide run from the klystrons to the reactor will be pressurized SF₆, and 2) The windows separating the pressurized and evacuated sections should be removed from as much as possible of the neutron flux. Pump down of the evacuated section will be most efficient if it is short, and the conflicting constraints of low neutron flux and short waveguide sections has been solved by means of a two-bend labyrinth. (Additional pumps may be required near location A, Fig. 7). This will minimize the time between breakdown, when the neutral pressure may be $\sim 10^{-4}$ torr, and the time when the rf can be turned on, which seems to be dependant only on when a pressure of $\sim 10^{-6}$ torr, can be achieved.

The location of the high-power klystrons is determined by the constraints that they should be far enough from the reactor to be away from the pulsed ohmic heating fields and high neutron radiation levels, which would interfere with their operation and maintenance. The close phasing requirements will probably complicate long runs.

Fig. 6. LH rf heating system

The toroidal vacuum pumping system is influenced by the choice of LHFI over neutral beam heating as neutral beam injectors provide a natural means of evacuating the torus between pulses. Toroidal vacuum systems on rf-heated tokamaks will probably require penetrations in shielding comparable to that required by neutral beams.

Materials

The choice of waveguide materials is constrained by the requirements for 1) low resistivity, 2) dimensional stability, and 3) good mechanical and thermal properties. On the basis of resistivity, copper and aluminum are the most obvious choices but copper has roughly 60% lower resistivity values, making it somewhat more preferable. Dimensional stability is important because slight thermal or radiation induced swelling or warping of the waveguides will alter the phase of the signals in the waveguide, preventing power absorption. Neutron-induced void formation, which produces swelling, occurs at a much higher rate in the temperature range 300° to 200° C in Al and 130° to 400° C in Cu. (The upper temperature limits of both materials may be increased in a fusion reactor due to the high levels of He and H that result from high energy neutron reactions). The thermal operating range of copper is thus greater than aluminum, and small (0.5%) amounts of Be may further increase the resistance of Cu to swelling, at the expense of raising the electrical resistivity. Other considerations that lead to the choice of Cu are its higher melting point (1083° vs. 660° C for Al) leading to more tolerance for temperature transients, higher strength, and compatibility with Be, which should allow the use of a He coating.

The surface material of the waveguide is critical for two reasons: 1) impurity sputtering effects would be minimized if a low Z coating was applied to all surfaces exposed to the plasma, and 2) multipactoring (resonant avalanche breakdown in waveguides) would be minimized if the surfaces chosen had secondary electron yields for electrons less than 1. Both these constraints can be met if the waveguides are coated everywhere with a thin layer of Be. This treatment has also been advocated for the whole reactor first wall.

Neutron-induced surface damage, which would continuously alter the surface roughness at the 10 μ in level is not a serious problem for rf in the 1 to 5 GHz band. However, higher-frequency heating systems (electron cyclotron heating at 70-120 GHz) may be quite sensitive to this effect.

The vacuum window in the waveguide system is the other element where material problems are critical. The constraints that limit the material choices are 1) long lifetime under high neutron and gamma fluxes at temperatures of ~100° C, 2) high dielectric strength, and 3) low loss tangent (<0.0002). Possible insulator materials include Al₂O₃, MgO, Be, Y₂O₃, ZrO₂, and Si₃N₄. The first three seem to have lower radiation resistance than the others but there is little appropriate experimental data on radiation damage on insulators. Loss tangent data, particularly in a radiation environment, is also not sufficient. A tentative selection of window material would be Y₂O₃ if radiation resistance was the most important consideration, and BeO or Al₂O₃ otherwise. The radiation resistance required is, of course, a function of the shielding around the window region, and adequate shielding would simplify maintenance and lower the probability of window failure.

Shielding

In order to study the problems associated with shielding the vacuum windows from the plasma, we have considered a labyrinth with the configuration shown in Fig. 7. A system with two bends was modeled because 1) it is doubtful that more than two bends could be required, and 2) all the required data for zero and one bend systems can be obtained from the two bend results. The shielding geometry was selected to give some idea of the maximum shielding possible, and it is expected that operating systems would not require such elaborate structures.

The system in Fig. 7 was modeled using a continuous energy monte carlo code (VIM)⁶ which followed 15,000 neutrons through the geometry. The results, which have statistical errors of about 25%, are presented in Table 1. They show that the neutron flux is attenuated by a factor of ~10⁴ as it streams down

Fig. 7. Shielding Geometry. Ducts are 0.44 m wide in Z, and X axis is symmetry plane.

TABLE 1

Radiation Levels in Ducts
(normalized to 1 MW/cm², 1st wall)

		Flux
Duct 1		
max		5.10 ¹⁸ n/m ² sec
min		8.10 ¹⁷
Duct 3		
max		5.10 ¹⁶
min		5.10 ¹⁴
		Average Dose
Duct 2		1.5·10 ²⁵ n/m ² .yr (7.10 ¹¹ Rads)
Duct 3		9.10 ²³ n/m ² .yr (5.10 ¹⁰ Rads)

Fig. 8. Grill Structure

Fig. 9. Overall System Design

the ducts, a significant fraction of this attenuation is contributed by the the two bends. The data also show a rapid attenuation of the neutron flux down the length of duct 3, where the average neutron energy is much lower than in duct 1. The radiation dose levels show that Al_2O_3 windows would probably survive for more than one year in duct 2 and for more than 15 years in duct 3 at averaged first wall fluxes of 1 MW/m^2 .

Another result of this study is that the flux at A in Fig. 7 is $\sim 1/10$ of the first wall values. This high flux requires that an additional penetration shield be placed at location B.

Cooling of Waveguides

Since the grill elements will be narrow, it becomes important to try to minimize the thickness of the wall separating adjacent waveguides. The minimum limiting thickness is determined by the size and geometry of coolant channels within the walls. Neutron heating amounts to about 1 W/cm^3 of material, and steady state surface heating loads are about 20 W/cm^2 . The waveguides and grill structure should operate at low temperatures ($<100^\circ\text{C}$) to avoid increased radiation-induced swelling, which takes place at a higher rate at high temperatures. The requirements imposed by disruptions have not been considered in detail.

The wall geometry selected is shown in Fig. 8. The wall thickness is 0.5 cm, with coolant channels running within the walls. The inlet and exhaust manifolds would occupy the space between the horizontal rows of waveguides. In order to spread out the thermal loading over as large an area as possible, it is hoped that the parts of the grill seen by the plasma can be smoothly rounded. This rounding might also improve the high-voltage capacity of the waveguides by eliminating locations for arcing.

Conclusions

Although it has not been experimentally proven that rf heating is an acceptable method of achieving ignition. This system (Fig. 9) has a number of advantages over neutral beams, which are the primary alternative. The advantages of rf are primarily due to the simplicity of that part of the system which is actually exposed to high-radiation levels. (The grill is simpler than a neutral beam injector.) Shielding and pumping on rf systems are also made easier than neutral beam systems and this over-all

simplicity is reflected in the costs estimates that have been done for comparable heating systems, with the rf system about 30% cheaper, (\$18 M for rf vs. \$24 M for neutral beam exclusive of power supplies). A large part of the cost advantage for rf systems is due to the fact that a significantly smaller building is required for the reactor.

A parallel study⁷ of LHRH, based on somewhat different assumptions, has tended to confirm the engineering feasibility of rf heating.

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A NEUTRAL BEAM SYSTEM FOR AN IGNITION TOKAMAK*

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Summary

We have attempted to make detailed designs of several neutral beam systems which would be applicable to a large machine, e. g., an ITR (Ignition Test Reactor), EPR (Experimental Power Reactor), or reactor. Detailed studies of beam transport to the reactor and neutron transport from the reactor have been made. We have also considered constraints imposed by the neutron radiation environment in the injectors, and the resulting shielding, radiation-damage, and maintenance problems. The effects of neutron heat loads on cryopanel and ZrAl getter panels have been considered. Design studies of power supplies, vacuum systems, bending magnets, and injector layouts are in progress and will be discussed.

Introduction

During the past year, we have considered neutral beam injector systems for an EPR and for TNS (The Next Step). In the EPR designs, 40 MW of 180 keV D^0 beam is produced by twelve injectors, with two beam lines per injector. In the TNS designs, 60 MW of 150 keV D^0 is produced by six injectors, with two beam lines per injector. Since the demands on ion sources are greater and the pumping requirements more severe for TNS (5 MW of D^0 per beam line) than for EPR (1.67 MW of D^0 per beam line), most of this discussion will be concerned with TNS injector systems.

Three design options requiring varying degrees of extrapolation beyond the state of the art for TFTR ion sources have been considered. Design 1 assumes a D^+ fraction of 75%, achieved in some present-day neutral injector ion sources, and relatively minor extrapolations in duty cycle and beam energy beyond the state of the art for TFTR ion sources. Design 2 assumes a D^+ fraction of 95%, a major extrapolation beyond the state of the art for TFTR sources. Design 3 is based on the presently remote possibility, given the current level of support, that a suitable 400 keV direct extraction D^- ion source will be developed in time to be of use for TNS. The design characteristics of the ion sources are given in Table I.

Designs 1 and 2 may be regarded as lower and upper limits to what might be achieved with sources of positive deuterium ions. Design 1, the present reference design, will be continually updated, as warranted by advances in the state of the art and by experimental determination of design parameters that can only be estimated at the present time. Design 2 represents a worthy but perhaps not quite attainable goal for further D^+ ion source development.

Preliminary Layout of a $D^+ \rightarrow D^0$ Beam Line

Designs 1 and 2 both call for six injectors with two beam lines per injector. Each beam line will have a positive ion source and accelerator, followed by a double-focusing bending magnet, a neutralizer for the atomic ions, and a second bending magnet to remove atomic ions from the beam emerging from the neutralizer. Direct energy converters will be used to recover energy from the molecular and atomic ions removed by the first and second separator magnets. Using data on the fringe fields due to the toroidal field coils, a preliminary layout of the $D^+ \rightarrow D^0$ beam line has been made and is shown in Fig. 1. For this layout, the fringe field at the exit of the neutralizer will be ~ 0.067 T. It is assumed that the volumes occupied by the D^+ , D_2^+ and D_3^+ beams and the two direct converters are shielded from stray magnetic fields but the problem of shielding these volumes without interfering with pumping and with the magnetic fields in the reactor has not yet been addressed.

For the ion sources of Designs 1 and 2, curved grids will be used in the source and accelerator to produce a convex plasma sheath and a diverging ion beam composed of diverging beamlets. The first, double-focusing bending magnet will convert the D^+ component of this diverging beam into a converging beam. Electron attachment in the neutralizer will convert about 31% of this ion beam into a converging D^0 beam which goes through a waist before it reaches the first wall of the reactor. The location of this waist and the beam size at the first wall are determined by the beam emittance, the initial beam divergence, and the bending angle of the D^+ beam. The dependence on bending angle is shown in Fig. 2 for Design 1.

D_2 gas is fed into the middle of the neutralizer at the rate of q_N torr-l/s. A fraction, α of this gas flows toward the first bending magnet; the remainder, flows toward the reactor. To minimize these gas flows, the neutralizer will be tapered to follow the converging ion and neutral beams. There is a trade-off between reduced beam size at the first wall and increased neutralizer size (and gas flow) as the D^+ bending angle is reduced from 90° to 72° (see Fig. 2).

D^+ , D^0 Beam Transport

For a given ion source and specified neutral beam power and energy, perhaps the most important parameters of a neutral beam injector system are the beam half-widths as functions of distance along the beam axis. They determine the sizes of neutralizers, beam ducts, and first wall penetrations (see Fig. 2). They also determine neutralizer gas loads, and, thus, gas line densities, beam losses, and neutral injector efficiencies. (Pumping speed in the magnet region is conductance limited to a value that will not permit

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arbitrary reduction in pressure and beam loss for a given gas load; as the gas load increases, losses increase.) Beam transport will be discussed briefly here and in more detail elsewhere in these proceedings.¹

Most of the transport calculations have been made by assuming that the bounding curves of the transverse (x, x' ; z, z') phase space areas of the beam emerging from the accelerator are or can be approximated by an ellipse whose area is given by

$$\epsilon = \frac{\text{phase space area}}{\pi} = \omega_0 y_0 \max \quad (y = x \text{ or } z) \quad (1)$$

where ϵ is the emittance, ω_0 is the maximum divergence of the on-axis beam, and $y_0 \max$ is the half-width of the beam; the subscript 0 refers to the exit grid of the accelerator.

Equation 1 gives the correct emittance for each of the beamlets emerging from a multiaperture accelerator exit grid; however, the phase space envelope that encompasses the entire beam from a multiaperture exit grid is a parallelogram and the emittance of the beam is

$$\epsilon = \frac{\text{phase space area}}{\pi} = \frac{4}{\pi} \omega_0 y_0 \max \quad (y = x \text{ or } z) \quad (2)$$

It is generally assumed that the density of particles in the phase space area between the parallelogram of Eq. 2 and the inscribed ellipse of Eq. 1 is small and that this area contains a negligibly small fraction of the entire beam. This assumption has made us a bit uneasy; we have, therefore, defined a new "effective" phase space ellipse that contains a larger fraction of the beam by letting

$$\omega_0 \rightarrow (4/\pi) \omega_0 \quad (3)$$

$$\beta_0 = y_0 \max / \omega_0 \rightarrow \beta_0 = y_0 \max / (4/\pi) \omega_0 \quad (4)$$

where β_0 is the initial value of an ellipse parameter β ; in terms of β and ϵ , the beam size at any distance from the source is given by

$$y_{\max} = (\beta \epsilon)^{1/2} \quad (5)$$

Beam sizes calculated by using Eq. 5 with $\epsilon = \omega_0 y_0 \max$ (Case I), $\epsilon = (4/\pi) \omega_0 y_0 \max$ (Case II), and single-particle point-to-point geometrical transport calculations (Case III) are given in Table II for TNS Design 1. We are left with the problem of whether we should try to squeeze the last possible particle through the neutralizer by designing it to accommodate the beam sizes of Case III or let it scrape off a (presumably) small fraction of the beam and design it for Case I or Case II beam sizes. Numerical studies not yet underway are expected to give reasonable estimates of the amounts of beam scraped off in the Case I and Case II designs. Trade-off studies to determine the best option will then be possible. If scrape-off losses are found to be excessive in the Case I and Case II designs and the losses due to charge changing and dissociative collisions in the gas streaming from

the neutralizer are also found to be excessively high in the Case III design, it will be necessary to reduce beam sizes by increasing the number of injectors and ion sources.

D^+ neutral beam injector design calculations (beam sizes, gas flows, beam losses, and efficiencies) using the geometrical approach of Case III have not yet been completed for Design 1 of Table I, nor have they been begun for Design 2.

Design 3

Design 3, for a D^- TNS neutral beam injector system, calls for six injectors with two beam lines per injector. Each beam line will have a direct-extraction D^- ion source, with the characteristics assumed in Table I, followed by a neutralizer and a bending magnet to separate D^- and D^+ ions from the D^0 beam energizing from the neutralizer; direct energy converters will be used to recover energy from the residual D^- and D^+ beams. A layout of the D^- beam line is shown in Fig. 3.

Curved grids will be used in the source and accelerator to produce a concave plasma sheath and a converging beam of square cross section. Electron stripping in the D_2 gas neutralizer will convert $\sim 59\%$ of the D^- beam into a D^0 beam whose size at the first wall depends on the emittance of the beam and the radius of curvature of the accelerator exit grid. The accelerator and the neutralizer are separated by a 1.0 m pumping gap to reduce the pressure in the accelerator and thus to minimize beam energy spread due to premature neutralization and grid loading due to ionization of gas in the accelerator. The Chamber I pressure is 1×10^{-4} torr and the pressure in Chamber II is 1×10^{-5} torr. Design 3 is summarized and compared with Designs 1 and 2 in Table III. Power and gas flow models for Designs 1, 2, and 3 are discussed in Reference 1.

Bending Magnet Design

A preliminary design of the bending magnet between the ion source and neutralizer (Fig. 1) has been completed. The constraints on this magnet are (1) bending radius $r = 1.5$ m, (2) clear aperture 0.5 m $\times$ 0.5 m, (3) field index, $n = \frac{R}{B} \frac{dB}{dR} = 0.5$, and (4) the magnet must be as open as possible for pumping, particularly along its outer circumference. Two approaches can be followed, shaping the pole face of the magnet, which results in large magnets, or pole face windings, which we have used. The resulting magnet (Fig. 4) uses currents along the boundaries of the beam region to define the magnetic field, and an iron return yoke to minimize the resulting fringe field. Current ratios of roughly (A: B: C = 4: -1: -3) give the desired field properties. The low field produced in this magnet (~ 500 G) does not require a massive iron yoke. Thus the pole faces and current carrying conductors could contain gaps which would provide a good conductance for vacuum pumping.

Neutral Beam Pumping

Two options for pumping have been considered; cryopumps and zirconium aluminum getter pumps.

Cryopumps

The refrigerant needs for neutral beam pumping are given in Table IV. Helium requirements are given in terms of liters of liquid nitrogen. The table reflects the requirements for one hour of operation of one injector. The 173 m² of cryopanel in each injector will operate at 4.2° K and will be shielded on both sides by 20° K panels. The 20° panels in turn will be shielded by 77° K panels.

The thermal load due to the neutron flux and the heat input from the gas load are calculated on a 75% duty cycle. Initial operation planned for a 10% duty cycle will reduce these two values by a factor of 7.5.

If the energy from the loss of beam in each injector were deposited on the pumping panel and its shields, the refrigerant requirements would increase by an enormous factor. It is, however, presumed that stray beam can be intercepted and cooled on strategically placed dumps. With this presumption, thermal loading from the neutron flux places the greatest demand upon the refrigeration. This factor alone enhances the desirability of using zirconium-aluminum getter panels for pumping the neutral beams.

Zirconium-Aluminum Pumps

Zirconium-aluminum getter panels develop high pumping speeds for active gases and especially for H₂. The panels provide a well-defined trapping volume of the gas, which is important in handling tritium, and they can be located as near to the gas source as possible. Operating experience with a zirconium-aluminum pump in the vicinity of a fission reactor core for three years shows satisfactory operation in neutron flux densities greater than expected in the vicinity of the neutral beams. Furthermore, thermal loading should have no adverse effects on performance since zirconium-aluminum pumps are normally run at temperatures of the order of 400° C to maintain active and efficient pumping surfaces in the presence of contaminants. Their disadvantages include the inability to desorb the active gases other than hydrogen when reactivated; extremely low pumping capacity for methane; and inability to pump the inert gases. For each injector, 115 m² of zirconium-aluminum getter panel will be required to realize the required pumping speed for deuterium.

Cryopanel would probably last as long as the life of the reactor. Zirconium-aluminum getter panels, on the other hand, have a limited life, the length of which is dependent upon the percentage of impurities, such as O₂, CO, and N₂, in the hydrogen. In Table V is some comparative data per injector, including lifetime and regeneration time, based on a 5 s injection period during each cycle.

The lifetime of the zirconium-aluminum panels is given in terms of either 1% O₂, CO, or N₂ contaminations. In practice, the neutralizer gas and fuel mixtures should have much lower contamination levels than we have assumed, making the zirconium-aluminum panel lifetime comparable to the (~ 5 years) life of the reactor.

It may be necessary to use cryopumps with the zirconium-aluminum panels to eliminate gases not pumped or poorly pumped by the panels. These cryopumps would be appendage pumps, however, and would represent, at most, a few percent of the total pumping. The combination of zirconium-aluminum panels and cryopumps still reflects a savings in initial investment and operating costs over the use of cryopumping only.

Regeneration

Table V reflects the need to regenerate the pumps under either mode of pumping. Unless provided for, regeneration would interrupt neutral beam operation. One way to avoid this would be to double the required amount of pumping panel and provide a means for isolating each half in turn when regeneration is necessary. The problem of providing reasonably vacuum tight valving to isolate hundreds of square meters of pumping panels appears staggering both in cost and complexity. If space is available, a very promising solution would be to provide one or two spare injectors. The spare injectors provide the means for rotating each injector in turn through the regeneration phase and thus eliminate the need for doubling the panel area and for panel isolation. The mode of operation will also minimize the regeneration pumping requirements, both in hardware and capacity. In addition, the spare injectors provide redundancy for on-line failures during an experiment.

Costs

There should not be much difference in initial costs between cryopanel and zirconium-aluminum getter panels. Since the cryopanel must be shielded, however, it is expected that more space within the injector will be required to accommodate cryopaneling. As the zirconium-aluminum getter panels must be operated at ~ 400° C, the thermal loading from the neutron flux and neutral beam losses becomes an asset rather than a detriment to pumping. Even if all external power were used to heat the getter panels, the cost to operate them (based on \$0.0191/kW h to operate the Argonne ZGS Accelerator) would be ~ \$9.50/h per injector. The power cost to operate liquifier/refrigerator equipment to supply refrigerant to the cryopanel per hour per injector would be approximately \$10.50. This figure does not reflect the high initial costs of the liquifier/refrigerator equipment. Costing this equipment separately for use with the cryopanel would probably not reflect true costs since the refrigerant needs would be lumped with the requirements for the superconducting coils and the toroidal system cryopumps, and all would be supplied from the same source.

Power Supply for Neutral Beam Source

The power system's composed of three major components:

1. A superconducting inductor which accumulates and stores the pulsed energy. This isolates the pulsed load from the utility grid.²

2. An inverter to generate 3-phase high-frequency power for a switched high voltage (HV) rectifier.
3. A saturated time delay transformer (STDT) and a longitudinal reactor (LR) which in conjunction with spark gaps and discharge resistors, protect the source during spark-down faults.⁴

Figure 5 is a diagram of the power supply system. It draws pulse energy from the superconducting energy storage inductor L_0 which is charged from the 60 Hz line between pulses. A dc/ac/dc converter provides precisely regulated dc power for high-frequency inverters. These inverters feed a summing transformer producing 3-phase, 12.5 kHz power. This power is filtered and applied through 3-phase solid state ac switches to an HV rectifier system. The HV output leads are connected to the ion source through an STDT.

In the event of a spark in the source, the STDT prevents appreciable energy transfer into the spark. The STDT immediately initiates removal of the ac input power to the HV rectifier and discharge of the energy stored in capacitor C_2 (the capacitance to ground of the arc and filament power supplies) which is discharged into R_2 via spark gap G_2 . Reactors LR limit the discharge of C_2 into the source to tolerable values.

Capacitor C_1 is discharged into R_1 via spark gap G_1 after a gap time delay (GTD) which is longer than the time required to deenergize the HV rectifier. In case the ac switches malfunction, a crowbar time delay (CTD) shorts the ac power before the delay time of the STDT has elapsed.

The neutral beam injector system provides 40 MW to the plasma for 6.5 s during startup. This requires an input power of 284 MW (e). Direct energy recovery of the molecular fraction of the beam provides 165 MW (e) recirculating power to the injector systems (see Fig. 1). Thus, 119 MW (e) additional power must be supplied to the injector system from the energy storage inductor.

Shielding

Nuclear analysis has been made on the duct penetration for the neutral beam injection. The reactor system modeled in the present analysis is illustrated in Fig. 6. The neutral beam port is represented by an open circular duct region that extends from the first wall through the blanket/bulk shields, passes in between a pair of toroidal-field (TF) coils, and leads to a beam injection chamber. The duct size is 4.5 m long and 0.5 m in diam. A 0.5 m thick local exterior penetration shield which consists of stainless steel (SS) or SS + 50% B_4C annular layers completely surrounds the beam duct. Radiation streaming effects through the penetrations were investigated in several regions: (1) cryopanel located parallel to the beam line inside the chamber, (2) the superconducting TF coils, and (3) the blanket/bulk shield regions in the vicinity of the beam ducts.

Figure 7 shows a nuclear heating rate along the beam duct and cryopanel walls as a function of distance from the first wall. The neutron wall loading assumed here is 1 MW/m^2 . The attenuation of the nuclear heating over the 4.5 m long duct is only ~ 3 orders of magnitude. This implies that in the neighborhood of the duct wall, special considerations similar to those of the first wall/blanket must be taken in all relevant technical areas such as thermal hydraulics, material selection, and mechanical engineering. Another implication of the high radiation streaming rate through the duct is that a special component shield circumscribing the beam injector chamber is needed to prevent direct radiation streaming into the reactor building.

Cryopanel in the vacuum pumping system must be protected against a high radiation heat load in order to provide for a practical low temperature cooling capability. The present calculation shows a maximum heating of $\sim 10^{-4} \text{ W/cm}^3$ in the cryopanel on the chamber side walls. This radiation level does not seem to cause any serious technological or economical problems relevant to the panel cooling.

Assembly

The beam injectors would be grouped in pairs, as shown in Fig. 8, with each injector having its own vacuum and power supplies, so that maintenance and conditioning of these devices could be as independent as possible. Each injector would be surrounded by a thick shield wall, and the shielding system would include some means of removing air from the high radiation environment. This is necessary because the Argon in air would become activated by the high neutron flux, complicating maintenance. Safe operation with tritium also imposes some constraints on the vacuum system of the injectors.

The cost of the injector system, exclusive of power supplies, has been estimated at about \$1.8 M per pair of injectors.

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TABLE 1
ION SOURCE DESIGN CHARACTERISTICS FOR TMS OR EPR

	Design 1	Design 2	Design 3
Beam Energy (keV)	150 or 180	150 or 180	400
Beam Composition (D^+ , D_2^+ , D_3^+)	(0.75, 0.18, 0.07)	(0.05, 0.03, 0.02)	95% D^+ assumed
Shape of Plasma Sheath	Convex	Convex	Concave
First Grid Transparency, τ_g	0.45	0.45	0.45
Plasma Ion Emission Current Density, j_0 (A/cm ²)	~ 0.300	~ 0.300	~ 0.178
Average Ion Beam Current Density at Last Grid, $j_+ \approx \tau_g \cdot j_0$ (A/cm ²)	0.135	0.135	0.080
Gas Efficiency, η_g	0.45	0.45	0.10
Pulse Length (s)	≥ 5	≥ 5	≥ 5

TABLE 3
60 MW NEUTRAL BEAM INJECTOR SYSTEM DESIGNS FOR TMS

	Design 1	Design 2	Design 3
Atomic Ion	D^+	D^+	D^+
Beam Composition (D^+ , D_2^+ , D_3^+)	(0.75, 0.18, 0.07)	(0.95, 0.03, 0.02)	95% D^+ assumed
Target for $D^+ \rightarrow D^0$	D_2 Gas	D_2 Gas	D_2 Gas
Number of Injectors	6	6	6
Number of Ion Sources/Injector	2	2	2
Ion Beam Current Density at Exit Grid (A/cm ²)	0.135 Avg.	0.135 Avg.	0.080 Avg.
Total Ion Beam Power, P_+ (MW)	294	222	108
Power Required to Produce the Ion Beam, $k P_+$ (MW)	323.5	244	119
Assumed k Value	1.1	1.1	1.1
Assumed Gas Efficiency	0.45	0.45	0.10
Gas Load/Inject $\dot{Q}_g$ (Torr A/s)	252	149	46.6
Injector Pumping Speed ($\dot{V}$)	8.65×10^6	8.65×10^6	9.24×10^5
Zr-Al Pumping Panel Area/Injection (m ²)	130	130	13.8
Net Power Input to Injectors (MW)	165	120	84
Electrical Power Efficiency	0.36	0.50	0.71
Pulse Length (s)	≥ 5	≥ 5	≥ 5

TABLE 2
SOME RESULTS OF D^+ , D^0 BEAM TRANSPORT CALCULATIONS

	Case I	Case II	Case III
Initial Beam Size			
2 y_0 max (m x m)	0.348 x 0.348	0.348 x 0.348	0.348 x 0.348
Initial Beam $\frac{1}{2}$ Angle Divergence, θ_0 (rad)	0.078	0.078	0.078
Maximum Initial Beamlet $\frac{1}{2}$ Angle Divergence, ω_0 (rad)	0.020	0.020	0.020
Emittance Definition	$\omega_0 y_0$ max	(4/3) $\omega_0 y_0$ max	Geometrical Transport
β_0 Definition	y_0 max/ ω_0	(4/3) y_0 max/ ω_0	Geometrical Transport
Beam Size at Neutralizer Entrance (m x m)	0.487 x 0.487	0.487 x 0.487	0.572 x 0.572
Beam Size at Neutralizer Exit (m x m)	0.330 x 0.330	0.344 x 0.344	0.472 x 0.472
Beam Size at First Wall (m x m)	0.306 x 0.306	0.390 x 0.390	0.304 x 0.304
Gas Flow Out of Neutralizer (forr l/s)	68.5	73.0	142

TABLE 4
REFRIGERANT CAPACITY PER INJECTOR PER HOUR OF OPERATION

	Watts at		Liters
	4.2 K	20 K	L/H ₂
Radiation Losses	0.2	69.0	397.0
Thermal Loads (from neutron flux) (a)	454.0	1593.0	36.0
Conduction Losses	30.0	179.0	0.9
Heat Input (from gas load)	12.0	45.0	< 0.1
Totals	496.2	1886.0	434.0

TABLE 5
COMPARATIVE DATA PER INJECTOR
FOR A 5-s INJECTION PERIOD DURING EACH CYCLE

	Chamber I	Chamber II
Regeneration Frequency (days)		
Cryopanel	5.47	14.12
Zr-Al panel (Reg. at 1×10^{-5} Torr) (using both sides of panel)	8.7	22.8
Panel Lifetime (years)		
Cryopanel	Life of Reactor	
Zr-Al panel (using both sides of panel)		
17 O ₂ contamination	2.2	5.7
17 CO, N ₂ contamination	0.44	1.2

Fig. 1. Preliminary layout of a $D^+ \rightarrow D^0$ TNS beamline. The bending angle is 72° .

Fig. 2. Location of beam waist versus D^+ bending angle, and beam half-widths at several locations. For TNS Design 1.

Fig. 3. Preliminary layout of a $D^- \rightarrow D^0$ TNS beam line.

Fig. 4. Preliminary design of $n = 1/2$ bending magnet.

Fig. 5. Schematic of the neutral beam injector power supply system.

NUCLEAR HEATING

Fig. 6. Neutral beam duct model for nuclear analysis.

Fig. 7. Nuclear heating rates versus distance from first wall.

Fig. 8. Vertical elevation showing EPR torus and neutral beam design.

Power and Gas Flow Models for Monoenergetic Neutral Beam Injectors *

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Summary

Large, ignition tokamak reactors (ITR, EPR, and beyond) will require supplemental heating to achieve ignition. In the earlier machines, at least, this heating will probably be provided by monoenergetic neutral beams. These beams, with energies ≥ 150 keV, will most likely be derived from D^+ or D^- ions produced by direct extraction ion sources. A positive ion source will be followed by a bending magnet, a neutralizer, and a second bending magnet. The first magnet will remove molecular ions, and the second one atomic ions. Direct converters will be used to recover energy from unused molecular and atomic ions. The first bending magnet may be omitted if D^- ion sources are used. Models have been developed for power and gas flow in injectors which employ direct extraction D^+ or D^- ion sources. The power flow model accounts explicitly for all beam losses in terms of line densities of gas along paths traversed by ions and neutrals and cross sections for dissociation and charge-changing collisions. The gas flow model uses the results of power flow calculations and known gas flows from sources and neutralizers to determine gas loads and pumping requirements in various parts of the injector.

Introduction

During the past year, we have considered a 40-MW, 180-keV neutral beam injector system for an experimental power reactor (EPR), and a 60-MW, 150-keV system for an ignition test reactor (ITR or TNS). Three design options, requiring varying degrees of extrapolation beyond the state of the art for TFTR ion sources, have been considered. The design characteristics of the ion sources are given in Table 1.

Neutral beam injector beam lines based on Designs 1 and 2 will have a positive ion source and accelerator followed by a double focusing bending magnet, a neutralizer for the atomic ions, and a second bending magnet to remove atomic ions from the beam emerging from the neutralizer. Direct energy converters will be used to recover energy from the molecular and atomic ions removed by the first and second separator magnets. It is assumed that the volumes occupied by the D^+ , D_2^+ , and D_3^+ beams and the two direct converters are shielded from stray magnetic fields, but the problem of shield-

ing these volumes without interfering with pumping and with the magnetic fields in the reactor has not yet been addressed.

For a given ion source and specified neutral beam power and energy, perhaps the most important parameters of a neutral beam injector system are the beam half-widths as functions of distance along the beam axis. They determine the sizes of neutralizers, beam ducts, and first-wall penetrations. They also determine neutralizer gas loads and, thus, gas line densities, beam losses, and neutral injector efficiencies.

To establish a frame of reference for discussions of power and gas flow models, and beam transport, a preliminary layout of a $D^+ + D^0$ beam line for TNS is shown in Fig. 1. (There are two beam lines per injector.)

Fig. 1. Preliminary layout of a $D^+ + D^0$ beam line for TNS. The D^+ bending angle is 72 deg.

For the positive ion sources, curved grids will be used in the source and accelerator to produce a convex plasma sheath and a diverging ion beam composed of diverging beamlets. The first, double-focusing bending magnet will convert the D^+ component of this diverging beam into a converging beam. Electron attachment in the neutralizer will convert about 31% of this ion beam into a converging D^0 beam which goes through a waist before it reaches the first wall of the reactor. The location of this waist and the beam size at the

TABLE 1. Ion Source Design Characteristics for TNS or EPR

	Design 1	Design 2	Design 3
Beam energy (keV)	150 or 180	150 or 180	400
Beam composition (D^+ , D_2^+ , D_3^+)	(0.75, 0.18, 0.07)	(0.95, 0.03, 0.02)	95% D^- assumed
Shape of plasma sheath	convex	convex	concave
First grid transparency, τ_g	0.45	0.45	0.45
Plasma ion emission current density, j_0 (A/cm ²)	~ 0.300	~ 0.300	~ 0.178
Average ion beam current density at last grid, $j_+ \approx \tau_g j_0$ (A/cm ²)	0.135	0.135	0.080
Gas efficiency, η_g	0.45	0.45	0.10
Pulse length (s)	≥ 5	≥ 5	≥ 5

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first wall are determined by the beam emittance, the initial beam divergence, and the bending angle of the D^+ beam. The dependence on bending angle is shown in Fig. 2 for Design 1.

Fig. 2. Location of beam waist versus D^+ bending angle, and beam half-widths at several locations, for Design 1.

D_2 gas is fed into the middle of the neutralizer at the rate of q_N Torr-l/s. A fraction, α , of this gas flows into Chamber I; the remainder flows into Chamber II. To minimize these gas flows, the neutralizer will be tapered to follow the converging ion and neutral beams. There is a tradeoff between reduced beam size at the first wall and increased neutralizer size (and gas flow) as the D^+ bending angle is reduced from 90 to 72 deg (see Fig. 2).

Power Flow Models for $D^+ \rightarrow D^0$ Injectors

The model for the TNS beam line layout of Fig. 1 is shown in Fig. 3. It is based on the ANL/EPR flow model,¹ which includes both electrical and thermal energy recovery; in the present model there is no thermal energy recovery.

In this model, P_+ is total ion beam power (not just that from one accelerator); $M-1$ is the first bending magnet; Γ_1 is the D^+ fraction of the deuterium ion beam; $e^{-\pi+\sigma_{10}}$ is the fraction of the D^+ beam which reaches the neutralizer, the remainder $(1 - e^{-\pi+\sigma_{10}})$ being lost due to reionization in a gas target of thickness π ; $e^{-\pi+\sigma_{10}}$ is the fraction of the D^+ beam from the neutralizer which reaches the second direct converter; and $F_{1\infty}$ is the D^- fraction (negligibly small) of the beam from the neutralizer. f_{10} is the design fraction of the neutral beam emerging from the neutralizer; it is given by

$$f_{10} = F_{0\infty} [1 - e^{-\pi N(\sigma_{01} + \sigma_{10})}], \quad (1)$$

where $F_{0\infty}$ is the equilibrium (thick target) fraction of D^0 . π_N has been chosen to give the design value $f_{10} = 0.95 F_{0\infty}$.

Written out, the summations in Fig. 3 are:

$$\Sigma(\sigma_{loss})_2 = \sigma_{D_2} + (1/2)(\sigma_D + \sigma_{D^+}) \quad (2)$$

$$\Sigma(\sigma_{loss})_3 = (1/3)(\sigma_D + \sigma_{D^+}) + (2/3)(\sigma_{D_2} + \sigma_{D^+}) \quad (3)$$

Values for the cross sections in Eqs. (2) and (3) can be obtained from McClure's measurements.² The term $(k-1)P_+$ represents the power going into the source filaments, the arc discharges and grid heating. The difference between the power going into the direct converters and electrical power coming out of them is assumed to be absorbed in the converters and does not appear as a load on the "heat sink"; i.e., beam dumps or injector walls.

Gas Flow Model for $D^+ \rightarrow D^0$ Injectors

In terms of pressure p (Torr), path length L (m) and gas temperature T (°K), the thickness of a gas target is given by

$$\pi = \frac{1.94 \times 10^{21}}{T} \int_0^L p dt \quad \text{atoms/cm}^2. \quad (4)$$

The pressure distributions along the paths travelled by atomic ions and neutrals and by molecular ions are not known and it is necessary to make estimates, some of

Fig. 3. TNS injector power flow diagram. π is a gas target thickness (atoms/cm²) for charge-changing collisions or dissociation. η_1 and η_2 are electrical energy recovery efficiencies (≤ 0.85).

them rather crude, of average pressures in various regions in order to calculate approximate losses and efficiencies.

The gas flows into and out of an ion source are given in Torr-l/s by

$$q_{in} = 0.095(i_+/n_g) \sum_1^3 j\Gamma_j; \quad q_{out} = (1 - n_g)q_{in}, \quad (5)$$

where n_g is the gas efficiency and Γ_j is the D_j^+ fraction of i_+ . Premature neutralization of D^+ in Chamber I results in a gas load per source

$$q_{10} = 0.095 \Gamma_1 i_+ (1 - e^{-\pi+\sigma_{10}}). \quad (6)$$

Recombination of D_2^+ , D_3^+ , and the fragments of their dissociation results in the gas loads per source

$$q_2 = 0.095 i_+ \times 2\Gamma_2; \quad q_3 = 0.095 i_+ \times 3\Gamma_3. \quad (7)$$

The total gas load to Chamber I from the two beam lines is given by

$$q_I = 2(q_{out} + q_{10} + q_2 + q_3 + \alpha q_N) \text{ Torr-l/s}, \quad (8)$$

where αq_N is the gas load to Chamber I from each neutralizer.

Recombination of D^+ on collecting surfaces in Chamber II results in a gas load per source

$$q_{10}^* = 0.095 \Gamma_1 i_+ (1 - 0.95 F_{0\infty} - F_{T\infty}) e^{-\pi+\sigma_{10}}, \quad (9)$$

where $F_{T\infty}$ is the equilibrium fraction of D^- . The design neutralizer efficiency is 95% of the equilibrium value. The gas load per source due to reionization of D^0 is

$$q_{01} = 0.095 \Gamma_1 i_+ f_{10} e^{-\pi+\sigma_{10}} (1 - e^{-\pi+\sigma_{001}}). \quad (10)$$

where $f_{10} = 0.95 F_{0\infty}$ is the neutralization efficiency. The gas load per source due to recombination of D^- on collecting surfaces is

$$q_- = \Gamma_1 i_+ F_{T\infty} (e^{-\pi+\sigma_{10}}). \quad (11)$$

The total gas load to Chamber II from two beam lines is given by

$$q_{II} = 2[q_{10}^* + q_{01} + q_- + (1 - \alpha)q_N] \text{ Torr-l/s}. \quad (12)$$

As was noted above, the pressure distributions along beam paths are not known and average pressures can only be estimated. The uncertainty is greatest in the region of the first bending magnet, M-1, where the pumping speed is conductance-limited to a value which will not permit arbitrary reductions in pressure and beam loss for a given gas load. A crude estimate of the room temperature value of the conductance, C, using an argument presented in an ANL/EPR study,¹ is $C \sim 1.7 \times 10^6$ l/s. This value was used to calculate beam losses in Chamber I.

D^+ , D^0 Beam Transport

In the most general case, the horizontal and vertical transfer matrices for beam transport from the source to the first wall in a beam line containing a bending magnet are products of six simpler transfer matrices that Livingood³ writes in the form

$$\begin{pmatrix} x_6 \\ x_6' \\ dp/p \end{pmatrix} = \begin{pmatrix} 1 & S & 0 \\ 0 & 1 & 0 \\ 0 & 0 & 1 \end{pmatrix} \begin{pmatrix} 1 & 0 & 0 \\ \frac{\tan u_2}{\rho} & 1 & 0 \\ 0 & 0 & 1 \end{pmatrix}$$

$$\times \begin{pmatrix} \cos \phi & \frac{\rho}{\delta} \sin \phi & \frac{\rho}{\delta^2} (1 - \cos \phi) \\ -\frac{\delta}{\rho} \sin \phi & \cos \phi & \frac{1}{\delta} \sin \phi \\ 0 & 0 & 1 \end{pmatrix} \times \begin{pmatrix} 1 & 0 & 0 \\ \frac{\tan u_1}{\delta} & 1 & 0 \\ 0 & 0 & 1 \end{pmatrix} \begin{pmatrix} 1 & S_1 & 0 \\ 0 & 1 & 0 \\ 0 & 0 & 1 \end{pmatrix} \begin{pmatrix} x_1 \\ x_1' \\ dp/p \end{pmatrix} \quad (13)$$

$$\begin{pmatrix} z_6 \\ z_6' \end{pmatrix} = \begin{pmatrix} 1 & S \\ 0 & 1 \end{pmatrix} \begin{pmatrix} 1 & 0 \\ -\frac{\tan u_2}{\rho} & 1 \end{pmatrix} \begin{pmatrix} \cos \psi & \frac{\rho}{\delta} \sin \psi \\ -\frac{\delta}{\rho} \sin \psi & \cos \psi \end{pmatrix} \times \begin{pmatrix} 1 & 0 \\ -\frac{\tan u_1}{\rho} & 1 \end{pmatrix} \begin{pmatrix} 1 & S_1 \\ 0 & 1 \end{pmatrix} \begin{pmatrix} z_1 \\ z_1' \end{pmatrix}. \quad (14)$$

In Eqs. (13-14), S_1 is the drift distance from the accelerator exit grid to the entrance face of the bending magnet; ρ is the radius of the orbit with momentum p ; u_1 is the angle at which the entrance face is slanted with respect to the perpendicular (radius) to the p orbit; u_2 is similarly defined for the exit face of the magnet; ϕ and ψ are given by

$$\delta \theta \equiv (1 - n)^{1/2} \theta \equiv \phi; \quad \epsilon \theta \equiv n^{1/2} \theta \equiv \psi \quad (15)$$

where θ is the bending angle of the magnet and

$$n = -(r/B) \text{ dB/dr} \quad (16)$$

is the field index of the magnet; and S is a drift distance measured from the exit face of the magnet. To first-order, axial (z -direction) motion in a bending magnet is not affected by changes in momentum and 2×2 matrices suffice to describe the axial motion.

For the beam line of Fig. 1, $n = 1/2$ and $u_1 = 0$; for the monoenergetic D^+ component of the positive ion beam, $u_2 = 0$ and $\delta p/p = 0$. Thus, for the D^+ beam and the D^0 beam derived from it, Eqs. (13) and (14) reduce to

$$\begin{pmatrix} y \\ y' \end{pmatrix} = \begin{pmatrix} A & B \\ C & D \end{pmatrix} \begin{pmatrix} y_0 \\ y_0' \end{pmatrix} \quad (y = x \text{ or } z) \quad (17)$$

where the subscript 0 refers to the beam at the accelerator exit grid and

$$A = \cos \phi_1 - \left(\frac{\delta}{\rho_1} \sin \phi_1 \right) S \quad (18)$$

$$B = S_1 \cos \phi_1 + \frac{\rho_1}{\delta} \sin \phi_1 + \left(\cos \phi_1 - \frac{\delta}{\rho_1} S_1 \sin \phi_1 \right) S \quad (19)$$

$$C = -(\delta/\rho_1) \sin \phi_1 \quad (20)$$

$$D = \cos \phi_1 - (\delta/\rho_1) S_1 \sin \phi_1 \quad (21)$$

$$\phi_1 = \delta \theta = \sqrt{0.5} \theta. \quad (22)$$

Equation (17) has been used recently to calculate beam sizes at various distances S from the exit face of the bending magnet of Fig. 1. However, most of the calculations have been made by assuming that the bounding curves of the transverse (x, x' ; z, z') phase space areas of the beam emerging from the accelerator are or can be approximated by ellipses whose equations are given by the Courant-Snyder invariant⁵, and then using the geometrical properties of these ellipses to calculate beam sizes. The Courant-Snyder invariant can be written in the form

$$\gamma_0 y_0^2 + 2 \alpha_0 \beta_0 y_0 y_0' + \beta_0 y_0'^2 = \epsilon \quad (23)$$

where the subscript 0 refers to the exit grid of the accelerator,

$$\gamma_0 = (1 + \alpha_0^2) / \beta_0 \quad (24)$$

and ϵ , the transverse emittance of the ion beam, is the phase space area (area of the ellipse) divided by π . Using the notation of Fig. 4, which gives the geometrical properties of the phase space ellipse, and the theorem of Apollonius, we may write

$$\epsilon_y = \frac{\text{phase space area}}{\pi} = \omega_0 y_0 \max \quad (y = x \text{ or } z) \quad (25)$$

$$\begin{aligned} y_1 &= \sqrt{\epsilon/\gamma} & y_3 &= -a\sqrt{\epsilon/\gamma} \\ y_2 &= \sqrt{b\epsilon} = y_{\max} & y_3' &= \sqrt{\epsilon/\gamma} \\ y_1' &= -a\sqrt{\epsilon/b} & y_4' &= \sqrt{\epsilon/b} = \omega_0 \end{aligned}$$

Fig. 4. Geometric properties of the phase space ellipse (see Section 3.4 of Ref. 4).

Equation (17) can be solved for y_0 and y_0' ; these values may be substituted into Eq. (23) and the result written in the form

$$\gamma y^2 + 2 \alpha \beta y y' + \beta y'^2 = \epsilon \quad (26)$$

where the Twiss parameters⁵ α , β , and γ at a location downstream of the accelerator exit grid are given by

$$\begin{pmatrix} \alpha \\ \beta \\ \gamma \end{pmatrix} = \begin{pmatrix} (AD + BC) & -AC & -BD \\ -2AB & A^2 & B^2 \\ -2DC & C^2 & D^2 \end{pmatrix} \begin{pmatrix} \alpha_0 \\ \beta_0 \\ \gamma_0 \end{pmatrix} \quad (27)$$

At the plane where Eq. (26) applies, the beam half-width is given by

$$y_{\max} = \sqrt{\beta \epsilon} \quad (28)$$

where β is given by Eq. (27).

Equation (25) gives the correct emittance for each of the beamlets emerging from a multiaperture accelerator exit grid. For the on-axis beamlet emerging from a planar exit grid, the phase space ellipse is oriented as shown in Fig. 5a. It may be noted that the maximum beamlet divergence, ω_0 , occurs at the beamlet axis ($y = 0$) while the divergence at the beam edge ($y = y_{\max}$) is zero. The phase space envelope that encompasses the entire beam from the multiaperture exit grid is shown in Fig. 5b, where $y_{\max}$ is now the half width of the entire beam. Except at the edges of the outermost apertures, $y' = \omega_0 \neq 0$ and the phase space diagram is (very nearly) a rectangle of area $4\omega_0 y_0 \max$. Thus, the emittance of the entire beam is

$$\epsilon_y = \frac{\text{phase space area}}{\pi} = \frac{4}{\pi} \omega_0 y_0 \max \quad (y = x \text{ or } z) \quad (29)$$

Fig. 5. Phase space area of a single beamlet (a) and rectangular phase space envelope of the entire beam from a multiaperture source (b).

It is more convenient to obtain beam half-widths by using Eq. (29) than by using Eq. (17), the geometrical transport equation. The assumption usually made by accelerator physicists is that the density of particles in the phase space area between the rectangle and the inscribed ellipse of Fig. 5b is small and that this area contains a negligibly small fraction of the entire beam. In the present instance, where injector efficiency is of prime importance, this assumption has made us a bit uneasy; we have, therefore, defined a new "effective" phase space ellipse that contains a larger fraction of the beam by letting

$$\omega_0 \rightarrow (4/\pi)\omega_0, \quad \beta_0 = y_0 \max / \omega_0 + \beta_0 = y_0 \max / ((4/\pi)\omega_0) \quad (30)$$

The area of this ellipse is given by Eq. (29). Beam sizes calculated by using Eq. (28) with $\epsilon = \omega_0 y_0 \max$ (Case I), $\epsilon = (4/\pi)\omega_0 y_0 \max$ (Case II), and Eq. (17) (Case III) are given in Table 2. We are left with the problem of whether we should try to squeeze the last possible particle through the neutralizer by designing it to accommodate the beam sizes of Case III or let it scrape off a (presumably) small fraction of the beam and design it for Case I or Case II beam sizes. Numerical studies not yet underway are expected to give reasonable estimates of the amounts of beam scraped off in the Case I and Case II designs. Trade-off studies to determine the best option will then be possible. If scrape-off losses are found to be excessive in the Case I and Case II designs and the losses due to charge changing and dissociative collisions in the gas streaming from the neutralizer are also found to be excessively high in the Case III design, it will be necessary to reduce beam sizes by increasing the number of injectors and ion sources.

For the geometrical transport calculations (Case III), the fractions of the beam widths contained in the calculated beam sizes are given by

$$2 \operatorname{erf}(\omega_0 / \omega_{\text{RMS}}) - 1 \quad (31)$$

where

$$\operatorname{erf}(x) = \frac{1}{\sqrt{2\pi}} \int_{-\infty}^x e^{-t^2/2} dt \quad (32)$$

The value of $\omega_0 = 0.020$ rad was chosen to given an estimated value $\omega_0 / \omega_{\text{RMS}} \geq 3$ so that $\geq 99.7\%$ of the total beam

TABLE 2. Some Results of D^+ , D^0 Beam Transport Calculations

	Case I	Case II	Case III
Initial beam size, $2 y_{0\max}$ (m $\times$ m)	0.348×0.348	0.348×0.348	0.348×0.348
Initial beam 1/2 angle divergence, Ω_0 (rad)	0.078	0.078	0.078
Maximum initial beamlet 1/2 angle divergence, ω_0 (rad)	0.020	0.020	0.020
Emittance definition	$\omega_0 y_{0\max}$	$(4/\pi) \omega_0 y_{0\max}$	Geometrical transport
β_0 definition	$y_{0\max}/\omega_0$	$(\pi/4) y_{0\max}/\omega_0$	Geometrical transport
Beam size at neutralizer entrance (m $\times$ m)	0.487×0.487	0.487×0.487	0.572×0.572
Beam size at neutralizer exit (m $\times$ m)	0.330×0.330	0.344×0.344	0.472×0.472
Beam size at first wall (m $\times$ m)	0.306×0.306	0.390×0.390	0.304×0.304
Gas flow out of neutralizer (Torr-l/s)	68.5	73.0	142

width is included in the calculated beam width. For the calculations of Cases I and II, the included fractions are lower by presently unknown amounts.

D^+ neutral beam injector design calculations (beam sizes, gas flows, beam losses and efficiencies) using the geometrical approach of Case III have not yet been completed for Design 1 of Table 1, nor have they begun for Design 2.

Design 3

Design 3, for a D^- TNS neutral beam injector system, calls for six injectors with two beam lines per injector. Each beam line will have a direct-extraction D^- ion source, with the characteristics assumed in Table 1, followed by a neutralizer and bending magnet to separate D^- and D^+ ions from the D^- beam emerging from the neutralizer; direct energy converters will be used to recover energy from the residual D^- and D^+ beams. A layout of the D^- beam line is shown in Fig. 6.

Fig. 6. Preliminary layout of a $D^- \rightarrow D^0$ TNS beam line.

Curved grids will be used in the source and accelerator to produce a concave plasma sheath and a converging beam of square cross section. Electron stripping in the D_2 gas neutralizer will convert ~59% of the D^- beam into a D^0 beam whose size at the first wall depends on the emittance of the beam and the radius of curvature of the accelerator exit grid. The accelerator and the neutralizer are separated by a 1.0-m pumping gap to reduce the pressure in the accelerator and thus to minimize beam energy spread due to premature neutralization and grid loading due to ionization of gas in the accelerator. The Chamber I pressure is 10^{-4} Torr; that of Chamber II is 10^{-5} Torr.

The power flow diagram for Design 3, shown in Fig. 7, is an obvious extension of the flow diagram in Fig. 3. f_{-} goes through a maximum as the target thickness

is varied through an optimum value. D'yachkov⁶ gives an expression for $(f_{T0})_{\max}$:

$$(f_{T0})_{\max} = \frac{(\sigma_{T0})}{(\sigma_{01})} \left(\frac{\sigma_{01}}{\sigma_{T0} + \sigma_{T1}} \right) \frac{\sigma_{T0} + \sigma_{T1}}{\sigma_{T0} + \sigma_{T1} - \sigma_{01}}. \quad (33)$$

The maximum occurs for an optimum target thickness

$$\pi_{\text{opt}} = \frac{1}{\sigma_{T0} + \sigma_{T1} - \sigma_{01}} \ln(\sigma_{T0} + \sigma_{T1})/\sigma_{01}. \quad (34)$$

Values of the cross sections appearing in these equations, taken from a recent compilation,⁷ are: $\sigma_{T0} = 1.3 \times 10^{-16}$ cm²/atom; $\sigma_{T1} = 8.5 \times 10^{-18}$ cm²/atom and $\sigma_{01} = 3.5 \times 10^{-17}$ cm²/atom for a 400-keV beam energy. With these values, Eqs. (33) and (34) give $(f_{T0})_{\max} = 0.59$ and $\pi_{\text{opt}} = 1.3 \times 10^{-5}$ atoms/cm². This value of π_{opt} gives a neutralizer line density of 2.0×10^{-3} Torr m at 293°K.

The curvature of the n -th (exit) grid of the accelerator, R_n , was chosen equal to the distance from the source to the first wall (11.0 m for the design of Fig. 6). This choice gives the smallest beam size at the first wall but larger values of beam size at the entrance and exit of the neutralizer than would be obtained if R_n were chosen to have the value $R_n = y_{0\max}/\omega_0$, where $y_{0\max}$ is the initial beam half width and ω_0 is the maximum initial beamlet one-half angle divergence. For the latter choice of R_n , the initially most divergent part of the beam is parallel to the beam axis and the beam size remains constant and equal to the initial size over the region $0 \leq S \leq R_n$ where S is distance along the beam axis from the accelerator exit grid. (See Fig. 8.) Since the neutralizer gas flow is quite small even in the worst case ($R_n = 11.0$, untapered neutralizer size equal to beam size at exit) there is no need for a trade-off between beam size at the first wall and neutralizer size.

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The discussion of D^+ and D^0 transport has its origins in unpublished notes⁸ and illuminating discussion with T. K. Khoe of the Accelerator Research Facilities Division, Argonne National Laboratory.

Fig. 7. Power flow diagram for Design 3. $e^{-\pi^* \sigma_{10}}$ is the fraction of the D⁻ beam from the neutralizer which reaches the D⁻ direct converter.

Fig. 8. The neutralizer width is a minimum when $R_n = y_{0\max}/\omega_0 = 6.99$ m (a). The beam width at $S = 11$ m is a minimum when $R_n = S = 11$ m (b).

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PROTECTION OF NEUTRAL-BEAM-ACCELERATOR ELECTRODES FROM SPARK DISCHARGES*

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Summary

The high-voltage (HV) electrodes of neutral beam sources (NBS's) must be protected from occasional sparks to ground. Spark currents can be limited with special transformers and reactors which introduce time delays that are long enough to quench the spark or to disconnect the energy source. A saturated time delay transformer (STDT) connected in series with the HV power supply detects spark faults and limits the current supplied by the power supply and its capacitance to ground; it also initiates spark quenching. Nonsaturated, longitudinal reactors limit the discharge current supplied by the energy stored in the circuit capacitance of the NBS filament and arc power supplies long enough to discharge this capacitance into a resistor. The design principles of these protective circuits are presented in this paper.

Introduction

Some electrodes of a NBS operate at voltages of up to 200 kV and must be protected from damage by occasional sparks to ground. Sparks are sustained by the HV power supply and by the energy stored in the circuit capacitance to ground.

If a high series impedance is designed into the HV power supply, a shunt regulator can limit the spark current supplied from the power source.¹ However, this makes the power source much more expensive and does not reduce by much the spark current due to circuit capacitance. As an alternative, conventional HV power supplies can be used in conjunction with fast switch tubes connected in series with the NBS. Figure 1 illustrates such a circuit arrangement. The switch tube limits the spark current and interrupts it within a few microseconds. Unfortunately, at very high potentials, switch tubes may themselves be subject to damage from sparking. When this happens, the spark current, which may also damage the NBS, will be sustained until the crowbar shorts the power source.

Destructive sparks can also be sustained by the energies stored in the circuit capacitance. These are shown as C_1 , C_2 , C_2' , and C_3 in Fig. 1. The current magnitudes of these capacitor discharges are limited only by the impedance of the discharge paths.

It has been proposed¹ to absorb the capacitively stored energy with eddy-current losses in magnetic cores. For this purpose, the HV leads going to the NBS are threaded through the holes of toroidal ferromagnetic cores. The resistance introduced by core losses of this one-turn inductor is connected in series with the NBS. The number of cores and their electromagnetic properties are selected empirically

to match the NBS. This method of arc attenuation is inefficient because magnetic core materials are formulated to keep eddy-current losses small. It also is not very practical, especially at high energies and voltages, where the core dimensions can become unwieldy. The eddy-current losses should limit the capacitive spark current to a nondestructive magnitude. For example, for a NBS operating at 120 kV and 65 A it would be desirable to limit the spark current to ≤ 200 A. This would call for a resistance value, generated by core losses, of $\geq 600 \Omega$. With a circuit capacitance of 4.5 nF (~ 32 J), the discharge time constant would be 2.7 μ s and the spark would be quenched after approximately 3 time constants ($\sim 8 \mu$ s). It may be impractical to build a magnetic-core assembly capable of generating eddy-current losses equivalent to $\geq 600 \Omega$ for 8 μ s when a step voltage of 120 kV is applied to its one-turn winding. During these 8 μ s the inductance of this core assembly must also be large enough to limit the inductive current to a small fraction of 200 A.

So far, tests to dissipate capacitively stored energies by this method have been made at relatively low voltages (≤ 40 kV) and with energies of ≤ 1.4 J.^{2,3} This energy is not much greater than the energy limit of 1J above which spark damage has been observed on a 40 kV NBS.⁴ Therefore, it is difficult to extrapolate from the tests data for the design of core assemblies for applications where energies of ≤ 40 J, at ≤ 200 kV, must be dissipated. Initially the toroidal cores were to protect only the arc and filament circuits, and the proposed circuit did not provide a core reset winding; the cores were operating on a minor hysteresis loop with correspondingly smaller eddy-current losses. Later,² the HV cable to the NBS was also threaded through the toroidal cores and a reset winding was added to compensate for magnetization by the normal NBS current. However, this reset winding was in the form of an air choke wound over the o.d. of the core stack. It produced a magnetic field which was at right angle with the field caused by the NBS current and, therefore, could not compensate for the magnetization by the NBS current.

Fig. 1. Conventional Power Supply System for NBS

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This paper describes circuits that limit NBS spark currents to tolerable values for the duration of magnetic time delays. The circuits initiate disconnection of the power source and provide means to efficiently discharge the capacitively stored energy into HV resistors. This eliminates most of the problems mentioned above and permits a relatively simple design of the HV power supply.

Protection from HV Power Source

In the simplified circuit of Fig. 1, capacitor C_3 represents the capacitance of the HV terminal of the NBS. It may have a typical value of 100 pF, which, at $E = 120$ kV, corresponds to a stored energy of 0.72 J. The capacitance of the HV cable and switch tube is approximately $C_1 \approx 3$ nF (≈ 22 J). In case of a spark in the NBS, capacitors C_1 and C_3 discharge through and damage the NBS. The switch tube limits the fault current from the HV power supply to values that are about 1.3 times the normal current. This fault current is interrupted by the switch tube within a few microseconds. In case the switch tube sparks over, the fault detector (FD) energizes the crowbar. The crowbar short circuits the power source and opens the circuit breaker (CB) of the commercial power line via current transformer CT_2 . An NBS can be protected from overcurrent supplied by the HV power supply and by the charge on capacitor C_1 with an STDT.^{4,5} The primary winding n_1 is connected in series with the load, as illustrated in Fig. 2. A secondary winding n_2 carries a dc bias current i_2 , which magnetizes the core to saturation. This is shown in Fig. 3 by point

Fig. 2. NBS Protected Against Spark Damage by STDT, Reactors, and Discharge Circuits

Fig. 3. Simplified B-H Curve for the STDT Core Material

R. The normal NBS current i_1 reduces the core saturation to point S, which is still in saturation but near the knee of the B-H curve. A spark causes the primary current to increase, bringing the core out of saturation. When this occurs, the magnetizing inductance L of the primary winding limits the rate of current increase to $di/dt \leq E/L$ while the magnetization shown in Fig. 3 changes from point T to U. The associated flux change induces in the transformer windings a voltage that is used to initiate remedial action. The time in seconds required for the flux to change from point T to point U of Fig. 3 is given by

$$\Delta t_1 = \frac{n_1 A \Delta B}{E} 10^{-8}, \quad (1)$$

where

A = effective core cross section, in cm^2 ,

ΔB = change in flux density between points T and U, in G, and

E = voltage applied to n_1 .

During the time interval Δt_1 , the spark current increases by Δi_1 , as shown in Fig. 3. The STDT can be designed to limit this increase to be a fraction of the normal NBS operating current i_1 .

In the circuit of Fig. 2, when the NBS sparks, the voltage induced in auxiliary winding n_3 blocks the switch tube (BST). After the switch tube has interrupted the current from the power supply, spark gap G_1 is triggered via a gap-time-delay (GTD) circuit. Capacitor C_1 discharges through resistor R_1 and through the inductance of the STDT.

With C_1 discharged the spark is quenched and the voltage across the transformer primary is zero. The core of the STDT resets to point S of Fig. 3 via points Y, Z, and X.

With the spark current limited by the STDT to a value only slightly larger than the normal NBS current, the switch tube should be able to interrupt the spark current reliably. However, if the tube fails to interrupt the current, a crowbar time-delay (CTD) circuit energizes the crowbar before the time delay Δt_1 of the STDT has elapsed. This action removes the power source while the fault current is still limited.

Equivalent Discharge Circuit

The discharge currents of C_1 are analyzed with reference to the equivalent circuit shown in Fig. 4a.

Fig. 4. Equivalent circuits for the capacitor discharge

With capacitor C charged to voltage V and for oscillatory circuit conditions, the currents are

$$i_R = \frac{V}{R} e^{-\alpha t} \left(\cos \omega t - \frac{\alpha}{\omega} \sin \omega t \right), \quad (2)$$

$$i_L = \frac{V}{\omega L} e^{-\alpha t} \sin \omega t, \quad (3)$$

and

$$i_C = i_R + i_L = \frac{V}{R} e^{-\alpha t} \left(\cos \omega t + \frac{R - \alpha L}{\omega L} \sin \omega t \right) \quad (4)$$

where

$$\alpha = \frac{1}{2RC}, \quad \omega = \left(\frac{1}{LC} - \alpha^2 \right)^{1/2}.$$

The peak current through the inductor is reached at the point of zero slope for the current described by Eq. 3. To solve for the time t_p of maximum current, we differentiate Eq. 3 and set the result to zero:

$$\frac{di_L}{dt} = \frac{V}{\omega L} e^{-\alpha t} (\omega \cos \omega t_p - \alpha \sin \omega t_p) = 0. \quad (5)$$

Solving Eq. 5 for the time t_p results in

$$t_p = \frac{1}{\omega} \tan^{-1} \frac{\omega}{\alpha}. \quad (6)$$

This multivalued function gives the various times corresponding to both the maximum and minimum points. The current reaches its maximum at a point before $\omega t = \pi/2$.

With $\alpha^2 > \frac{1}{LC}$, the resistor overdamps the circuit, and the above equations become

$$i_R = \frac{V}{R} e^{-\alpha t} \left(\cosh \beta t - \frac{\alpha}{\beta} \sinh \beta t \right), \quad (7)$$

$$i_L = \frac{V}{\beta L} e^{-\alpha t} \sinh \beta t. \quad (8)$$

$$i_C = i_R + i_L = \frac{V}{R} e^{-\alpha t} \left(\cosh \beta t + \frac{R - \alpha L}{\beta L} \sinh \beta t \right), \quad (9)$$

and

$$t_p = \frac{1}{\beta} \tanh^{-1} \frac{\beta}{\alpha} \quad (10)$$

where

$$\beta = \left(\alpha^2 - \frac{1}{LC} \right)^{1/2}.$$

The circuit is critically damped when $\omega = \beta = 0$. This is the case for

$$R = 0.5(L/C)^{1/2}. \quad (11)$$

Pulse Permeability of Core Material

The B-H curve of Fig. 3 is a dc hysteresis loop. It can be realized with grain-oriented 50% nickel-iron core material, e.g. Deltamax. However, less

expensive grain-oriented silicon steel, e.g. Silectron, is satisfactory for most applications.

The STDT must provide both a time delay Δt_1 (Eq. 1), and an inductance L that limits the current i_L (Eqs. 3 or 8) to tolerable values.

When a step voltage is applied to the STDT, eddy-currents will be generated in the core laminations. They will prevent magnetic flux from penetrating the whole core area right away. In time the eddy-currents decay and the area containing flux increases. For this reason the value of Δt_1 will be smaller than is calculated from Eq. 1. It is customary to express this reduction in useful cross section for the flux by means of an effective pulse permeability

$$\mu_p = B/H \mu_0 \quad (12)$$

where

B = induction in G,

H = magnetizing force, A cm⁻¹, and

μ_0 = permeability of space, $4\pi \times 10^{-9}$ H cm⁻¹.

The solid lines in Fig. 5 show published magnetization curves for 2 mil Silectron laminations. Similar data has not been published for Deltamax laminations. Therefore, the B-H curves shown for 1 mil Deltamax have been derived from the Silectron curves as described below.

Fig. 5. Pulse Magnetization Curves for 2 mil Silectron and 1 mil Deltamax.

The peak dc permeability of Deltamax laminations is about 7 times larger than that of Silectron. When these materials are pulsed under conditions where the flux distribution over their cross sections is identical, then the pulse permeability of Deltamax should also be 7 times larger ($\mu_{pD} = 7 \mu_{pS}$).

The depth where the magnetic field has a value of e^{-1} of the field on the surface of a core lamination is

$$\sigma = \left(\frac{\rho}{\pi f \mu} \right)^{1/2} \quad (13)$$

where ρ = resistivity, $\mu\Omega$ cm,
 f = frequency, s^{-1} .

To achieve the same flux distribution in laminations of Deltamax and Silectron their respective ratios of lamination thickness d to skin depth σ must be the same.

$$\frac{d_D}{\sigma_D} = \frac{d_S}{\sigma_S} \quad (14)$$

The resistivities of Deltamax and Silectron are $\rho_D = 45 \mu\Omega$ cm and $\rho_S = 50 \mu\Omega$ cm. Finally, from Eqs. 13 and 14 we find the lamination thickness of Deltamax d_D which has a pulse permeability 7 times that of 2 mil Silectron.

$$d_D = d_S \frac{\sigma_D}{\sigma_S} = d_S \left(\frac{\rho_D \mu_{PS}}{\rho_S \mu_{PD}} \right)^{1/2}$$

$$= 2 \text{ mil} \frac{45}{7 \times 50}^{1/2} = 0.72 \text{ mil}.$$

By dividing the H-values of the 2 mil Silectron curves in Fig. 5 by a factor 7, we obtain H-values applicable to Deltamax laminations 0.72 mil thick for $B \leq 14$ kG. To correct for the additional eddy currents in 1 mil as compared to 0.72 mil Deltamax laminations, a factor of $7 \times 0.72 = 5$ was used to obtain the curves for 1 mil Deltamax shown in Fig. 5.

From the curves of Fig. 5 we can calculate μ_p and with it the inductance of the primary winding n_1 of the STDT from

$$L = \frac{n_1^2 \mu_0 A}{l_g + l_{Fe}/\mu_p} \quad (15)$$

where

l_{Fe} = length of flux path in iron, cm

l_g = length of flux path in air, cm.

Protection from Capacitive Stored Energy of Arc and Filament Power Supplies

Figure 1 shows the capacitive discharge currents i_{C_2} and i_{C_2}' of the arc and filament power supplies. They are superimposed on the circulating arc current i_A and filament current i_F as shown. In most cases, capacitors C_2 and C_2' are effectively connected in parallel. This is illustrated by combining them to one capacitor, $C_2 \approx 1.5$ nF (~ 10 J) in the circuit of Fig. 2.

To limit the capacitive discharge current when the NBS sparks, longitudinal reactors L_1 and L_2 have been added to the circuit. A dc bias winding resets the core of these reactors to a point similar to point X of Fig. 3. The reactor windings are wound bifilar to have negligible inductance in the metallic circuits of the load currents i_A and i_F . The longitudinal circuits, which are completed through capacitance C_2 and ground, carry capacitor discharge currents $i_{C_2L_1}$

and $i_{C_2L_2}$. These currents are limited by the magnetizing impedance of the reactors. The reactors are designed for a time delay Δt_2 that is larger than the time required to discharge C_2 .

When the NBS sparks, a signal from the STDT initiates triggering of spark gap G_2 causing C_2 to discharge into R_2 and through L_1 and L_2 . L_1 and L_2 are dimensioned to keep these discharge currents within the rating of the NBS.

Arc Current Due to C_3

Locating the STDT and the reactors as close as practicable to the NBS terminals holds the value of C_3 to a minimum. The electrodes of the NBS should be designed to withstand the discharge of C_3 (≤ 1 J). Relatively small ferrite cores could be added to the NBS terminals to inductively limit the peak current of this discharge.

Design Considerations

The STDT Circuit

With a typical duty cycle of 1/30 for the HV power supply the rms current of the NBS in the circuit of Fig. 1 is only 12 A. The primary of the STDT can be wound with relatively small wire and with a relatively large number of turns. Therefore, we can use the lower permeability Silectron core material which is less expensive and less sensitive to strain and temperature than Deltamax. To fully realize the magnetic capabilities of a given material requires a core geometry having a minimum air gap, such as tape-wound toroids. Gapped structures reduce the unsaturated core permeability. This is illustrated by the dc B-H curves of the Silectron core assembly, with and without a gap, as shown in Fig. 6. The low pulse permeability of Silectron of $\mu_p \approx 600$ makes the core assembly insensitive to small air gaps. A gapped core is preferred because it permits using low-cost coil-winding techniques.

Fig. 6. Conceptual Design for an STDT and DC Hysteresis Curves for Silectron Cores

Figure 6 shows the conceptual design for an STDT for a 120 kV, 65 A load. The core cross section of 109 cm^2 is made up from eight C-cores each wound from 1 in wide and 2 mil thick Sillectron tape. The switch tube interrupts fault currents in less than $3 \mu\text{s}$. In order to keep Δt_1 small we discharge C_1 in about $1 \mu\text{s}$. This requires a discharge resistor of about $R_1 \leq 1 \mu\text{s} / 3 \times 3 \text{ nF} \leq 111 \Omega$. The time delay of the STDT should be about $6 \mu\text{s}$. From Eq. 1 and with $\Delta B \approx 28 \times 10^3 \text{ Vs cm}^{-2}$ we require

$$n_1 \geq \frac{\Delta t_1 E 10^8}{A \Delta B} = \frac{6 \mu\text{s} 120 \text{ kV} 10^8}{109 \text{ cm}^2 28 \text{ k Vs cm}^{-2}} = 24 \text{ turns.}$$

With a pulse permeability of $\mu_p \approx 600$ the inductance L of this winding is from Eq. 15

$$L = \frac{24^2 \times 1.26 \times 10^{-8} \text{ H cm}^{-1} 109 \text{ cm}^2}{(0.005 + 95/600) \text{ cm}} = 4.84 \text{ mH.}$$

Since $R_1 = 111 \Omega < 0.5 (4.84 \text{ mH} / 3 \text{ nF})^{1/2} = 635 \Omega$, the circuit is overdamped. From Eq. 7 the discharge current through resistor R_1 is

$$i_R = 1081 \text{ A } e^{-1.5 \times 10^6 t} (\cosh 1.48 \times 10^6 t - 1.016 \sinh 1.48 \times 10^6 t).$$

The discharge current of C_1 which flows through the STDT and the NBS is, from Eq. 8,

$$i_L = 16.8 \text{ A } e^{-1.5 \times 10^6 t} \sinh 1.48 \times 10^6 t.$$

Its maximum occurs, from Eq. 10, at time $t_p = 0.881 \mu\text{s}$ with a value of 4.8 A .

The Longitudinal Reactors

The arc and filament currents are large, requiring large conductor cross sections. Therefore, a one-turn design as shown in Fig. 7 is recommended for most applications. In order to keep the core area small, the higher permeability Deltamax core material should be used without an air gap. In addition, the discharge of C_2 should be as fast as practicable. For example, we could discharge $C_2 \approx 1.5 \text{ nF}$ in about $0.5 \mu\text{s}$ with a resistor of $R_2 \leq 0.5 \mu\text{s} / 3 \times 1.5 \text{ nF} \leq 111 \Omega$. The delay time Δt_2 of the reactor should then be $\geq 1 \mu\text{s}$. From Eq. 1 this requires a core area of $A \geq 42.9 \text{ cm}^2$. This area could be realized by stacking 17 cores of 20 cm I.D. and 40 cm O.D. which are wound from 1 in wide 1 mil Deltamax tape.

With a flux path length of about 94 cm and a pulse permeability $\mu_p \approx 3000$, the one-turn reactor has an inductance of

$$L = 1.26 \times 10^{-8} \text{ H cm}^{-1} \frac{429 \text{ cm}^2}{94 \text{ cm} / 3000} = 173 \mu\text{H.}$$

The capacitive discharge current through resistor R_2 is, from Eq. 7

$$i_R = 1081 \text{ A } e^{-3 \times 10^6 t} (\cosh 2.27 \times 10^6 t - 1.32 \sinh 2.27 \times 10^6 t).$$

The current through the reactor and through the NBS is, from Eq. 8,

$$i_L = 306 \text{ A } e^{-3 \times 10^6 t} \sinh 2.27 \times 10^6 t.$$

Its maximum occurs at time $t_p = 0.40 \mu\text{s}$ with a value of 75.5 A . This is well below the limit of 200 A which the NBS can tolerate.

When the NBS sparks, the one-turn windings of the reactor are exposed to a 120 kV step voltage. To keep this potential from the reset bias power supply, an inductor L_B , rated 120 kV , and a low voltage (LV) capacitor C_B are part of the bias circuit.

Combining the STDT and Longitudinal Reactor on One Magnet Core

In the circuits of Figs. 2 and 7, capacitor C_2 was discharged in the shortest time practicable. The discharge of C_1 was delayed until the switch tube had interrupted the fault current fed from the power supply. This time sequence permits an optimal design of the time delay for the reactor L (Δt_2) and for the STDT ($\Delta t_1 > \Delta t_2$).

For applications where the switch tube can interrupt current and block HV in $\leq 1 \mu\text{s}$, it may be economical to combine the functions of the reactor and the STDT on one core. In this case, all windings would have the same number of turns, e.g. one. The switch tube would interrupt in $\leq 1 \mu\text{s}$ the HV power supply current. Approximately $0.5 \mu\text{s}$ later, the spark gaps would discharge capacitors C_1 and C_2 into resistors R_1 and R_2 , respectively. For a total time delay of $2.5 \mu\text{s}$ and one turn windings, the core area for Deltamax cores would be $A \geq 1070 \text{ cm}^2$. Since all windings are coupled, the discharge time constants should be the same $R_1 C_1 = R_2 C_2$.

Fig. 7. Spark Protection of NBS with and STDT, a One-Turn Reactor and Discharge Circuits.

Eliminating Spark Gaps C_1 and C_2

For NBS's operating at relatively lower potentials and with less stored energy to be dissipated, the spark gaps of the circuit of Fig. 7 can be eliminated. This is done by coupling resistors R_1 and R_2 into the discharge path by transformer action as shown in the circuit of Fig. 8. Here the bias winding n_2 of the STDT serves also as a signal winding for the switch tube and crowbar control circuits. A secondary winding n_3 couples discharge resistor R'_1 into the circuit. The STDT serves as a pulse transformer discharging C_1 into R'_1 .

The turns ratio n_1/n_3 permits circuit optimization. In fact, if the STDT is made up of a number of smaller cores, the resistor R'_1 could consist of several lower-voltage resistors, each being coupled into the circuit by one of the smaller cores.

Fig. 8. Spark Protection with and STDT, a Reactor and Transformer Coupled Discharge Resistors.

A one-turn secondary added to the longitudinal reactor of Fig. 7 converts it into a 1:1 pulse transformer with a load resistor R'_2 as shown in Fig. 8. Again, if the core cross section of reactor L is made up of a number of small cores, then discharge resistor R'_2 could be made up of several lower-voltage

resistors, each coupled by transformer action into the discharge circuit by these separate cores.

The reset bias supply of Fig. 7 has been eliminated by connecting the bias winding to the dc filament power supply. Resistor R_B is required to set the correct bias current. Inductor L_B absorbs the 120 kV step voltage when the NBS sparks. This bias circuit must be connected as shown by the solid lines. If it were connected across the terminals of the filament supply, as shown by the dashed line, 120 kV would appear across the LV power-supply terminals.

The transformer-coupled discharge circuit of Fig. 8 eliminates spark gaps and associated trigger circuitry, thereby improving circuit reliability and speed of response. Discharge resistors R'_1 and R'_2 are automatically applied as soon as the NBS sparks. However, in this circuit all the discharge currents of C_1 and C_2 pass through the NBS. This is illustrated by the equivalent circuit of Fig. 4b.

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PLASMA ENGINEERING STUDIES FOR THE TOKAMAK EXPERIMENTAL POWER REACTOR*

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Summary

This paper describes plasma engineering studies made in support of a revised version¹ of a tokamak experimental power reactor (EPR). These include: (1) magnetohydrodynamic (MHD) equilibrium and stability calculations, which establish the ranges of β_c and the corresponding MHD parameters; (2) particle and power balance calculations, which are used for sizing and trade-off studies; and (3) burn cycle dynamics calculations, which determine start-up and driving systems requirements and reactor net power output. The resulting EPR reference design is described.

MHD Considerations

In order to arrive at a feasible reference design for a tokamak experimental power reactor, a number of MHD equilibrium studies have been performed. These MHD studies evaluate two-dimensional MHD equilibria for a variety of pressure and diamagnetic profiles for fixed plasma boundaries of the form

$$R = R_0 + a \cos(\theta + d \sin \theta) \quad (1)$$

$$Z = \kappa a \sin \theta, \quad (2)$$

where $0 \leq \theta \leq 2\pi$, R_0 is the major radius, a is the minor radius, d is the D-shapedness, and κ is the elongation. These calculations are described in detail in Ref. 2.

The power in a beta-limited device, such as a tokamak, is proportional to the square of the toroidal beta, β_c . The MHD equilibrium studies indicate that for a fixed safety factor, $q(a)$, higher elongations (larger κ) and lower aspect ratios (smaller $A = R/a$) give higher obtainable values of β_c . (There is a weaker dependence on profile sharpness and D-shapedness.) On the other hand, the larger κ and smaller A also require higher plasma currents and hence have higher driving system requirements. In particular they need more space in the center of the device.

Two design points were chosen earlier, one with ($R_0 = 5$ m, $A = 3$, $\kappa = 1.3$, $d = 0.25$) and one with ($R_0 = 4.7$ m, $A = 3.5$, $\kappa = 1.65$, $d = 0.50$).¹ These had similar power outputs and driving system requirements and had indications of being able to achieve similar values of β_c . The gain from the higher κ in the second design was approximately offset by the necessity of going to higher A . In attempting to design an equilibrium field (EF) coil system with the constraint that the EF coils be outside the toroidal field (TF) coils, it was found to be impractical to form the higher d of the second design.⁴ Consequently the parameters of the second design were changed to ($R_0 = 4.7$ m, $A = 3.5$, $\kappa = 1.65$, $d = 0.25$).

Reasonable equilibria (i.e., with centrally peaked current profiles, no current reversal, and no excessive distortion of the inner flux surfaces) having

$q(a) = 3$ and $q(0) = 1$ can be found for either of these designs for β_c up to about 10%.

The ideal linear MHD stability of the two design points has been analyzed using the PEST code.⁵ Series of equilibria were constructed with the same plasma shape and having the same pressure and safety factor profiles, but at increasing β_c , to find the critical β_c values for the onset of various unstable modes. In each case the results quoted are derived from convergence studies of the unstable growth rates for a particular toroidal mode number, N , under two sets of boundary conditions: (1) a perfectly conducting wall at the plasma edge (denoted W), and (2) a conducting wall at infinity (denoted F).⁶

Both designs were found to satisfy the criteria for localized ideal and resistive interchange modes,⁷ and to be stable to $N = 1$ W modes to β_c values above the design values. However unstable $N = 1$ F, 3 F and 3 W modes were found in each case with the critical beta values shown in Table I. These modes are both pressure and current driven with the $N = 1$ mode being primarily a kink mode, and the $N = 3$ mode primarily a pressure driven ballooning mode. The safety factor profiles chosen preclude the appearance of internal kinks at the design points as shown by the stability of the $N = 1$ W mode. In practice, the presence of conducting structure near the plasma will cause some degree of passive stabilization of the surface kink $N = 1$ F mode and the current driven contribution to the $N = 3$ F mode. Consequently the stability of the equilibria will probably be determined primarily by ballooning modes such as the $N = 3$ W mode. The predicted critical betas are $\leq 6\%$, but these values require some qualification due to the ideal linear MHD assumptions used. For instance, in practice the modes may saturate as they evolve nonlinearly and resistively, allowing slightly higher β_c values to be reached. This, together with other factors, led to a choice for the reference value of $\beta_c = 7\%$, which seems a reasonable design point for a stable MHD equilibria at this time. Provision has been made, however, for operation at lower (or higher) values of β_c . (See below.)

Table I. Critical Beta Values in Percent

Design	$N = 1$ F	$N = 1$ W	$N = 3$ F	$N = 3$ W
$\kappa = 1.3$, $d = 0.25$	2.85	> 8.00	3.28	5.35
$\kappa = 1.65$, $d = 0.25$	3.73	> 8.00	4.27	5.66

The second design (with $d = 0.25$) was chosen as the reference design. The flux surfaces, current density, required external field, and safety factor are shown in Fig. 1.

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Figure 1. The Flux Surfaces, Current Density, Required External Field, and Safety Factor for the ($B_0 = 4.7$, $A = 3.5$, $\kappa = 1.65$, $d = 0.25$) Reference Design at $\beta_c = 7\%$.

Steady State Performance

The particle and power balance calculations used to determine the reactor performance are in terms of spatial averages over given profiles. The profiles are the same used in the MHD calculations, and the averages are averages over the corresponding flux surfaces. The balances are for D-T ion density, $\bar{n}_{DT}$; alpha particle density, $\bar{n}_\alpha$; ion temperature, $\bar{T}_{DT}$; and electron temperature, T_e . The models are described in Ref. 3.

The density, temperature, and pressure profiles used in both the steady-state and dynamic performance analyses were assumed to be of the form ψ^2 with

$$\hat{\psi} = \frac{\psi - \psi_m}{\psi_2 - \psi_m} = \begin{cases} 0 & \text{plasma edge} \\ 1 & \text{magnetic axis} \end{cases} \quad (3)$$

The quantity ψ is the flux function found from the MHD studies. (The magnetic field lines are tangent to the surfaces $\psi = \text{constant}$.) ψ_2 is the value at the plasma edge (limiter), and ψ_m is the value at the magnetic axis. The reference pressure profile is moderately broad with $\alpha = 1.4$; the density profile is quite broad with $\alpha = 0.3$; and the temperature profile is sharper with $\alpha = 1.1$. The power would be higher if the density and temperature profiles had similar exponents,³ but the broad density profile is thought to be more commensurate with actual reactor conditions.

The reference electron temperature is $\bar{T}_e = 8$ keV, which is near the temperature which gives the optimum power when profile effects are included.³ A reference impurity level of 0.5% background ^{16}O and 5.0% wall sputtered ^9Be is assumed. (The actual time dependence of the ^9Be is calculated in the dynamic analyses.)

Experience has shown that a total, steady-state power of about 600 MWth is necessary to produce a reasonable amount of net power. Table II shows the performance as a function of aspect ratio and β_c for a fixed power output of 600 MWth. It can be seen that for a given β_c , size, required plasma current, required

Table II. Performance of a $\kappa = 1.65$, $d = 0.25$ EPR as a Function of β_c and Aspect Ratio for a Fixed Total, Steady-State Power of 600 MWth. ($B_{TFC}^{\text{max}} = 9$ T.)

β_c	A	R_0 (m)	I_p (MA)	$\Delta B_{OH}^{\text{ind}}$ (T)	α_{TIM}	r_{axis}/a
0.06	3.0	—	—	—	—	—
	3.5	5.0	7.9	8.7	0.35	0.20
	4.0	5.0	6.6	6.3	0.31	0.22
0.08	3.0	4.6	8.8	18.2	0.56	0.22
	3.5	4.5	6.8	12.0	0.47	0.26
	4.0	4.4	5.6	8.6	0.41	0.29
0.10	3.0	4.2	7.9	25.6	0.69	0.27
	3.5	4.1	6.2	16.3	0.58	0.32
	4.0	4.1	5.1	11.5	0.52	0.36

inductive OHC field swing, and confinement all favor high aspect ratio. The problem is that the probability of achieving a particular value of β_c most likely decreases with aspect ratio² as indicated, for example, by the fact that the magnetic axis is pushed further out for the high aspect ratio cases. It can also be seen that the size decreases strongly with β_c as does the required current. The inductive OHC field swing increases, however, because the space on the inside of the device is less.

Since one of the major uncertainties in tokamak reactor design is β_c , the EPR has been designed to operate at higher toroidal fields if β_c is lower than the reference value of 7% (or lower fields if β_c is higher than 7%). Table III shows the performance as a function of β_c and peak field, B_{TFC}^{max} , at the toroidal coils. The reference value is $B_{TFC}^{\text{max}} = 9$ T, but the coils have been designed to produce 10 T. The performance would be degraded somewhat from the reference value if β_c could be made no higher than 5%.

Table III. Performance of the Reference Design as a Function of β_c and Toroidal Field.

β_c	B_{TFC}^{max} (T)	P_T (MW)	P_w (MW/m ²)	$\bar{n}_{DT}$ ($10^{20}/\text{m}^3$)	I_p (MA)	α_{TIM}
0.05	8	194	0.4	0.8	6.4	1.6
	9	311	0.7	1.0	7.2	0.8
	10	474	1.0	1.2	8.0	0.4
0.07	8	378	0.8	1.1	6.5	0.8
	9	606	1.3	1.4	7.3	0.4
	10	923	2.0	1.8	8.1	0.2
0.09	8	619	1.3	1.5	6.7	0.5
	9	992	2.1	1.8	7.5	0.3
	10	1510	3.2	2.3	8.3	0.1

The reactor performance is quite sensitive to the value of $q(a)$. ($q(0)$ is assumed fixed at unity.) In present experiments $q(a)$ is typically 4-5. The energy confinement degrades below $q(a) \approx 3$, and $q(a) \approx 2.5$ is the lower limit for any operation.

(However, it can be noted that for some of the highest β_t ($\approx 1\%$) runs on ORMAK the $q(a)$ values were found to be low (≈ 2.6) with no evidence of degraded energy confinement as found for the lower β_t , less beam heated plasmas.¹⁰) Table IV shows the performance of the reference design as a function of $q(a)$. Operation would favor high $q(a)$ except that the probability of achieving a given value of β_t most likely decreases strongly with $q(a)^2$ as evidenced, for example by the increasing magnetic axis shift. The reference value was taken to be $q(a) = 3$.

Table IV. Performance of the Reference Design as a Function of $q(a)$ at $\beta_t = 7\%$.

q	P_T (MW)	I_P (MA)	nt (10^{20} s/m ³)	α_{TIM}	r_{axis}/a
2.0	589	9.9	3.3	0.39	0.15
2.5	597	8.3	3.2	0.39	0.19
3.0	606	7.3	3.1	0.40	0.23
4.0	620	6.1	3.0	0.45	0.28
5.0	631	5.4	2.9	0.50	0.33

Burn Cycle Dynamics

An analysis of the EPR burn cycle was performed for the reference operating case of $B_{TFC} = 9$ T and $\beta_{max} = 7\%$. A time-dependent version of the particle and power balance model described above was used. In addition, the particle balance for a wall sputtered impurity, $\bar{n}_i$, was included. The MHD equilibria were calculated as a function of the plasma current and β_t at a given time with the pressure profile shape (but not magnitude) held fixed. Also incorporated were various technological models of the reactor pertaining to the driving and heating systems and to the net electrical power production. The overall model is described in Chap. 3 and Appen. C of Ref. 3.

Two of the most critical parameters of the burn cycle are the ohmic heating current reversal time, Δt_{OH} , and the neutral beam turn on time, t_{BO} . These parameters largely set the plasma current ramp time and the behavior of β_t which in turn influence the energy, power, and other requirements of the plasma driving system. (See, for example, Ref. 11.) The sensitivity of the driving system requirements to these parameters was determined for a range of Δt_{OH} and t_{BO} of from 1-6 s. Values of $\Delta t_{OH} = t_{BO} = 2$ s were selected as giving about the best results. Figures 2-4 show the main features of the burn cycle for the reference case. The sequence of events is as follows: The plasma current (not shown) is ramped up to 5.8 MA in 2 s under the combined transformer action of the ohmic heating and equilibrium field coils. The neutral beams are turned on at $t = 2$ s. At about $t = 6$ s the average plasma temperature reaches ≈ 8 keV, and the beam is turned off. This constitutes the startup period.

During the interval $2 \leq t \leq 6$ s the plasma current increases to its final value of 7.3 MA due to the increase in the equilibrium field. The plasma is initially operated with less than the full steady-state DT density in order to get somewhat hotter temperatures during the 2 s ohmic heating period. This reduces the resistive volt second consumption to some extent.

Figure 2. Ion and Electron Temperatures and Toroidal Beta During Reference Case Burn Cycle.

During the time the beam is on, the DT density is brought up to its full value. This is done by adjusting the refueling current, I_{DT} , to supply a value of DT in excess of that fusing and diffusing. (95% recycling at the wall is assumed.) At the end of the startup period the plasma is at ignition. During the burn ^4He and ^9Be accumulate in the plasma, the ^4He from fusion and the ^9Be from wall sputtering. (A background oxygen impurity equal to 0.5% of the DT density is also assumed to be present at all times.) Each ^4He and ^9Be atom contributes 2 and 4 electrons, respectively, to the plasma, and the electron density (not shown) soon exceeds the ion density by a considerable extent ($\approx 60\%$). Because the plasma is beta limited, the accumulation of these extraneous ions and excess electrons forces a reduction in the DT density. This is accomplished, in the simulation, through a control scheme³ that adjusts the DT refueling rate (and hence $\bar{n}_{DT}$) so as to maintain β_t constant. The control scheme is not perfect and there are small oscillations in β_t and $\bar{T}_e$ and $\bar{T}_{DT}$. The reduction in $\bar{n}_{DT}$ appears fairly small in Fig. 3 but is actually quite significant since the fusion power scales as $\bar{n}_{DT}^2$.

As time goes on the plasma cools off, first because the fusion heating diminishes and secondly because of increased radiation losses, due to the ^9Be buildup. At about $t = 50$ s the plasma loses ignition and starts to cool rapidly. This defines the start of the shutdown period, which continues until the end of the cycle, $t = 60$ s. During the first 8 s of the shutdown period, the plasma current and driving system currents are reduced by about 30%. During the final

Figure 3. Plasma Ion Densities and DT Refueling Rate During Reference Case Burn Cycle.

2 s the plasma current is completely ramped down terminating the plasma discharge and the burn cycle. The peak in the radiated power at the end of the shutdown occurs as the plasma cools to the point of maximum line and recombination radiation from the ^9Be and ^{16}O impurities. After the cycle is over, the vacuum chamber is pumped out, and preparations are made for the next burn pulse.

The 60 s burn cycle described above has a natural self quench. Shorter burn cycles can be achieved by injecting a high atomic number material (e.g., ^{18}Ar) to quench the plasma. Alternatively, the burn cycle can be extended somewhat by using supplemental beam heating. In addition different wall coating materials (e.g., BeO and B_4C) and/or different edge temperatures (200 eV has been assumed here) have been shown to give different, usually longer, burn times.¹²

The net electrical power for this reference burn cycle is $P_{\text{net}} = 81 \text{ MWe}$. The power accounts for the conversion of fusion energy to electricity with a 30% efficiency and for the electrical energy required to operate the neutral beam, poloidal coil, cryogenics, vacuum, coolant pumping, and other reactor and plant systems.

Figure 5 shows the variation in the net electrical power for a 60 s burn cycle for different values of the toroidal magnetic field, conversion efficiency, and β_{max} . A value of $\epsilon_{\text{TH}} = 0.39$ is felt to be achievable, and this would result in a substantial increase in net electrical power generation for any fixed value of the other parameters.

Figure 4. Power Terms From Plasma and Beam Power During Reference Case Burn Cycle.

Figure 5. Net Electrical Power from the Argonne EPR as a Function of β_c for Several Values of the Toroidal Field, B_{TFC} at the Coils and the Thermal-to-Electric Conversion Efficiency, ϵ_{TH} .

After consideration of the various tradeoffs presented above, the reference design summarized in Tables IV-V was chosen. This design seems achievable in the context of present knowledge of tokamaks. There is substantial room to maintain a reasonable device if some of the parameters turn out to be optimistic, and there is opportunity to take advantage of constraints which turn out to be less than expected.

Table IV. Reference Plasma Parameters

Major Radius, R_0 (m)	4.7
Aspect Ratio, A	3.5
Elongation, κ	1.65
D-Shapedness, d	0.25
Toroidal Beta, β_t	0.07
Safety Factor (Edge), $q(a)$	3.0
(Magnetic Axis), $q(0)$	1.0
Toroidal Field (Coils), B_{TFC} (T)	9.0
(Plasma), B_{to} (T)	4.3
Plasma Current, I_p (MA)	7.3
Average Ion Density, $\bar{n}_{DT}$ ($10^{20}/m^3$)	1.4
Average Temperature, $\bar{T}_a$ T_{DT} (keV)	8.0
Typical Effective Charge, Z_{eff} (0.5% ^{16}O , 5.0% 9Be)	1.7
Confinement Parameter, $n\tau$ (10^{20} s/ m^3)	3.1
Burn Pulse Length, Δt_{burn} (s)	60
Peak Neutron Wall Loading, P_w (MW/ m^2)	1.3
Peak Fusion Power, P_T (MWth)	606
Fusion Energy per Pulse, U_{th} (GJ)	23.4
Net Electric Power, P_{net} (MWe)	81

Table V. EPR Plasma Driving and Heating System Requirements for the Complete Burn Cycle

Volt-seconds to Plasma (V-s)	57
Magnetic Energy Transferred (GJ)	2.7
Maximum Field in OHC, B_{OH} (T)	8.0
Maximum Field Rise in OHC, $\dot{B}_{OH}$ (T/s)	6.7
OH Current Reversal Time, Δt_{OH} (s)	2.0
Neutral Beam (rf) Power, P_B (MW)	40
Neutral Beam (rf) Heating Time, Δt (s)	4.1

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SUPERCONDUCTING MAGNET SYSTEMS FOR THE ANL EPR DESIGN*

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Summary

The magnet systems for the current Argonne experimental power reactor (EPR) design build on the earlier designs but incorporate a number of improvements. The toroidal field (TF) coil system consists of 16 coils of the constant tension shape, with NbTi, copper, and stainless steel as superconductor, stabilizer, and support material respectively. They are designed for 10 T operation at 3.7 K or 9 T operation at 4.2 K. Two changes from earlier designs permit a saving in material requirements. The coils are wound with the conductor in precompression and the support material in pretension so that when the coils are energized, the stainless steel experiences a stress of 60 000 psi while the copper stress does not exceed 15 000 psi. Both the copper and NbTi are graded, with higher current densities where magnetic and radiation effects are smaller.

The ohmic heating (OH) coil system consists of a central solenoid plus ten other coils, all located outside the TF coils for ease of maintenance. The NbTi-copper coils are cryostable and operate at 4.2 K. The solenoid is segmented, with rings of insulation between segments to transfer the centering force from the TF coils to an insulating cylinder inside the OH solenoid. Locating the OH solenoid inside the support cylinder plus raising the central field to 8 T, enables the OH system to develop more volt-seconds than the earlier designs, even though the plasma major radius is smaller.

The superconducting equilibrium field coils, also outside the TF coils, provide the field pattern required for a D-shaped plasma.

Introduction

The magnet systems for the ANL design of an EPR have been widely reported;¹⁻⁵ this paper focuses on the changes which have been incorporated in the 1977 design and on some other ideas currently under consideration. The overall design appears in Fig. 1.

Toroidal Field Coils

Parameters for the TF coils are given in Table I. These are 16 coils of the pure-tension shape, with an

Figure 1. EPR Magnet Schematic, showing TF, OH, and EF coils.

* Work supported by the U. S. Department of Energy.

NbTi-copper conductor and cooled by pool boiling. The coils are designed to operate at 10 T and 3.7 K or at 9 T and 4.2 K. The TF coils operate in series to eliminate the possibility of carrying unequal currents, which would produce large bending moments and out-of-plane forces on the coils. The reduction in size of the reactor from previous years reduces the energy stored in the TF coil system from 30 GJ to 14 GJ, and also reduces the amount of copper stabilizer and stainless steel support.

Table 1. Toroidal Field Coil Parameters

Number of coils	16
Coil shape	Pure-tension D
Superconductor	NbTi
Stabilizer	Copper
Support material	Stainless Steel
Support cylinder	Fiberglass-reinforced plastic
Coil bore, vertical x horizontal	8.73 m x 5.64 m
Operating temperature	3.7 K (4.2 K)
Peak field	10 T (9 T)
Field at plasma	4.81 T (4.33 T)
Total amp-turns	115 MA-turns (104 MA-turns)
Operating current	60 000 A (54 000 A)
Turns per coil	60 x 2
Average current density	1540 A/cm ² (1390 A/cm ²)
Inductance	7.9 H
Stored energy	14.3 GJ (11.6 GJ)

Figure 2. Cross Section of TF Coil

The magnet model⁶ developed for systems studies has proved useful in designing the TF coils. When the plasma and blanket/shield space requirements and a few other parameters are specified, the model calculates the size and shape of the coils, and their composition.

Conductor and Coil Structure

The coil is trapezoidal in cross section, with a central rib for support. Each half coil is wound of a 60 kA sheet conductor. The coil cross section and conductor are shown in Figs. 2 and 3. The conductor consists of a sheet of copper stabilizer with many copper and NbTi-copper composite wires stranded around it and soldered to it. Stainless steel reinforcement is wound with the sheet conductor to reduce the hoop stress in the conductor.

The allowable stress in the conductor is set at $1.94 \times 10^8 \text{ N/m}^2$ (15 000 psi) to limit the strain-induced resistivity of the copper and prevent it from yielding. If the stainless steel were wound with the copper, at the same strain level, the stress in the stainless steel would be only $1.8 \times 10^8 \text{ N/m}^2$ (26 000 psi). If, however, the conductor is wound in precompression and the stainless steel in pretension, then when energized the stainless steel will experience a stress of $3.5 \times 10^8 \text{ N/m}^2$ (50 000 psi); and only half as much is required.

Because the field decreases with position going out across the thickness of the coil, the amounts of

HIGH FIELD (10 TESLA) CONDUCTOR

MEDIUM FIELD (5 TESLA) CONDUCTOR

LOW FIELD (2 TESLA) CONDUCTOR

Figure 3. Three Grades of 60 kA Conductor for TF Coil

Figure 4. EPR TF Coil System Showing Access Between Coils. (A) 16 Coils, (B) 12 Coils with Same Total Current

superconductor, copper, and stainless steel required per turn of conductor also decrease. Figure 3 shows three representative grades of conductor. The number of grades used would be determined by balancing the material and space savings of going to more grades against the cost of making more grade to grade joints.

The conductor was designed to be cryostable at 60 kA, 4.2 K, the rated field, and an effective heat transfer to the helium of 3500 W/m^2 . A dynamic stability computation predicts that at 4.2 K, the rated field, and a perturbing heat pulse of 1.0 J/cm^2 of cross section, the recovery currents of the three conductors in Fig. 3, would lie between 62 and 66 kA.

Figure 5. Variation of Field Ripple with Radial Position with and without Iron, for 16 and 12 Coils

The conductor described here is inherently a dc conductor. Its use requires that the TF coils be shielded from the pulsed field from the PF coils. Thus the design includes a high-purity aluminum shield, as described elsewhere.^{2,7} Otherwise, a different kind of conductor would be needed.

Other Considerations

Two additional ideas which have not been incorporated into the 1977 design, but which are being studied, are an aluminum-stabilized conductor with an aluminum-alloy support material and the use of ferromagnetic steel to lower the field ripple. Calculation with the magnet model⁶ showed that the coils using aluminum would be 40% lighter and have material costs 17% less, but the coil thickness increased from 0.63 m to 1.19 m. The ohmic heating solenoid and support cylinder would no longer fit radially inward from the TF coils; consequently, the whole reactor would have to grow. In other words, the economics of using aluminum and aluminum alloys in the TF coils cannot be approached from the magnet system alone, but require a study of the entire reactor.

Using ferromagnetic segments of the shield to reduce field ripple is also being studied. Figure 4 shows the improved access which could be achieved if the sixteen TF coils were replaced by twelve with the same total ampere-turns. However, the field ripple in the outer part of the plasma (out to 6.0 m) is much worse with twelve coils than with sixteen. If segments of the blanket/shield directly inward from the outer leg of each TF coil were ferromagnetic, the ripple would be reduced.

The effect of adding the iron, both for a 16-coil system and a 12-coil system, was computed at the

Rutherford Laboratory, using the magnet design program GFUN3D.^{8,9} The results appear in Fig. 5. For the 16-coil system, the iron was assumed to be in the form of a block 0.5 m x 0.5 m x 5.0 m high inside the outer leg of each TF coil; with 12 coils each block was increased by 16/12 in cross sectional area. Fig. 5 shows that at 6.0 m, for example, the iron reduces the ripple from 1.3% down to 0.2% for sixteen coils, but only from 4.6% down to 2.9% for twelve coils and the specified amount of iron. Twice the iron (not shown in Fig. 5) reduces the ripple with 12 coils to 0.5% at 6.0 m. In an actual reactor, the ferromagnetic material could be incorporated as part of the blanket and shield system.

Ohmic Heating Coils

The OH and equilibrium-field (EF) coils are located external to the TF coils, to facilitate installation and maintenance and to allow them to be superconducting. The location of these coils is shown in Fig. 1, and the principal parameters are given in Table 2.

The OH coils consist of a central solenoid and ten additional coaxial coils. The central solenoid is between the TF coils and the central support cylinder. This configuration maximizes the central flux core area for a solenoidal OH coil to provide the necessary volt-seconds. The OH coils are energized to give a negative flux before the plasma is formed, then the current in the OH coils is reversed, inducing a current in the plasma.

Table 2. Poloidal Coil Parameters

	OH Coils	EF Coils
Number of coils	Solenoid + 10	12
Location	Outside TF coils	Outside TF coils
Superconductor/stabilizer	NbTi/Cu	NbTi/Cu
Operating temperature	4.2 K	4.2 K
Current density	1730 A/cm ²	1730 A/cm ²
Operating current	70 kA	70 kA
Total amp-turns	82.3 MA-turns	45.4 MA-turns
Maximum volt-seconds	90 V-s	37 V-s
Central field	8 T	0.3 T
Field at plasma	< 10 ⁻³ T	0.56 T
Inductance	0.73 H	0.94 H
Coupling coefficient to plasma	-0.25	-0.175
Energy transferred	1.75 GJ	2.30 GJ
Inductive energy: OH, EF, and Plasma	3.75 GJ	

Conductor

A 70 kA cable conductor suitable for the OH coils appears in Fig. 6; the design is a compromise between the conflicting requirements of low ac losses and full stabilization. The 70 kA cable consists of 70 basic cables wound around a backbone strip about 1 mm thick. The strip, either solid G-10 or a stainless-steel braid, provides support for the cable during cabling and winding, and also withstands the hoop force in the coil.

1000A BASIC CABLE

70 kA CABLE CONDUCTOR

Figure 6. 70 kA Conductor for Pulsed OH and EF Coils

Figure 7. Segmented OH Solenoid and Support

Each 1000 A basic cable consists of six basic strands wound around a stainless steel cable. See Fig. 6. A basic strand consists of a superconducting composite with six copper wires stranded around it and soft-soldered to it. The basic strand is cryostable.

Figure 8. One Ring of Support Cylinder and OH Solenoid Bobbin

Coil Structure and Support Cylinder

The proposed TF coil support cylinder is made of a fiberglass-reinforced plastic such as G-10 or G-11. The cylinder is positioned inside the OH coil with axial support rings transmitting the load from the TF coils to the support cylinder as shown in Fig. 7. Each coil is 0.20 m thick, with 0.05 m between coils. Liquid helium is introduced to the bottom of the coils, and the gas is vented at the top of the coil. Both the liquid channels and the gas-venting channels are provided by micarta or G-10 strips, about 0.6 mm thick running radially outward, as shown in Fig. 8.

Liquid and gaseous helium flow as shown by the arrows in Fig. 7. The slots shown in Fig. 8 disperse the gas. The rings will also serve as coil-winding bobbins.

Equilibrium Field Coils

The EF coils are decoupled magnetically from the OH coils by locating them all on the same flux line of the OH coils, operating them in series, and requiring that the numbers of positive and negative amp-turns in the EF coil system be equal. Selection of the flux line and of the coil positions along the flux line is governed by engineering constraints. The currents are chosen to produce the required equilibrium field configuration in the plasma, using a free-boundary MHD equilibrium code, but subject to the constraint of minimizing energy in the EF coil system.

Twelve coils comprise of the EF coil system. An elongated, D-shaped plasma requires more current and energy in the EF coil than a circular plasma would, but the required field pattern can be achieved by EF coils outside the TF coils. EF coil parameters also appear in Table 2. The EF coil system is described in more detail in reference 10. The EF coils use the same 70 kA conductor as the OH coils.

A system of water-cooled copper coils is located within the blanket and shield. This coil system provides the 500 V, 5 ms voltage pulse necessary to

break down the plasma, and also provides the trimming fields necessary to control the plasma position.

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COMPUTATIONAL MODEL FOR SUPERCONDUCTING TOROIDAL-FIELD MAGNETS FOR A TOKAMAK REACTOR*

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Summary

A computational model for predicting the performance characteristics and cost of superconducting toroidal-field (TF) magnets in tokamak reactors is presented. The model can be used to compare the technical and economic merits of different approaches to the design of TF magnets for a reactor system. The model has been integrated into the ANL Systems Analysis Program. Samples of results obtainable with the model are presented.

Introduction

An integrated systems computer program for simulation of tokamak power plants has been developed at ANL.¹ The program consists of modular units with each unit corresponding to a reactor subsystem. This report describes a model that has been developed for the toroidal-field (TF) coils. The model is described in more detail in reference 2.

The Model in Outline

The TF coils are taken to be of the constant tension shape;³ the shape is calculated by the approximate method of Moses and Young.⁴

Several material choices are permitted in specifying the magnet. The magnet may be superconducting or conventional; if it is superconducting, the superconductor may be NbTi or a hybrid with NbTi in the low-field turns and Nb₃Sn in the high-field turns. The superconductor may be graded or ungraded. The magnet may be cryostable or intrinsically stable; the stabilizer may be graded or ungraded.

The simplified flow diagram in Figure 1 shows how the model operates. The maximum magnetic field and the space requirements inside the TF coil are first specified; from these the coil thickness is estimated and the inner and outer mid-radii R_i and R_o of the coil calculated. At this point the choice between cryostable or intrinsically stable coils occurs. In either case, the choice between NbTi or hybrid coils is made at this point on the basis of the maximum field. The critical current density, J_c , and the cross-sectional area, A_{sc} , complete the specification of the superconductor.

For an intrinsically stable conductor, the ratio of copper to superconductor is specified; for a cryostable conductor the amount of copper is determined from cryostability considerations involving heat transfer to the helium and electrical resistivity. Since the magnetoresistivity and radiation-induced resistivity vary across the thickness of a coil, the copper can be graded. The amount of stainless steel support material is determined by the specified allowable stress or strain and the tension in the coil.

From the total cross section the corresponding thickness is calculated. If it differs too much from the previous estimate of thickness, the calculation must be iterated until it converges. After it does, the circumference is calculated; and from it the total amount of superconductor, copper and stainless steel in the system. Finally, stored energy and costs are calculated.

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The Model in Detail

Parameters Specified

Certain parameters must be specified initially: the peak magnetic field, B_{max} , the design temperature, T , the outer radius of the inner TF coil leg and the inner radius of the outer TF coil leg. These latter two are determined by the space needs for the blanket and shield. The design temperature T is the sum of the operating temperature, generally 4.2 K, and the temperature safety margin, generally 0.5 K.

Another parameter to be specified is the permissible stress or strain in the conductor. For a Nb₃Sn-Cu composite conductor, the strain is limited by the brittleness of the Nb₃Sn; but for a NbTi-Cu composite conductor, it is limited only by the strain-induced resistivity of the copper.

The Superconductor

Two kinds of coils are envisioned; for fields B_{max} up to 8 or 10 T, NbTi is used. For higher values of B_{max} , hybrid coils are considered, with NbTi in the outer turns and Nb₃Sn in the inner turns where the field is higher. The choice is an input option. In either case, the total current NnI is given by Ampere's Law.

Although superconducting TF coils for a tokamak reactor would almost certainly be cryostable, it may prove interesting to compare cryostable and intrinsically stable coils; so provision is made in the magnet model for intrinsically stable coils as well.

Critical Current Density. For NbTi, the critical current density as a function of temperature T in Kelvin and field B in Tesla is shown in Figure 2. The data in Figure 2 are fit by the expression

$$J_c = (T_c - T) (3.352 - 0.3607 B + 11.929/T) \times 10^4 \text{ A/cm}^2 \quad (1)$$

where the critical temperature T_c can be found by

$$T_c = 8.56 (1 - B/14.7)^{1/2} \quad (2)$$

Over the region $1 \text{ T} \leq B \leq 11 \text{ T}$, $3 \text{ K} \leq T \leq 6 \text{ K}$, $T_c - T \geq 1 \text{ K}$, Eq. (1) fits the data to 10% or better.

There is considerable uncertainty as to what values of J_c can be obtained in practical Nb₃Sn conductors. We use the expression

$$J_c = (35.55 - 4.25 B + 0.1375 B^2) (0.739 + 0.144 T - 0.0196 T^2) \times 4.74 \times 10^4 \text{ A/rm}^2 \quad (3)$$

Cross-Sectional Area. For a cryostable NbTi magnet, the cross-sectional area of superconductor is given by

$$A_{NbTi} = NnI/J_c \quad (4)$$

where J_c is found from Eq. (1) with $B = B_{max}$.

Few intrinsically stable magnets operate at the short-sample value of J_c . Consequently, the area found

In Eq. (5), q_h includes the effect of heat transfer along the conductor or between turns as well as into the helium. So interpreted q_h may be less than 0.06 W/cm^2 or greater than 2.2 W/cm^2 , but it is customary to take $q_h = 0.35 \text{ W/cm}^2$.

With these factors specified, J can be found from Eq. (5) and A_{Cu} from Eq. (6).

Grading the Copper. The resistivity of the copper is dominated by the magnetoresistivity and the radiation-induced resistivity, both of which are smaller for the outer turns than for the inner ones. Thus, there can be savings in grading the copper as there are in grading the superconductor.

Stainless Steel Reinforcement for Coils

Enough stainless steel reinforcement must be included with the coils to withstand the tension, τ , in the coils and limit the stress or strain in the coils to the allowable value.

The tension is related to the stresses through the relation

$$\tau = A_{Cu} \sigma_{Cu} + A_{SS} \sigma_{SS} \quad (9)$$

where σ_{Cu} and σ_{SS} are the allowable stresses in the copper and stainless steel respectively. In general, a specified σ_{Cu} and σ_{SS} occur simultaneously only if the coil is wound with the copper in precompression and the stainless steel in pretension. The model calculates the required prestresses before and after cooldown and confirms that they do not exceed σ_{Cu} or σ_{SS} .

Alternatively, the allowable strain, ϵ , can be specified rather than the stresses σ_{Cu} and σ_{SS} ; in this case no pretension is assumed.

Geometry of Coils

Moses and Young⁴ have shown how the geometrical parameters of a constant-tension coil shown in Figure 3 can be generated numerically. We have integrated their expression for a family of parametric values, and fit the results as functions of R_1 and R_2 .

Iteration for Thickness, t

The total area of the coils exceeds that occupied by the superconductor, copper and stainless steel by a factor of $1 + \gamma$, corresponding to helium space, cryostat

Figure 3. Geometric Parameters for a Constant-Tension Coil

walls, insulation, etc. Assuming no gaps between coils in the inner leg, we find the thickness, t , by

$$t = (1 + \gamma) (A_{SC} + A_{Cu} + A_{SS}) / 2\pi R_1 \quad (10)$$

The model requires iteration over the coil thickness t . It is necessary to take some initial value for t and iterate through the calculation until the value for t converges. Then R_1 and R_2 can be found, and from them the other shape parameters of Figure 3.

Magnet Anneal

Most of the radiation damage in the superconductor and normal metal can be recovered by warming up the magnet to near room temperature. There is a strong incentive to reduce the shield thickness, permit a higher radiation level at the magnet, and anneal the magnet periodically. However, magnet warmup and cooldown require two to three months of plant downtime. Thus, there is an optimum time span between magnet anneals. At present the program permits specifying t_a in one of two modes: either t_a is an input variable, or t_a is defined to be equal to the lifetime of the first wall.

The Support Cylinder

There is a compressive force pushing all TF coils toward the axis of the tokamak; this force must be taken up by the support cylinder, a hollow cylinder of stainless steel. The cylinder must be thick enough that the stresses resulting from that force do not exceed the peak acceptable circumferential stress σ_{max} , (typically $3.45 \times 10^{11} \text{ Nt/m}^2$ or 50,000 psi). The support cylinder must also be thick enough to be stable against buckling.

Stored Energy

The stored energy of the TF coil system is an important parameter in its own right; in addition, attempts have sometimes been made to scale the costs of magnet systems according to stored energy.

The energy is found from the volume integral

$$E = \frac{1}{2\mu_0} \int B^2 dV.$$

We assume that B is zero outside the coils and has a $1/r$ dependence inside. We approximate $h(r)$, the half height as a function of radius, by a parabola passing through the points (R_1, H_{R1}) and $(R_2, 0)$ and of height H_{max} .

$$E = \frac{2 B_{max}^2 (R_1 + t/2)^2}{\mu_0 (R_2 - R_1)^2} [R_2 (H_{R1} R_2 - 2 R_1 \Omega)] \quad (11)$$

$$\ln (R_2/R_1) + (R_2^2 - R_1^2) \Omega - (R_2 - R_1) H_{R1}/2]$$

$$\text{with } \Omega = H_{max} (1 + \sqrt{1 - H_{R1}/H_{max}}).$$

TF Magnet Costs

The volume of superconductor, copper stabilizer or stainless steel reinforcement is found by multiplying the circumference by the cross-sectional area, incorporating grading if appropriate. Multiplication by the density then yields the mass. The cost of materials is calculated assuming unit material costs. The electrical insulation, superinsulation and dewar are costed on the basis of volume or area. The magnet cost is

equal to the material plus fabrication and winding cost, and at present, is taken as four times the material cost.

The refrigeration load is calculated as the sum of the nuclear energy deposition and thermal leakage. The refrigeration load is converted to electric power requirements using a multiplicative factor of 300 for 4.2 K cooling and 500 for 3 K cooling. The electric power requirements are passed into another part of the system code that calculates the plant net electric power output.

Results

The model was used to study how magnet parameters vary with major radius, peak magnetic field and material

Figure 4. Magnet Thickness as a Function of Major Radius at Several Values for the Maximum Magnetic Field.

Figure 5. Sensitivity of the Magnet Thickness to the Permissible Strain. Results are shown for a Maximum Magnetic Field of 8 T and Several Values for the Major Radius.

properties. One or two parameters were changed for each study. During variation with major radius, the aspect ratio R/a was held at a constant value of three.

Coil Thickness

The coil thickness is shown in Figure 4 as a function of major radius for several peak fields. The tension increases with both radius and field, and consequently the coils must be thicker to withstand the tension. Moreover, the lower permissible strain with hybrid coils accentuates the increase in thickness with field.

Variation of coil thickness with allowable strain is shown in Figure 5 for an 8 T field and in Figure 6 for a 12 T field. In each case, major radii of 5 m to 13 m are treated. The rapid increase in thickness as the strain is decreased below 0.1% is striking. For a 12 T field, 0.05% strain, and an aspect ratio of 3, there is not enough room for TF coils with major radius of 5 m or less. Under the same conditions there is not enough room for the support cylinder in reactors with 7 m or less major radius.

Stored Energy and Cost

The energy stored in the TF coil system is shown in Figure 7 (and the cost in Figure 8) as functions of major radius for fields between 8 T and 14 T. Here too the rapid increase with both field and major radius is evident.

The cost as a function of allowable strain for major radii between 5 m and 13 m is shown in Figure 9 for a field of 8 T and in Figure 10 for a field of 12 T. The increased thickness required for low strain, shown in Figure 5 and Figure 6, represent increased material requirements and thus increased cost.

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Figure 6. Sensitivity of the Magnet Thickness to the Permissible Strain. Results are shown for a Maximum Magnetic Field of 12 T and Several Values for the Major Radius.

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Figure 7. Stored Energy as a Function of the Major Radius at Several Values of the Maximum Magnetic Field.

Figure 9. Sensitivity of the Magnet Cost to the Permissible Strain for a Maximum TF Field of 8 T.

Figure 8. Cost of the TF Magnet as a Function of Reactor Size and Maximum Field.

Figure 10. Sensitivity of the Magnet Cost to the Permissible Strain for a Maximum TF Field of 12 T.

THE EQUILIBRIUM FIELD COIL SYSTEM FOR THE ARGONNE EPR DESIGN

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Summary

The equilibrium field (EF) coil system for the Argonne Experimental Power Reactor (EPR) and the methods by which it has been designed are described. The number of coils, their placement, and the currents in them are fixed by considerations of the trade off between the stored energy in the coils and the closeness with which the required magnetohydrodynamic (MHD) equilibrium can be matched. The bulk of the equilibrium field is produced by superconducting coils outside the toroidal field (TF) coils. These coils are decoupled from the ohmic heating (OH) system. Normal conducting coils just outside the vacuum chamber are also provided for fine control. The amount of D-shapedness of the plasma cross section is found to be limited. The reference design EF coil system configuration is described, and the internal configuration of the conductor and implications of the EF coil system on the reactor burn cycle and on the driving system costs are discussed.

General Procedure

Reference Equilibrium

In order to determine an EF coil system, a reference MHD equilibrium is first chosen. The equilibria used in these studies are determined by fixed-boundary MHD calculations as described in Refs. 1-2. These calculations depend on the choice of plasma parameters. One of the more important of these parameters is β_t , the ratio of the plasma pressure to the toroidal magnetic field pressure. The reactor power increases with β_t , but so do the requirements on the EF system. The achievable values of β_t for a reactor are not yet known but are expected to be roughly 7% for the Argonne EPR.² To be conservative the coils are designed for $\beta_t = 8\%$.

Among the outputs from the MHD calculations is the flux function, $\psi_{\text{ext}}(R, Z) = 2\pi R A_\phi(R, Z)$, for the required EF field. (A_ϕ is the azimuthal component of the external field vector potential.) The flux function for the fixed boundary calculations is only known inside the plasma, and it is the purpose of the EF coil system to match this flux function as accurately as necessary to reproduce the desired plasma shape and other plasma parameters.

The locations of the EF coils are subject to a number of constraints: It has been the Argonne philosophy to place the EF coils outside the TF coils. The principal reason is that maintenance of these coils after the reactor becomes hot would be extremely difficult if they were inside. Placed outside, they are easily raised or dropped if it is necessary to work on them. Use of superconducting EF coils is also facilitated by a position outside of the TF coils. Additional difficulties are that the space inside the TF coils is crowded, access to other components is impaired, and initial fabrication is much more difficult.

A second constraint is that the EF system should be decoupled from the OH system. (Since the two must be operated with different time variations, large voltages would be induced in each other if they were coupled.) Decoupling is done by placing the EF coils on an OH flux line, connecting them in series, and requiring that the total current in them add to zero.

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A third constraint is that the EF coils cannot be placed too near the TF coils since they produce an undesirably high field on the TF coils. In the present design the centerlines of the EF coils were kept approximately 1 m away from the TF coils.

There are other positions the EF coils cannot occupy as well. The neutral beam ducts prevent coils from being placed too near the midplane on the outside, and the OH solenoid prevents coils from being placed too near the major axis. Space must be left between two coils near the top (and bottom) outside to allow the vacuum ducts to penetrate.

Trimming Coils

The EF coils are expected to provide the bulk of the required field. It is felt, however, that these coils may be too far from the plasma and may be too restricted in their location to provide adequate fine control for a probably sensitive high β_t plasma or to prevent tendencies of the plasma to move. For this fine control the initiation-trimming (IT) coils are used. These coils, which are also necessary for the early plasma startup, are segmented, normal copper conductors located just outside the vacuum vessel. The necessary currents (≤ 0.5 MA) in these coils are sufficiently small that they do not affect the reactor power balance. They are sufficiently shielded to prevent undue radiation enhanced resistivity, and they can be annealed. They would be feed-back controlled to maintain the plasma position and to provide any fine tuning not provided by the EF coils. The EF coils are designed, however, as if the IT coils did not provide any of the required equilibrium field.

Mathematical Method

In order to find the required currents for fixed positions of the EF coils, the following quantity is minimized as suggested in Ref. 3:

$$c = (1-r) \sum_{n=1}^N (\psi_n - \psi_{\text{ext},n})^2 + r \sum_{i,j=1}^K L_{ij} I_i I_j + \lambda \sum_{i=1}^K I_i \quad (1)$$

where $\psi_{\text{ext},n}$ is the desired flux function given at N points, $n=1, \dots, N$; ψ_n is the actual field due to K EF coil pairs at $(R_k, \pm Z_k)$, $k=1, \dots, K$. (An expression for ψ_n is given in Ref. 1.); r is a parameter which determines the relative importance of the first (field error) term and the second (stored energy) term; and λ is a Lagrange multiplier associated with the constraint that the currents add to zero.

The inductance matrix, L_{ij} , is calculated as follows:

$$L_{ii} = \mu_0 R_i \left[\log \frac{8R_i}{a_i} - \frac{7}{4} \right] \quad (2)$$

$$L_{ij} = \mu_0 \sqrt{R_i R_j} \left[\left(\frac{2}{k} - k \right) K(k) - \frac{2}{k} E(k) \right] \text{ if } i \neq j \quad (3)$$

where

$$k^2 = \frac{4R_i R_j}{(R_i + R_j)^2 + (z_i - z_j)^2}$$

and $K(k)$ and $E(k)$ are the complete elliptic integrals of the first and second kinds, respectively. The self-inductance terms depend on the coil size. (a_i is the minor radius of coil i .) Since the coil size depends on the currents in the coils (The current density is assumed fixed.), it is necessary to iterate the procedure until consistent sizes and currents are obtained.

By varying the quantity, r , a better (or worse) match to the desired field can be obtained at the expense of increased (or decreased) stored energy. Experience has indicated an average relative error of $\pm 0.4\%$ in the flux function is necessary to provide an adequate match to the plasma boundary and other parameters.

The optimum EF system is found by changing the coil locations and number of coils until the lowest stored energy is obtained for a given error (near 0.4%).

Argonne EPR Reference Design

EF System Configuration

It is generally felt that elongated plasma cross sections with outwardly pointing D-shapes are beneficial to the achievement of high β_t . These configurations tend to have more highly shaped required external fields than the circular cross section cases.¹ An example is shown in Fig. 1. It is especially hard to produce the field near the upper, inside part of the cross section with coils far from the plasma and with no coils to the left (in Fig. 1) of the plasma (because of the TF coils and OH solenoid). (It can also be noted that these fields should also be unstable to vertical excursions since the $\vec{J} \times \vec{B}$ force has an upward component. The vertical positioning would have to be feed-back controlled via the IT coils.)

Fig. 1. A typical required external field for an elongated D-shaped plasma. The major axis is on the left. This cross section has $\kappa = 1.65$ and $d = 0.50$.

Figure 2 shows the stored energy versus field error curves for three elongations (κ = height/width = 1.00, 1.30, and 1.65) and two D-shapes. ($d = 0$ is elliptical, and the plasma becomes more D-shaped as d increases. The actual relations are given in Ref. 2.) The curves for $d = 0.50$ (the value for the cross section in Fig. 1) are off the graph to the top for all three elongations. It can be seen that the higher elongations require more stored energy and that there is a limit to the sharpness of the D-shape that can be formed with a reasonable EF system located outside the TF coils.

Fig. 2. Stored energy versus field error, illustrating the relative difficulty of obtaining elongated, D-shaped equilibria. The $d = 0.50$ curves are off the graph. All cases have an aspect ratio of 3.5. Typically, an error of $\approx 0.4\%$ is sufficient to match the plasma parameters.

The Argonne EPR Reference Design² has $\kappa = 1.65$ and $d = 0.25$. The D-shapeness, d , was limited by the above considerations. The EF system parameters for $\beta_t = 8\%$ are summarized in Table 1. Figure 3 shows how closely this configuration matches the $\beta_t = 7\%$ reference value if the currents are all reduced by 0.95. The "actual" boundary was calculated using a slightly modified version of the Princeton free-boundary equilibrium code. The inductance and decoupling of the EF and OH system have been verified independently by more accurate field calculations using the distributed current in the actual coil configurations as shown in Fig. 4.

Fig. 3. The plasma boundary due to the actual EF coil system compared to the desired boundary for $B_z = 7\%$. The EF current is 95% of the design value.

TABLE 1. Coil Configurations
(The numbers given are for coil pairs, one above and one below the midplane.)

Coil	R (m)	z (m)	ΔR (m)	Δz (m)	NI (MA-turns)	Conductor Length (MA-m)
1	2.04	5.38	0.48	0.25	-4.24	54
2	2.38	5.62	0.48	0.25	-4.24	63
3	3.00	6.00	0.40	0.50	7.14	135
4	4.80	6.05	0.88	0.50	15.56	469
5	8.05	4.27	0.43	0.50	-7.61	385
6	9.59	2.36	0.38	0.50	-6.61	398
Total:					0	1504

Fig. 4. A cross section of the Argonne EPR showing the EF coils (crosshatched), the IT coils (black), and the OH coils (X).

Superconducting Coils

The EF coils are cryostable with NbTi superconductor and copper stabilizer. They operate at 4.2 K and are cooled by pool-boiling helium. The operating current is 70 kA, and the current density is 1730 A/cm^2 .

Fig. 5. 70-kA cryostable conductor for the EF coils.

The 70-kA conductor, shown in Fig. 5, is designed to be cryostable and still have low ac losses. It consists of 70 basic cables wound around a backbone strip of solid G-10 or stainless steel braid. Each basic cable, in turn, consists of six basic strands of copper and NbTi-copper composite wound around a stainless steel cable. The basic strands are individually cryostable. The coils and conductors are described further in Ref. 4.

Reactor Implications

The coil design described above is based on the external field distribution needed by the plasma at peak power. During the startup, burn, and shutdown the field must be varied continuously to keep the plasma in MHD equilibrium. At any given time in the burn cycle the required field is a function of the plasma pressure and current. Burn cycle simulations (of the type described in Refs. 2 and 5) determine the currents and voltages in the EF coils as a function of time. For the EPR with the EF coil design described above and using a typical startup scenario, it takes a maximum power, P_{EF}^{max} , of about 1.6 GVA to run the EF system.

The EF coils are driven by a thyristor-type rectifier-inverter power supply operating out of a superconducting energy storage coil. The cost of this supply (≈ 40 M\$ for the above value of P_{EF}^{max}) is one of the major costs of the driving system, and minimizing the EF power supply cost generally dictates the choice of startup time and other parameters of the burn cycle. To first order the value of P_{EF}^{max} scales linearly with the maximum stored energy in the EF system. Thus, the optimization design process described above, which minimizes the stored energy, is critical in reducing the overall reactor costs.

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TNS SUPERCONDUCTING OHMIC-HEATING SYSTEM*

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Summary

The superconducting ohmic-heating (OH) system is the selected design for the General Atomics Co./Argonne National Laboratory TNS tokamak design studies. The key features of the OH system design are: (1) parallel coil connection, (2) better utilization of flux core by embedding support cylinder of the toroidal-field coil within the OH inner radius, (3) independent trim coils for correcting the stray fields, (4) low-loss high-current cryostable cable design and (5) OH coil cycling circuit using a reversing bridge. Detailed designs are presented.

Introduction

A conceptual design on the superconducting ohmic-heating coil has been made for the GA/ANL TNS Tokamak Reactor Studies. Aside from the economical considerations, there are several difficult tasks encountered in designing the superconducting OH coil: (1) the OH coil system must be magnetically decoupled from the equilibrium field (EF) coil system; (2) the stored energy of the OH coil is very large and the necessity for rapid charging and discharging imposes severe requirements of extreme high current cable at exceedingly high voltage. To reduce the overall TNS Tokamak cost, the flux core of OH coils should be small. Superconducting OH coils allow a much better utilization of the flux core by placing the support cylinder of superconducting toroidal field (TF) coils within the inner diameter of the superconducting OH coils. To decouple the OH coils from EF coils, the OH coil system is required to be connected in parallel with the EF coil. By dividing the OH coil into many parallel paths, both the EF coil and OH coil will require lower voltage and reasonable currents in the coil.

The choice of about 8T central field, 8T/s for the rate of change of flux and about 2 m for the OH solenoid diameter is based on a study for the rationale limits on the central field and the dB/dt.

Coil Configurations

The OH coil geometry is related to several basic requirements: (1) it must produce a total flux change of 37 V-s; (2) stray field in the plasma region must be less than 10 G and stray field on the TF coils must be less than 500 G; (3) the average current density² in the coil cross section be kept less than 3000 A/cm²; and (4) it is desirable to divide the long solenoid into several sectional coils with effective number of turns per sectional coil equal to 20. These sectional coils are connected electrically in parallel.

The OH coil resulting from the above requirements is shown in Fig. 1, and Table 1 shows some of

* Work supported by the U. S. Department of Energy.

the numerical parameters for this coil. The coil shown consists of a central solenoid, sections 9 through 30, field profile coils, sections 3 through 8, and two correction coils, coils 1 and 2. Each coil section 3 through 30 contains 20 turns.

The central solenoid produces the coil flux. The profile coils, positioned approximately on a flux line and so spaced as to not interfere with reactor penetrations, are used primarily to direct the core flux in the return path away from the plasma and TF coil regions. The coil sections 3 through 30 are all connected in parallel and form the OH coil which is to be connected in parallel with the EF coil.

The exact current flowing in each of the 56 parallel connected coil sections in the OH coil is determined by a computer code called AIRCOIL.

To reduce the field in the plasma area to 10 G, it was necessary to add the two correction coils, sections 1 and 2, in Fig. 1. The best solution resulted for the correction coil located as shown in Fig. 1. The area A outlined in Fig. 1 contains stray fields of less than 10 G. The correction coil number 2 is placed inside the TF coils for this solution. Cases were run for this coil located outside the TF coils, but the best that could be obtained was an area with less than 10 G shown in Fig. 1 as area B.

The field at the TF coil surface next to the central solenoid ran from about 145 G to 200 G. The maximum values were adjacent to the 5 cm vertical gaps between the solenoid coil sections. The field on the TF coil center line exceeds 500 G in an area extending from about the top of the TF coils towards the central solenoid for a length of about 3 m.

Table 1. Coil Parameters for the TNS
Superconducting OH Coil

Central Field	8 T
Maximum dB/dt	8 T/s
Total Ampere-Turns	82×10^6 A-T
Total Ampere-Meters	490×10^6 A-M
Source Current	4.1×10^6 A
Total Inductance	85.1 μ H
Stored Energy	715 MJ

Parallel Operation

A question about this parallel connected coil is concerned with the stability of the currents flowing in each coil and the effect on the plasma field due to errors in coils positioning during assembly and due to a coil going normal. These effects were calculated and the results are summarized in Table 2.

The coil section studied was no. 20 which is in the central solenoid area. This coil was used because it is far enough from section no. 30 so that it

Table 2. Changes in Coil Currents

Section No.	Reference	Coil Currents (kA)		
		R = + 2.5 cm	Z = + 2.5 cm	Resistance = + 2.0 x 10 ⁻⁶ ohm
16	81.221	81.230	81.233	81.221
17	81.173	81.399	81.090	81.178
18	81.137	80.279	81.792	81.125
19	81.107	90.992	76.071	81.270
20	81.083	58.381	81.072	80.724
21	81.058	90.911	85.811	81.222
22	81.047	80.198	80.505	81.035
23	81.029	81.253	81.121	81.034
24	81.019	81.030	81.016	81.020

does not effect the current in section no. 30 and because a central solenoid coil is expected to be most sensitive to changes in position and resistance. The position changes used were for an increase in radius of 2.5 cm and independently an increase in the vertical height of 2.5 cm. The resistance was increased at first by 2.0×10^{-6} ohm which corresponds to several cm of cable going normal; effect was less than 1 part in 10. Higher resistances were input and the effects of an added resistance of 2.0×10^{-6} ohm are shown in Table 2. Only the neighboring coils nos. 16 to 24 are shown. The others are not effected.

The changes in the plasma fields due to the changes in section no. 20 were less than a few percent (about 5 G). The change in the field at the center of the TF coil adjacent to section no. 20 was about 12% (about 40 G).

Conductor Design

The operational current for each of the 56 parallel paths will be different for different paths. As an illustration, an 80 kA cable is designed and shown in Fig. 2; the cable is designed with low ac losses and full stabilization. The requirements of low ac losses and full stabilization are conflicting requirements; a compromise between the two must be made.

As illustrated in Fig. 2, the basic strand consists of a superconducting composite and six pure copper wires. The operational current is about 170A at 8 T. The pure copper wires are stranded around and soft-soldered to the superconducting composite. Full stabilization is realized within the basic strand. The basic strand is coated with 0.0125 mm thick organic insulation. The thin coating will greatly reduce the eddy current losses and yet allows limited current sharing within the basic cables. The coating is so thin that the heat transfer characteristics of liquid helium will be enhanced rather than hindered.

Six basic strands, each of 1.56 mm diameter, are transposed around a stainless steel cable of 1.56 mm diameter to form the 1000 A basic cable. The stainless steel cable serves as the structural member for the hoop load. It also enhances the mechanical integrity of the basic cable.

The final cable consists of 80 basic cables fully transposed around a G-10 strip of stainless steel

braid as shown in Fig. 2. The G-10 strip or stainless steel braid will serve as the backbone for the cabling and as the structural member for the hoop load. The thickness of the backbone strip will be about 1 mm. Turn-to-turn insulation is provided by G-10 strips 1 cm wide by 3 mm thick. The overall cross section of the 80 kA cable will be about 1.4 cm thick by 19 cm wide. The average current density within the coil structure will be about 3000 A/cm².

Coil Structure, Coil Winding and Coil Assembly

The proposed TF coil support cylinder is made of a fiberglass reinforced plastic such as G-10 or G-11. The cylinder is positioned inside the OH coil with axial support rings transmitting the load between the support cylinder and the TF coil as shown in Fig. 3.

The coil thickness for each of the coils in the central solenoid is about 20 cm. The coil i.d. is about 1.4 m and the o.d. is 2.0 m. It is proposed that liquid helium be introduced to the bottom of coils and the gas be vented at the top of the coil. Both the liquid channels and the gas venting channels are provided by micarta or G-10 strips. The thickness of these strips is about 0.6 mm. These strips run radially outward as shown in Fig. 4. The flow directions for both the liquid helium and the gas helium are indicated in Fig. 3. As shown in Fig. 4, 20% of the outer circumference of the G-10 support rings is slotted to disperse the helium gas. The separation of the liquid supply from the gas venting is important to assure the success of a fast pulsed OH coil in a pool boiling system.

The fiberglass rings, as shown in Fig. 4, will be used as the coil winding bobbins. The interturn cooling channels are provided by periodically slotted micarta strips. The dimensions of the cooling strips are indicated in Fig. 4. The strip is wound under some tension along with the cable conductor. Note that the width of the cooling strips is about 0.25 mm larger than that of the conductor. The cooling strips will transmit the axial compression.

To assemble the central solenoid, the sections of the coils, together with the supporting rings, will be placed on top of one another and bolted together in place as shown in Fig. 3.

AC Losses and Refrigeration Requirement

For the assumed filament diameter of 10 μm and equivalent twist pitch of 1.0 cm, the filament losses are in all cases between one and two orders of magnitude smaller than the eddy current losses and will be neglected in the following discussion. For an outside radius of 1.0 m and a peak field of 8.0 T, the program calculates an eddy current loss of $1.01 \times 10^5 \text{ J/m}$ per cycle or 1.11 MJ total for the solenoid. The total energy transferred is about 2.9 GJ, so the eddy current losses are a small fraction of the total energy transferred. For a 300 second cycle and a refrigeration factor of 500 between room temperature and 4.2 K, the eddy current heating contributes 1.85 MW to the refrigeration load.

Axial Pressure and Hoop Stress

The two components of magnetic pressure on the OH coils are axial pressure and radial hoop pressure. These pressures were computed and tabulated in Table 3.

Table 3. Radial Pressure and Axial Pressure of OH Coils

Coil No.	$\bar{\sigma}_r$ (psi)	$\bar{\sigma}_t$ (psi)
1	0.57	32
2	2.4	45
3	0.17	6.6
4	1.1	33
5	2.0	300
6	696	4260
7	886	4840
8	2400	10680
9	4050	12810
10	4910	14720
11	3210	9620
12	4640	13460
13	5380	15230
14-30	4840	13710

Cryostat Design

Figure 5 illustrates the cryostat cross section for the superconducting OH coils. To reduce eddy current losses, a nonconductive helium vessel is required. The illustrated design uses glass-reinforced epoxy filament-wound or braid-wound. The outer wall will be about 5 cm thick for the necessary strength as a vacuum vessel; for the coil insulation it must have close tolerance for fitting the TF coils vertical legs and the OH coil plastic supporting rings. The inner wall is made 10 cm thick to prevent buckling against a 1000 psi internal pressure. The helium vessel, as well as the weight of the coil, is supported by a low thermal conductivity ring which rests on the building foundation. Multilayers of superinsulation are installed with the reflective coating in segments to avoid eddy current heating in the insulation layers.

The helium vessel is insulated with a thermal radiation shield of laminated construction to minimize eddy current heating. The radiation shields are con-

nected to the radiation shield of the TF coil system. Multilayer superinsulation is used between the outer vacuum jacket and the radiation shield.

The outer vacuum jacket is constructed of stainless steel which is extended to the vacuum jacket of the TF coil system.

The cryostats for the remaining coil sections will have circular cross sections and will be supported by the reactor structure.

Current Leads and Refrigeration Requirements

The current leads, a total of about 60 pairs, are arranged in concentric circles at the top as shown in Fig. 5. This arrangement will minimize the stray fields due to the leads. The arrangements will also simplify the support of current leads because a clamping ring inserted between the leads will neutralize the external forces. The terminals of each pancake coil are brought into the spaces between the inner walls and the supporting plastic rings.

Because the coils are energized for 30 seconds out of a total cycle time of about 300 seconds, the refrigeration requirements for the 60 pairs of 80 kA leads could be significantly reduced if the leads were designed for the rms current of about 25 kA instead of 80 kA. For normal conducting power leads with gas cooling, Hilal, in a recent paper, has shown that the optimal condition will require 0.34 W/A for an ideal carnot engine at 300 K temperature, and it requires 1.36 W/A for the real cycle at 300 K. Therefore, the total refrigeration power for the 60 pairs of current leads will be 4.08 MW at room temperature. This is a significant amount of refrigeration power.

To reduce the refrigeration power requirement at room temperature, composite leads consisting of a superconducting section and a normal section must be used. By gas cooling both sections, and holding the junction at 15 K with a separate refrigeration loop, Hilal and Thomas et. al., have shown that for an optimum helium mass flow rate, the refrigeration power at room temperature for a composite lead can be about 33% less than for a normal metal lead. Thus, if Nb3Sn stabilized with sufficient copper is used as the superconducting section of the current lead, the refrigeration power for the leads will be reduced to 2.73 MW.

Power Supply For OH Coil

Sections of the OH coil are connected in parallel which leads to very large currents and relatively low voltages compared to the sections connected in series. The field in the OH coil must be reversed. This function is accomplished through the use of a reversing bridge.

A TNS cycle consists of charging the coil over a period of time, cycling the current from -I to +I in a short period of time for startup, maintaining a constant dI/dT during the burn time and recovering the OH coil energy at the end of the burn cycle.

Circuit Description

The reference design, for a single branch, of the parallel connected OH coil configuration along with the current and voltage waveform are shown in Fig. 6. The power supply starts charging through thyristor S3 and S4. The voltage required for charging the OH coil in 1 second to 80 kA is 381 volts.

The OH coil current is rapidly reversed to start the TNS burn cycle. This is accomplished by first driving the power supply into inversion, driving the current from -80 kA to zero in 1 second. When the current goes to zero, thyristors S3 and S4 stop conducting. Thyristors S1 and S2 are gated on to cause the coil to charge in the reverse direction. The voltage required to take the OH coil current from -80 kA to +80 kA in 2 seconds is 381 volts.

After the 2 second start up cycle, the current in the OH coil is 80 kA. A 30 second burn cycle begins, during which time the power supply voltage is lowered to maintain a constant or slightly increasing current as required to control the plasma current.

At the end of the burn cycle the energy stored in the OH coil is returned to the energy storage system by phasing back the power supply thyristor firing angles, causing the power supply to go into inversion. Once the energy is returned the cycle can be repeated. Thyristors S1 and S2 will stop conducting when the OH coil current goes to zero.

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Fig. 1.
Coil Configurations of TNS
Superconducting OH Coil

1000A BASIC CABLE

80 kA CABLE CONDUCTOR

Fig. 2.
1000 A Basic Cable and
80 kA Cable Design

Fig. 3.
Schematics of OH Coil
Support Structure

Fig. 5.
Schematics of Cryostat
Cross Section of OH Coil

Fig. 4.
Cooling Channels of OH
Section Coils

Fig. 6.
OH Coil Power Supply With
Reversing Bridge Circuit
and Waveforms

ANL EXPERIMENTAL PROGRAM FOR PULSED SUPERCONDUCTING COILS*

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Summary

Argonne National Laboratory (ANL) had recognized the clear advantage of a superconducting ohmic-heating (OH) coil and started an aggressive development program in FY 1977. The main objectives for FY 1977 are to develop cryostable basic cable configurations with reasonably low ac losses, to develop 12 kA cryostable cable, using it to design and build a 1.5 MJ pulsed coil, and to develop a rather inexpensive large fiberglass reinforced helium cryostat for the 1.5 MJ pulsed coil. The principal objective in building the 1.5 MJ ac coil is to demonstrate ac cryostability of a large coil ranging from 2 T/s up to 12 T/s. Another objective in the pulsed coil program is to determine the feasibility of parallel coil operation in order to avoid excessive voltage and current requirements and to minimize the number of turns for the equilibrium field (EF) coils, should the EF coils be connected in parallel with the OH coils. A two-coil section model using the 11 kA cable will be built and tested.

Introduction

The conceptual design studies of tokamak experimental power reactors undertaken at Argonne and elsewhere over the last three years have identified the need for large-volume pulsed superconducting magnet systems to contain and drive the plasmas in these ignition devices. Because of the large stored energy of the tokamak magnet coils, they must be cryogenically stable, but they must also tolerate rapid cycling.

At Argonne, considerable progress has been made to identify critical elements of tokamak coil design and required technology development. In July of 1975, a program plan was suggested¹ which would develop the technology required to assure successful operation of the pulsed coils and efficient and inexpensive energy storage and transfer systems when needed for future tokamak reactors.

Scope and Objectives

A set of superconducting ohmic heating coils (OHC) is the selected design for the ANL/GA TNS OH-Coil system² and, in all likelihood, future tokamak reactor systems. The selection is based on the results of critical evaluations of the Argonne EPR design studies³ of the past two years (FY 1975 and FY 1976) and based on the results of the on-going GA/ANL TNS OH-coil conceptual design studies.

The main objectives for FY 1977 are to develop cryostable basic cable configurations with reasonably low ac losses, to develop 11 kA cryostable cable, to design and build a 1.5 MJ prototype pulsed superconducting OHC, and to develop a rather inexpensive large fiberglass reinforced helium cryostat for the 1.5 MJ pulsed coil. All of these efforts are important steps to the advancement in the state-of-the-art of pulsed superconducting magnet technology. The principal objective in building the 1.5 MJ ac coil is to demonstrate ac cryostability of a large coil with the dB/dt ranging from 2 T/s up to 12 T/s.

Another important objective in the FY 1977 pulsed coil program is to determine the feasibility of

parallel coil operation. The present ANL/GA TNS OHC design, for which ANL has prime responsibility, considers parallel operation in order to avoid inducing equilibrium field (EF) coil currents during OHC pulsing. The studies so far have indicated that parallel operation of multiple OHC sections appear feasible and perhaps advantageous.⁴ This means that OHC can be divided into many parallel paths and thus the OHC can be charged with a relatively low voltage and high current. A two-coil section model using the 12 kA cable will be built and tested in the next few months.

Present Results

Basic Cable Development Program

The cable configuration giving the best compromise between stability and ac losses is illustrated in Fig. 1. The six pure copper wires are soldered to the superconducting composite forming an essential current sharing subgroup. A thin coating of organic varnish is brushed on the surface of each of the three subgroups in the basic cable. The varnish coatings serve to reduce the eddy current losses among the subgroups. The coating is thin enough, however, that limited current sharing among subgroups will be allowed. The criterion chosen for cryostability is such that both minimum propagating current and recovery current are greater than critical current. The basic cable is rated at 405 A at 5 T.

Small Testing Coils

To study the ac losses and the magnet current sharing, three small testing coils⁵ were wound using three different basic cables. The ac losses are measured both by a calorimeter method and by an electrical method. To study the current sharing of small coils, a potentiometer is used to balance the inductive voltage of the testing coils. The specifications and the results of these testings were presented in reference 5.

Development of Large Fiberglass Reinforced Plastic Dewars

For superconducting OHC, stainless steel helium cryostat will have excessive ac losses. Therefore, a nonmetallic cryostat with low helium permeation must be developed. As a first step, a plastic cryostat is developed for a 1.5 MJ pulsed coil. The plastic cryostat, as shown in Fig. 2, consists of two tanks with 100 layers of superinsulation between. The superinsulation is slit to reduce the eddy current heating, as shown in Fig. 3. The inner tank has an I.D. of 91.4 cm, a depth of 152.4 cm, and a wall thickness of 0.95 cm; while the outer tank has an I.D. of 107 cm, a depth of 156.5 cm, and a wall thickness of 1.27 cm. Both tanks are made of fiberglass reinforced Hetrion 31 polyester with 35% glass component.

The cryostat was filled with liquid nitrogen without superinsulations. Afterwards, many patch-like small cracks were found on the inner surface of the inner tank. Therefore, the inner tank was reinforced by about 1 cm thick wet-wound fiberglass epoxy.

As shown in Fig. 2, the cryostat cover is a 6.3 cm thick micarta plate. A 30 cm thick styrofoam cut to fit the inner diameter of the inner tank is attached to the micarta cover for radiation shield.

*Work supported by the U. S. Department of Energy.

with 100 layers of superinsulation, the cryostat was filled with an 80 cm deep liquid helium. The steady state heat leak measured was about 0.35 W. A 10^{-7} Torr vacuum was achieved with cryopumping. Helium permeation was not detectable over a period of 48 hours.

On-Going Program

1.5 MJ Coil Fabrications

The 1.5 MJ successful construction will be the most important milestone of the FY 1977 ANL pulsed coil program. The design of the coil and fabrication of G-10 coil form are completed. The magnet characteristics are tabulated in Table 1. The coil cross section is shown in Fig. 4.

The operational current is 11 kA. The design of the cable is shown in Fig. 1. Fabrication of a 600 m long cable will be completed in November 1977. Five-hundred thirty-one meters of cable will be used to wind the 1.5 MJ pulsed coil. The remaining 69 m will be used to wind two 20-turn pancakes for parallel operation tests which are described in the following section.

In addition to the demonstration that the 1.5 MJ OHC is cryostable, another important goal of building the coil is to energize and deenergize the magnet over a wide range of dB/dt . Furthermore, to study the conductor motion and its frictional heating of high current cable, pressure transducer and unbalanced voltage method will be used to measure the conductor motion and the energy release due to the frictional heating.

The power supply, as shown in Fig. 5, planned for charging the 1.5 MJ coil is a 7 MW ignitron-type power supply with 700 V and 11 kA peak ratings.

Parallel Operation of Sectional Coil

The feasibility of parallel operation will be demonstrated in two 20-turn pancake coils. The two pancake coils will be tested within the field bore of the 1.5 MJ coil.

FY 1978 Program Goals

The objectives of FY 1978 are to study the parallel operation of pulsed coils, to develop a 50 kA cryostable cable, to build a small test coil with the 50 kA cable, and to perform a preliminary engineering design of a 100 MJ model coil.

100 MJ Demonstration Coil

The ultimate goal of the ANL Pulsed Coil Program is to design, build, and test a demonstration-size, 100 MJ pulsed superconducting coil. The coil would be tested with a 100 MVA motor-generator-flywheel set and energy conversion system available at ANL.

Schedule and Costs

With approximately \$3 M funding over a five-year period, the ANL Pulsed Coil Program could be ready to start the fabrication aspects of TNS superconducting OHC by FY 1982. Details are presented in Table 2.

Table 1. Magnet Characteristics of 1.5 MJ Prototype Superconducting OHC

Central Field	4.3 T
Peak Field	4.7 T
Operational Current	11 kA
Inductance	24.9 mH
Stored Energy	1.5 MJ
Coil I.D.	41.6 cm
Coil O.D.	86.4 cm
Axial Length	58.1 cm
No. of Layers	18
No. of Turns/Layer	15
Total No. of Turns	270
Layer-to-Layer Spacing	4.76 mm
Conductor Current Density	5760 A/cm ²
Cryostable Recovery Heat Flux	0.35 W/cm ²
Average Current Density	2274 A/cm ²
AC Losses	3.7 kW at 8 T/s
Max. Radial Magnetic Pressure	12 ksi
Max. Axial Magnetic Pressure	4 ksi

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Fig. 2. Plastic Helium Cryostat

Fig. 3. 100 Layers Super-insulation on the Inner Tank

Fig. 4. 1.5 MJ Coil Cross Section

Fig. 5. 7 MW Power Supply

Table 2. ANL PULSED COIL PROGRAM

(In Thousands of Dollars)

Tasks	FY 1977	FY 1978	FY 1979	FY 1980	FY 1981
1. <u>High Current Cable Development</u>	\$34	\$105	\$ 105	\$ 105	\$105
A. Basic Cable Study	4	5	5	5	5
B. 12 kA Cable Study	30	-	-	-	-
C. 50 kA Cable Study	-	100	-	-	-
D. 100 kA Cable Study	-	-	100	-	-
E. TNS OHC Cable Study	-	-	-	100	100
2. <u>Cryostability Study of Pulsed Magnet</u>	-	10	55	55	25
A. Heat Transfer and Cooling System	-	-	25	25	-
B. Mechanical Perturbation	-	5	25	25	25
C. AC Losses Study	-	5	5	5	-
3. <u>Plastic Cryostat Development</u>	5	10	50	125	50
A. 36 in. Diam. x 60 in. Deep Dewar	5	-	-	-	-
B. 100 MJ Coil Cryostat Development	-	10	25	75	-
C. TNS OHC Cryostat Development	-	-	25	50	50
4. <u>Build Model Coil</u>	27	10	300	1075	200
A. 1.5 MJ	22	-	-	-	-
B. Parallel Operation Coils	5	10	25	75	-
C. 100 MJ Design and Engineering	-	-	25	50	50
Construction	-	-	650	350	50
Test	-	-	-	100	100
5. <u>TNS Superconducting OHC Design</u>	-	50	75	100	100
A. Conceptual Design	-	50	-	-	-
B. Detail Design	-	-	75	100	100
6. <u>100 MVA Power Supply Conversion</u>	-	-	100	100	100
TOTAL FUNDING REQUIRED	\$66	\$185	\$1085	\$1435	\$580

HEAT TRANSFER AND FLOW CHARACTERISTICS OF LIQUID HELIUM IN NARROW CHANNELS*

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Summary

This work was undertaken to study the heat transfer and flow characteristics in narrow channels under both transient and steady state conditions. The void fraction in the channel under these conditions was calculated from the capacitance measurements. It is found that under steady state conditions the void fraction does not change abruptly when the critical heat flux is exceeded. It was also found that for a relatively long heat pulse (94 msec) vapor bending will not occur if the critical heat flux is not exceeded and the energy density is less than 700 mJ/cm^3 .

Introduction

Superconductive magnets cooled by pool boiling of liquid helium have channels between the conductors for helium circulation. Heat transfer and flow characteristics in such narrow channels may determine the degree of the stability of these magnets. It is especially necessary to design large magnets with channels large enough to avoid any vapor binding under both steady state and transient conditions.

Heat transfer to liquid helium in channels has been studied by many investigators.¹⁻⁶ These studies provide extensive data on the boiling of liquid helium in channels under steady state conditions. Iwasa⁷ et al studied heat transfer and the possibility of vapor locking in narrow channels under transient conditions by measuring the temperature rise of the surface. Although this is important in determining the stability of the magnet it is not an indication of vapor locking. It is the purpose of this study to investigate the heat transfer characteristics of liquid helium channels under both transient and steady state conditions. We are especially interested in determining the amount of helium vapor present in the channel during a heat pulse or a steady state heat load.

Experimental Apparatus and Measuring Techniques

The different parts of the apparatus are shown in Fig. 1. A liquid helium channel 0.006 cm wide and 3.18 cm high is enclosed between copper blocks as shown in Fig. 1. The channel width can be varied by having different thickness copper foils between the copper blocks. The G-10 blocks shown in the figure are used for insulation.

The heaters used are made out of stainless steel foils 0.0025 cm thick and each heater was epoxied to a copper plate. The plate is sandwiched in between the copper and the G-10 blocks. The upper and the bottom of the copper blocks were also insulated by epoxy.

- (1) SHORT SIDES G-10 INSULATION BLOCKS
- (2) LONG SIDES G-10 INSULATION BLOCKS
- (3) COPPER BLOCKS
- (4) HEATER ASSEMBLY
- (5) COPPER FOILS
- (6) LIQUID HELIUM CHANNEL

Fig. 1 Schematic of the test section.

The temperature of the copper blocks is measured using gold-.07 at.% iron versus chromel thermocouples. The capacitance between the two sides of the channel is measured to determine the volume fraction of the vapor in the channel. The capacitance measurement technique will be reported elsewhere. With the large surface area used capacitance change of .25 picofarad per one percent void fraction can be obtained. This capacitance change was enough to measure in a very short time with the capacitance bridge designed and used.

Relation Between Void Fraction and Capacitance Change

Consider the channel shown in Fig. 2 where a mixture of helium liquid and vapor coexist. It can be shown that the capacitance of this channel is given by

$$C = \frac{\epsilon_l L}{w} \int_0^1 \frac{d\bar{z}}{1 + \alpha(\epsilon_l/\epsilon_g - 1)} \quad (1)$$

where

C is the capacitance,

ϵ_l and ϵ_g are the dielectric constants of liquid and vapor respectively,

L is the height of the channel,

w is the width of the channel,

$\bar{z}$ is a dimensionless coordinate ($= Z/L$) as shown in Fig. 2 and

u is the void fraction.

In the present case ϵ_l/ϵ_g is small ($\pm .04$) and α cannot exceed unity. Equation (1) therefore can be

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Fig. 2 Model of a channel for capacitance calculations.

reduced to

$$C = \frac{c_L L}{w} \int_0^1 [1 - \alpha(\frac{c_L}{c_g} - 1)] dz \quad (2)$$

and the change in capacitance due to the presence of vapor is given by

$$\Delta C = \bar{\alpha} \Delta C_{\max} (\frac{c_L}{c_g} - 1) \quad (3)$$

where $\Delta C_{\max}$ is the capacitance change with helium vapor only in the channel and $\bar{\alpha}$ is the average void fraction.

The relation between the dryness fraction, x , and the void fraction is given by

$$x = \frac{\alpha \rho_v}{\alpha \rho_v + (1-\alpha) \rho_L} \quad (4)$$

where

ρ_v is the density of the vapor and

ρ_L is the density of the liquid.

The above relation is valid only when the slip ratio, vapor velocity/liquid velocity, is one.

Results

The experiment was conducted having either a steady state or transient current to the heaters. In either case the temperature difference and the capacitance changes were measured.

Steady State Results

The temperature difference is plotted vs. the heat flux for two cases A) one side is heated and B) the two sides are heated, Fig. 3. The capacitance change $\Delta C/\Delta C_{\max}$, the void fraction α and the dryness fraction x are plotted versus the heat flux for both cases as shown in Figs. 4, 5, 6 and 7. It should be mentioned that the void fraction shown in these figures, as calculated from the capacitance measurements, represent the average void fraction. The void

fraction at the exit of the channel is expected to be higher and cannot be calculated without studying the variation of the void fraction along the height of the channel. It is also important to indicate that the heat flux in the present case is per unit surface area (not per unit cross sectional area of the channel). It can be seen from these results that the walls of the channel can be at high temperatures (≈ 10 K) without drying out the channel. Exceeding the critical heat flux in the channel under state conditions therefore will not necessarily result in vapor locking. Also it is important to observe that smoothly increasing the heat flux does not abruptly change the void fraction when the critical heat flux is exceeded.

It is interesting to notice that the critical heat flux heating one side is half the critical heat flux heating both sides. Also the void fraction resulting from heating one side is about half the void fraction heating both sides at the same heat flux. This means that the bubbles generated are small (the bubble diameter is smaller than the channel width) and flow close to the surface. More work is in progress to explain this phenomena.

Transient Results

The transient test was conducted in the following manner. The power supply was triggered to the desired power level by a signal from a wave generator. The thermocouples output, capacitance bridge output, and current and voltage of the power supply were recorded. We show in Figs. 8 and 9 both traces of the current rise of the power supply and the capacitance change as examples. Both the duration of the signal and the power supply current can be varied to the desired level. Figure 10 is a plot of the temperature difference versus the heat flux for 120 msec pulse. As can be seen from this figure the temperature difference was small when the heat flux was less than the critical heat flux of approximately $.2 \text{ W/cm}^2$. When the critical heat flux is exceeded, however, the temperature differences becomes large. It is therefore important not to exceed the critical heat flux for long-time pulses in order to avoid excessive temperature rise.

In Figs. 11 and 12 the capacitance change, the void fraction and the dryness fraction are plotted against energy density (energy density is defined as the energy generated by the heater divided by the channel volume). In this case the duration of the pulse is 94 msec and the energy density is varied by changing the power supply current. As can be seen from these figures, that for energy densities of $700 \text{ millijoules/cm}^3$ the void fraction is close to 100 percent and the channel becomes dry. The accompanied temperature rise however was negligible (< 1 K). It might be concluded therefore that the heat transfer has not been hindered having the channel full of helium vapor and the helium vapor is enough for heat transfer. It may be mentioned however, that the maximum heat flux in this case never exceeded the critical heat flux.

Fig. 3. Temperature difference vs. heat flux under steady state conditions A) one side of the channel is heated B) two sides of the channel are heated

Fig. 4. Relative capacitance change vs. heat flux with the two sides heated and under steady state conditions.

Fig. 5. Void fraction and dryness fraction vs. heat flux under steady state conditions with one side heated.

Fig. 6. Relative change in capacitance with one side heated.

Fig. 7. Void fraction and dryness fraction vs. heat flux under steady state conditions heating both sides.

Fig. 8. Oscillogram of the power supply current. The time scale is 20 msec/cm and current scale is .4 amp/cm.

Fig. 9. Oscillogram of A) the triggering signal (.2 V/cm) and B) the capacitance bridge output (5 V/cm).

Fig. 10. Temperature difference vs. heat flux for 120 msec pulse.

Fig. 11. Relative capacitance change vs. energy density for 94 msec pulse.

Fig. 12. Void fraction and dryness fraction vs. energy density for 94 msec pulse.

Conclusions

Experimental work is still under way to provide more information on the conditions under which vapor locking may occur. We may conclude from the limited amount of data obtained, however, that large temperature rises will not take place for small energy densities (less than 700 mJ/cm³) as long as the critical heat flux is not exceeded. Excessive temperature rise may occur due to high heat flux or high energy density or both. The relation between these two factors are being studied and will be published later.

From the steady state results it can be seen that for heat fluxes close to the critical heat flux the void fraction is about 50%. For higher heat fluxes the void fraction is continuously increasing. Under steady state conditions large temperatures may occur without drying out the channel. For the large gap used the critical heat flux and the void fraction heating one side of the channel is half the critical heat flux and the void fraction heating both sides which is an indication that the bubbles flow close to the surface.

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LIMITS ON THE FIELD OF OHMIC HEATING SOLENOIDS, APPLIED TO A TOKAMAK TNS*

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Summary

If the ohmic heating solenoid for the TNS or other large tokamak is an ungraded cryostable superconducting solenoid, with NbTi at 4.2 K as the superconductor, then the smallest outer diameter is not achieved at the highest attainable field. There is a lower optimum field which minimizes the outer diameter for a given volt-second requirement. At higher fields the mean diameter decreases; but the high fields require more superconductor, more copper stabilizer, more stainless steel for support, and more liquid helium coolant. For the GA-ANL design for TNS, the optimum field is 7.55 T and the minimum outside diameter for the solenoid is 2.15 m.

If, on the other hand, the solenoid is graded, with more NbTi, copper, and stainless steel on the inner turns where the field is higher, then the volt-seconds can always be increased, for a given outer diameter, by adding more turns at a higher field inside until either the critical field is reached or the solenoid bore is filled. However, the material and money required to add a few more volt-seconds increases rapidly with field.

Introduction

Because the outer diameter of the superconducting OH solenoid of a tokamak influences many other dimensions of the device, that diameter should be as small as possible to minimize overall cost. However, providing a given flux change with a smaller diameter solenoid requires that the field be higher, and the diameter is thus constrained by the highest field B that can be attained in the solenoid and the maximum rate of change B of that field. The field is limited by the critical field of the conductor, and B is limited by eddy current and filament heating losses.

However, it turns out that for an ungraded cryostable superconducting solenoid the lowest outer diameter D_0 is not achieved at the highest field. The mean diameter decreases as $B^{-1/2}$, but higher fields require more superconductor, more copper, more stainless steel for support, and larger helium channels for added cooling. There is an optimum B which minimizes D_0 , the sum of the mean diameter and the thicknesses of NbTi, copper, stainless steel, and helium. Reference 1 describes the calculations in more detail.

If the conductor is graded, there is an absolute minimum D_0 only when the peak field reaches the critical field or when the bore is filled. These results are described below.

Limits on B

The absolute limit on the field B is determined by the field dependence of the critical current density of the NbTi superconductor at a specified temperature. Variation of critical current density with field and temperature² is well fit by the expression

$$J_c = (T_c - T) (3.352 - 0.3607 B + 11.929/T) \times 10^4 \text{ A/cm}^2 \quad (1)$$

where T is the temperature in Kelvin, B the magnetic field in Tesla, and J_c the critical current density. The critical temperature T_c is given by

$$T_c = 8.56 (1 - B/14.7)^{1/2} \quad (2)$$

Operation at 4.2 K with an allowed temperature variation of 0.5 K requires that T_c be 4.7 K or higher, which limits fields to 10.3 T or less.

Limits on B

The conductor stores almost no heat; thus heat must be transferred to the helium as it is generated by hysteresis and eddy currents.

The average eddy current heat per unit length of conductor circumference P_{cu}/L during the field change is given by³

$$P_{cu}/L = \frac{2 \bar{B}^2 \dot{B}^2 A_{cu}}{3 \pi \rho} \quad (3)$$

where A_{cu} is the total cross-sectional area of the copper stabilizer, ρ is the electrical resistivity, $\dot{B}$ is the effective twist-pitch of the filaments, and $\bar{B}$ is the rate of change of the field B. Equation (3) was derived assuming a constant $\bar{B}$. The B dependence of Equation (3) means that as B increases, larger helium channels are required to absorb the heat without vapor locking. In general however, an absolute limit on B requires consideration of stability as well as eddy current and filament heating.

OH Solenoid Diameter

In providing a given flux change $\Delta\Phi$, a higher field in the OH solenoid permits a smaller mean radius R of the solenoid. On the other hand, higher fields require more superconductor, more copper, more stainless steel for support, and larger helium channels. The outside diameter D_0 of the solenoid is given by:

$$D_0 = 2 \bar{R} + w_{NbTi} + w_{Cu} + w_{He} + w_{SS} \quad (4)$$

Superconductor Volume and Filament Losses

To provide a field B in an OH solenoid of height h requires a number of amp turns

$$I = Bh/\mu_0 \quad (5)$$

in the long solenoid approximation, which is sufficiently accurate. The thickness of superconductor is $B/\mu_0 J_c$, with J_c given by Eq. (1).

The heat loss per cycle due to hysteresis in the filaments is given by⁴

$$Q_f = (4/3\pi) V_{NbTi} J_0 B_0 d_f \ln(1 + B/2B_0) \quad (6)$$

where V_{NbTi} is the superconductor volume, $J_0 = 22.735 \times 10^4 \text{ A/cm}^2$ is the critical current at zero field and $B_0 = 4.14 \text{ T}$ is a field characterizing the critical current curve. The filament diameter d_f is taken to be 10 μm .

* Work supported by the U.S. Department of Energy.

Copper Volume and Eddy Current Losses

Above, the heat transfer to the helium was equated to the eddy current heating to find an upper limit on B . For a more complete calculation, it must be equated to the eddy current heating plus the filament heating; plus the heat developed when the conductor goes normal; i.e.

$$q_h p = P_{Cu}/L + P_f/L + J_{Cu}^2 \rho A_{Cu} \quad (7)$$

where J_{Cu} is the current density in a normal section of conductor, and the other symbols are defined above. Equation (7) can be rewritten as a quadratic equation in A_{Cu} , taking (A_{Cu}/p) as a constant.

$$\left[\frac{2B^2 \rho^2}{3 \pi p} - \frac{q_h}{A_{Cu}/p} \right] A_{Cu}^2 + (P_f/L) A_{Cu} + I^2 \rho = 0 \quad (8)$$

Equation (8) can be solved for A_{Cu} and thus for w_{Cu} . The total eddy current heating per pulse Q_{Cu} is found from the power and time.

Helium Volume

If only a fraction γ of the helium volume V_{He} can be vaporized, then heat balance requires that

$$Q_{Cu} + Q_f = q_L \gamma V_{He} \quad (9)$$

where q_L is the latent heat of helium, on a vapor volume basis. The thickness of helium space is then found from V_{He} .

Stainless Steel Volume

The tension in the solenoid will be given by $F = IRB/2$. This tension should be taken by the stainless steel without exceeding a stress $\sigma = 60,000$ psi $= 4.14 \times 10^8$ Nt/m², which determines w_{SS} . The stainless steel is assumed to be distributed as needed to support against the tension.

Limit on OH-Solenoid Diameter: Results

A computer code BBDOT has been written to find the optimum field B which minimizes D_o , the outside diameter of the solenoid as expressed in Equation (4). For specified parameters, D_o is calculated at 0.5 T intervals from 3 T to 10 T, and then at 0.1 T intervals near the minimum D_o .

Certain of the parameters deserve special mention. For example, the eddy current heating is proportional to the square of the twist-pitch λ . Actual twist-pitches of 0.4 cm to 4 cm are feasible depending on the size of the strands, but for a conductor of many small strands, strand-to-strand eddy currents may well be present. A twist-pitch of 1 cm was chosen as the standard case; in Figure 1, B and D_o are plotted as functions of filament twist-pitch.

Other sensitive parameters have been studied as well. In Figure 2, B and D_o are plotted as functions of q_h , the effective heat transfer to the helium. Figure 3 shows how the optimum B and D_o vary with the flux swing $\Delta\phi$; a factor of ten increase in $\Delta\phi$ results in a factor of 2.68 increase in D_o . Optimum values of B as a function of the rise time τ appear in Figure 4 along with the corresponding values of D_o and $B = 2 B/\tau$.

Figure 1. Optimum Field and Minimum OH Solenoid Diameter as Functions of Filament Twist Pitch.

Figure 2. Optimum Field and Minimum OH Solenoid Diameter as Functions of the Effective Average Heat Transfer Coefficient q_h .

Figure 3. Optimum Field and Minimum OH Solenoid Diameter as Functions of Volt-second Requirement, $\Delta\phi$.

Of the three parameters; pitch, τ , and q_h appearing in Figures 1, 2 and 4, D_0 is least sensitive to q_h because q_h determines only the amount of copper necessary to transfer heat into the helium. On the other hand the pitch determines the eddy current heating; and τ , the rate of heat generation in both the copper and superconductor. Thus the pitch and rise time influence the amount of helium required as well as the amount of copper.

As would be expected, near a minimum value of diameter the diameter changes only slowly with field; thus a sizeable decrease in field may produce only a small increase in diameter. For the standard parameter values a field decrease of 0.56 T or 1.25 T increases the diameter only 1 cm or 4 cm respectively above the minimum value.

Relaxation of Assumptions: Graded Solenoid

Calculations have been done only for NbTi at 4.2 K. It is expected that calculations at a different temperature or with Nb₃Sn would still show a minimum diameter as a function of field, but with different values for D_0 and B. However, if the solenoid is graded, that is if the amount of superconductor, support material, and copper per turn vary across the solenoid, then D_0 continues to decrease with increasing B until the inner radius of the solenoid becomes zero or the critical field is reached. This was pointed out by Roger Boom⁵ in the following argument. Into a solenoid with minimum D_0 for a given $\Delta\phi$, as described above, we insert a smaller solenoid with a higher B. The overall $\Delta\phi$ then increases; we can reduce it to its initial value by decreasing D_0 , in contradiction to the claim that D_0 was minimized.

In fact, the above argument suggests how a graded solenoid can be calculated. Because a long solenoid has no external field, each incremental thickness of the OH solenoid can be calculated without knowledge of the part of the solenoid inside it. A computer code BGRADE, a modification of BBDOT, has been written. An outer radius, background field, and increment of field are specified; and the superconductor, copper, stainless steel, and helium requirements for each successive layer of the magnet are calculated, along with the cumulative field, volt-seconds, and material volume. The field increment can be small, to establish the theoretical limits, or large, to represent an actual coil graded every several turns.

Figures 5-10 show the results of computations on continuously-graded OH solenoids, using BGRADE. Figure 5 shows how the field, attainable volt-seconds, and current density vary across a graded coil with a fixed outer radius 1.0 m. Figure 6 shows how the total cross sectional area of NbTi, copper, stainless steel, and helium increase with the volt-second requirements, for that outer radius. As more turns are added at the inside radius, they experience a higher field, require more NbTi, copper, and stainless steel, and yet contribute fewer volt-seconds. Figures 7 and 8 show respectively how the peak field and the cost of superconductor increase with the volt-second requirement, for three different values of the outer radius. The superconductor is costed at \$60/kg, and the solenoid is assumed to be 10 m high. They suggest that there is an economical limit to the available volt-seconds for a specified outer radius.

Figures 9 and 10 show how peak field required and the cost for a graded solenoid vary with the outer radius and with the required volt-seconds. They too suggest that for a given volt-second requirement, the outer radius can be squeezed in only at considerable

Figure 4. Optimum Field, Minimum OH Solenoid Diameter and Rate of Change of Field as Functions of Resistive τ .

expense. The cost in Figure 10 is based entirely on the material cost; for materials alone, a cost unit is \$0.25 M; multiplied by four to account for fabrication cost, a cost unit is \$1.0 M. The costs of the energy transfer system, which rise sharply with increasing field and stored energy, are not included.

The graded coil, like the ungraded, is sensitive to the value of the parameters in the computation. Unlike the graded coil, the ungraded shows no clearcut optimum field and minimum diameter. The increasing cost of a smaller solenoid operating at a higher field must be balanced against the savings of an overall smaller TNS.

Acknowledgements

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Figure 5. Field, Volt-seconds Available, and Current Density as Functions of Radial Position in a Graded (NH) Solenoid with Outer Radius 1.0 m.

Figure 7. Variation of Peak Field with Volt-Second Requirements in a Graded OH Solenoid, for Three Outer Radii.

Figure 6. Required Cross Sectional Area of NbTi, Copper, Stainless Steel, and Helium as Functions of the Volt-Second Requirements for a Graded OH Solenoid with Outer Radius 1.0 m.

Figure 8. Variation of Superconductor Cost with Volt-Second Requirement in a Graded OH Solenoid, for Three Outer Radii.

Figure 9. Variation of Peak Field with Outer Radius of a Graded OH Solenoid, for Various Volt-Second Requirements.

Figure 10. Variation of the Cost of a Graded OH Solenoid with its Outer Radius, for Various Volt-Second Requirements. Cost Units are Explained in the Text.

ALTERNATE OHMIC HEATING COIL ARRANGEMENTS FOR COMPACT TOKAMAK*

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Introduction

In this report, the results for a number of ohmic heating (OH) coil arrangements which will allow the reduction of the major radius of Experimental Power Reactor (EPR) tokamaks will be given. In each case the results are compared, at least indirectly, to the reference case, Fig. 1, which has the OH solenoid inside the central core of the reactor.

The goal for the alternate geometries studied was to stay within the requirements imposed by the EPR conditions on the plasma and to produce as much or more OH V-s as the reference case. The requirements which were imposed are:

1. achieve a toroidal magnetic field at the plasma center ≥ 3.4 T without exceeding the peak field capability of the superconducting materials while maintaining an inboard blanket and shield thickness of about 0.9 m,
2. produce the OH V-s without developing magnetic fields over the minor cross-sectional area of the plasma,² and
3. produce the OH V-s without developing magnetic fields of more than 0.5 T anywhere in the TF coil region.³ (A lower value would be better.)

Figure 1 shows the reference case and defines some of the terminology used in the other cases. The geometries which are more fully discussed later in the report are shown in Fig. 2. In all the figures, the equilibrium coils are not included because they are beyond the scope and intent of this study. The reference case has the solenoid coil located inside the central core and the profile coils located outside the TF coil envelope. Thickness of the TF coil was scaled from the latest EPR-77 design at ANL according to Ampere's law. The magnetic field at the outside radius of the inner leg was kept at a value of 9 T for all studied cases. Because it is a "long" solenoid and placed in the central core of the TF coil, it inherently satisfies requirements 1, 2, and 3 most easily. Case 2, Fig. 2, has a concentric pair of completely-shielded long solenoids surrounding the inner legs of the TF coils and a set of completely-shielded profile coils surrounding the inner legs of the TF coils. Because the outside radius of the inner legs of the TF coils remains constant in our case studies, the major radius of the plasma must be slightly increased for this case. Case 3 has 16 long, narrow solenoids located inside the blanket and shield and completely-shielded profile coils inside the TF coil envelope. Almost the entire circumference is occupied with solenoids in this case. It introduces the concept of making the shield electrically active instead of passive as it is in most other designs.^{4,5} The shielding space is partially filled with electrical conductors and used to produce V-s. It is inherently easier to assemble and maintain this coil than it is for case 2. Case 4, an extension of case 3, has 16 long narrow solenoids inside the blanket and

shield. However, the solenoids are contoured around the plasma. This configuration is expected to eliminate the need for some of the profile coils inside the TF coil envelope. It should be comparatively simple to install and maintain this type of coil system since the solenoids could and should be designed to be removable through the space between the TF coils. Cases 2', 3', and 4' have the same arrangements of OH coils as cases 2, 3, and 4 respectively, but with the addition of a long solenoid in the central core to increase the V-s capability.

Alternate Geometries

In the following sections the various alternate geometries for the OH coil will be discussed in some detail. These discussions are based only on the geometries of the current distributions shown in the accompanying figures. The exact values for the supply currents or the current densities used are beyond the scope of this paper, but current densities of less than 3000 A/cm² are generally used in the various coil sections.

The calculations for these various geometries were done with a code, AIRCOIL, developed at Argonne.⁶ This code will calculate the magnetic fields at specified points in a radial plane for a maximum of 60 input coil sections in a cylindrically symmetric magnet structure. It will also iteratively adjust the locations and the numbers of turns for specified coil sections in order to reduce the magnetic fields in a specified region. This optimizing method was used to help us establish the attainability of the requirements given above for the OH coil. For the solenoid ring options, the resultant fields in a radial plane which were generated by the solenoid coils in the ring had to be determined. This involved the superposition of the magnetic fields in the radial plane of interest for each coil in the ring. The results of this calculation were then input into AIRCOIL as a constant bias field and the optimizations of the locations and numbers of turns for the cylindrical, profile coils were done in the customary way.

The reference coil shown in Fig. 1 is the usual design with a central solenoid used to generate the core flux and the profile coils to keep the core flux in the return path from entering the plasma region. The major radius for the plasma in this case is 4.2 m. This coil develops a core field of 5.02 T with an integrated flux of 11.4 Wb between the vertical axis of the machine and the inner edge of the plasma. The maximum flux linkage that could be developed for an OH coil with the outside diameter shown (190 cm) is about 16 to 18 Wb.⁷ The reference core field was chosen at about 5 T, knowing it is not a maximum value but that it is probably an easily attained value and will not require any more detailed calculations to establish this fact as would a 50% higher value. This type of coil is familiar to OH coil designers and can easily be made to satisfy the requirements for the stray fields in the plasma and TF coil regions.

The first alternate OH coil geometry, case 2, is shown in Fig. 3. This case consists of two concentric solenoids located just outside of the inner legs of

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the TF coils. The major radius of the plasma was increased to 4.62 m to allow room for these coils while maintaining the full 0.9 m thickness for the inboard blanket and shield. Above and below these solenoids are the profile coils, which consist of cylindrical coils with some carrying reverse currents and which direct the flux from between the solenoids to travel outside the plasma region. The flux density between the solenoids is about 6.4 T. This coil satisfies requirements 2 and 3 summarized above; it produces 18.5 Wb of core flux and gives a large plasma area with less than 10 G and does not produce more than 0.3 T in the TF coil region. It does, however, sacrifice some of requirement 1 because of the necessary increase in the major radius of the plasma. Another disadvantage of this case is the fact that the current density in the solenoid coils is around 5000 A/cm², which is too high for a real coil at the present time. Another disadvantage of this case is the fact that all of the OH coils lie inside the TF coil envelope. This will produce serious problems during the fabrication and repair of this machine.

The second alternative, case 3, is shown in Fig. 4. This geometry contains 16 solenoids uniformly spaced at a radius of 210 cm in a ring between the plasma and the TF coils. These solenoids have an outside diameter of 70 cm and are 7.0 m high. They are placed so that there is about 15 cm of shield between the plasma and the solenoid coils and about a 10 cm space between the solenoid ring and the inner leg of the TF coil. It is anticipated that the core of each of the solenoid coils can be filled with a non-magnetic and electrically-resistive shielding material. The major radius of the plasma is 4.2 m but the effective inboard shield and blanket thickness is somewhat reduced at periodic locations around the axis of this machine. The flux density inside each solenoid is 6.3 T giving a net core flux of 24.2 Wb. The region in the plasma which contains stray fields of less than about 10 G is outlined in Fig. 4. This geometry, therefore, satisfies requirements 1 and 2 above. Requirement 3 is satisfied completely but there is a region in the TF coil in which the fields approach 0.5 T. This region is located adjacent to the top edge of the solenoid coils and is outlined in Fig. 4 as containing more than 10 kG, but this was an earlier result.

In order to improve this last condition for this geometry, one could decrease the flux density in the solenoid coils giving a reduced core flux, or one could increase the distance between the TF coils and the solenoid coils. There are several ways of doing the latter, but each of the alternatives has some negative aspects. To increase the distance between the TF coils and the solenoid coils, the solenoid coils can be reduced in diameter; and to compensate for the loss of solenoid area, the core flux densities can be increased, which could be a problem. The major radius of the plasma could also be increased, which is a disadvantage, but it will allow for the improvement of the net shielding for the TF coils. Some combination of these different solutions could be utilized in some optimum design. It is not clear, however, at this point that this problem of high stray fields in the TF coils is necessarily a serious one. The area involved is rather small and is at a vertical position which would allow the introduction of enlarged coolant paths in the radial dimensions of the TF coils.

There are some other possible alternatives which will give an improved design. One is shown in Fig. 4 in a phantom outline. This is the addition of a core solenoid like that used for the reference case

in Fig. 1. This will add at least 11.4 Wb of core flux. With this coil and using the solution above, which calls for the reduction of the solenoid fields in case 3, a total core flux of at least 27.5 Wb could be produced.

Another possibility is to add the core solenoid to case 3 giving a net core flux of at least 36.1 Wb. This core solenoid can similarly be added to case 2, resulting in case 2'. This will give a core flux of at least 29.9 Wb. These cases were not calculated in detail to establish the stray fields in the plasma and TF coil regions. It is not expected, however, to add any significant fields in these regions since these coils can produce, by themselves, better results than the geometries to which they are added.

Another alternative geometry is to bend the solenoid coils away from the TF coils in a radial plane and only in the regions near the ends of the solenoids. This argument can be carried still further, by extending the ends of these bent solenoids around the plasma region to some extent. Calculations for this case, case 4 in Fig. 2, have not been performed, but it can be expected that this will not significantly alter the results for the core flux that were found in case 3. We do expect, however, that the high fields in the TF coil region can be reduced still further to acceptable values. An added advantage is also anticipated. This is the reduction and possible elimination of the cylindrical profile coils contained inside the TF coil envelope.

The final case that we have considered, case 4', involves the addition of a core solenoid coil to case 4. This will then give a net core flux of at least 36.1 Wb and will comfortably meet all of the requirements stated above.

Results and Conclusions

The main features of our study are shown in Table I. The V-s listed are calculated assuming a complete reversal of the field in the OH coils. Case 2 meets all the requirements except for 1, and the major radius is not greatly reduced from the current EPR design of 4.7 m. However, if the coils were made of copper, they could be put inside the shielding and used to produce a large amount of fast OH V-s. This would allow a reduction of the major radius to 4.2 m as in the other cases, and greatly reduce the fabrication and repair problems associated with having coils inside the TF coils. In addition, an OH solenoid must be inserted in the central core for production of slow OH V-s. As would be expected, case 3' and 4' produce the most V-s because of the addition of the OH solenoid in the central core. Case 4' is the most attractive from the point of view of fabrication and repair since all of the coils are removable through the spaces between the TF coils. In addition, these coils could be made of copper to produce a large amount of fast OH V-s while the central core solenoid could be used to produce slow OH V-s. Also, the copper coils can be used for active shielding of the TF coils from the induced fields due to the plasma. Time has not, however, permitted us to study this possibility.

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TABLE I
Summary of Alternate OH Heating Features

Case Number	Major Radius	Minor Radius	Magnetic Field at Major Radius with 9 T TF Field	OH with Field Reversal	Area in Plasma with Field $\leq 10G$	Maximum Field in TF coil
(m)	(m)	(m)	(T)	(V-s)	(m ²)	(T)
1	4.2	1.4	3.5	22.8	7.0**	.05
2	4.62	1.32	3.2	37.0	5.5	.5
3	4.2	1.4	3.5	48.4	4.0	.42
4	4.2	1.4	3.5	48.4*	4*	<.4*
2'	4.62	1.32	3.2	59.8	5.5	.5*
3'	4.2	1.4	3.5	72.2	4*	.4
4'	4.2	1.4	3.5	72.2	4*	<.4*

*Values are estimated

**Approximately the entire plasma area

Fig. 1. Reference OH coil geometry case 1, with definitions of machine parts.

Fig. 2. Alternate OH coil geometries

Fig. 3. Details of the geometries and results for case 2 and case 2' (case 2' includes the core solenoid shown in phantom).

Fig. 4. Details of the geometries and results for case 3 and Case 3' (Case 3' includes the core solenoid drawn in phantom). The area in the TF coil shown to contain ≈ 10 kG fields actually contain fields ≤ 4 kG in the latest results.

PLASMA DRIVING SYSTEM REQUIREMENTS FOR COMMERCIAL TOKAMAK FUSION REACTORS*

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Introduction

The plasma driving system for a tokamak reactor is composed of an ohmic heating (OH) coil, equilibrium field (EF) coil, and their respective power supplies. Conceptual designs of an Experimental Power Reactor¹⁻³ (EPR) and scoping studies of a Demonstration Power Reactor⁴ have shown that the driving system constitutes a significant part of the overall reactor cost. The capabilities of the driving system also set or help set important parameters of the burn cycle, such as the startup time, and the net power output. Previous detailed studies^{1-3,5-6} on driving system dynamics have helped to define the required characteristics for fast-pulsed superconducting magnets, homopolar generators, and very high power (GVA) power supplies for an EPR.

The driving system requirements for commercial reactors have not been examined in detail to date. Plasma driving systems requirements are needed to guide studies of commercial reactor systems, to define research and development needs and to provide some perspective as to how well design options for near-term tokamaks extrapolate to commercial reactors. The plasma driving system requirements depend upon the reactor-design concept, of course. This paper summarizes our results⁷ for a single reactor configuration together with several design concepts for the driving system. Both the reactor configuration and the driving system concepts are natural extensions from the EPR. Thus, the new results presented in this paper can be compared with the previous EPR results¹⁻³ to obtain a consistent picture of how the driving system requirements will evolve — for one particular design configuration.

Reactor Model

The reactor model used for this study is an 8-m major radius tokamak having a plasma B_t of about 8k, and a power output of 1000 MWe. The plasma is D-shaped with a height-to-width ratio of 1.3 and has equilibrium parameters $n_{pT} = 1.44 \times 10^{20} \text{ m}^{-3}$, $I_p = 12.3 \text{ MA}$, $q(a) = 3$, $B_p = 1.85$, $T_{pT} = 8 \text{ keV}$, and $n = 5$ spitzer. The model and design parameters are based, in part, on data developed by the parametric systems analysis project at ANL.⁸ The reactor coil configuration, shown in Fig. 1, is conventional, with a central, solenoidal OH coil with a few additional external trimming coils, and a set of EF coils external to the toroidal field coils. Both the OH and EF coils are superconducting and decoupled from one another. The locations of the EF coils and the relative currents in them have been selected so as to obtain the magnetic field in the plasma required to keep the plasma in MHD equilibrium. The peak field of the OH coils is 8 T. This and the OH coil radius of 3.3 m determine the available flux swing of the OH coil.

Power Supply Configurations

Four different driving system power supply configurations have been examined as to their suitability for this reactor. Each configuration contains an energy

transfer device for use with the OH coil during startup and a central energy storage device used to provide large power demands when necessary. Three types of possible OH transfer devices have been considered: (1) a homopolar generator; (2) an SCR-type rectifier-inverter power supply (SCR P.S.); and (3) a dump resistor. Two types of central energy storage devices have also been considered: (1) a superconducting energy storage inductor (ESI); and (2) a motor-generator-flywheel (MGF) set. These transfer and central storage devices have been combined into four configurations, the variable elements of which are listed in Table 1.

TABLE 1. Hardware Configurations

Hardware Configuration No.	OH Transfer Device	Central Storage Storage Device
1	Homopolar generator	Energy storage inductor
2	SCR-type power supply	Energy storage inductor
3	SCR-type power supply	Motor-generator flywheel
4a	Single dump resistor	Energy storage inductor
4b	Multi-dump resistor	Energy storage inductor

The equivalent circuit of Configurations 1 and 3 are shown in Figs. 2 and 3. All configurations use several common elements. The EF power supply is an SCR P.S. in all cases because the EF current cannot be free-running; it must be precisely controlled to keep the plasma in MHD equilibrium. (The MHD requirements depend on the plasma current and plasma pressure.) The requirements on the EF supply and the cost of it do, however, depend greatly on the type of OH transfer device used and upon details of the startup. The exact design of the EF supply also depends on whether it will operate out of an ESI or an MGF. All four configurations also use an auxiliary SCR P.S. in the OH circuit. This auxiliary supply is used to charge the OH coil prior to startup, and to make up for plasma resistance losses (supply volt-seconds) during the burn. Each configuration also involves a neutral beam power supply and a power supply that interfaces with the grid, power plant, and auxiliary subsystems. These have not been included in the cost estimates. The interfacing power supply is also used to recharge the central energy storage device. All of the configurations also incorporate a shorting switch (not shown) across the OH coil. This switch is opened just after the OH transfer device is connected, and is closed just before the transfer device is disconnected.

Computational Model

The driving system requirements for this reactor have been analyzed by means of a spatial-profile-averaged, time-dependent, plasma code coupled to a driving system code. The plasma code solves particle balance equations for each constituent plasma ion species (D-T, alpha, wall-sputtered impurity) and solves energy balance equations for the ions and the electrons.

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Plasma heating by alpha and neutral beam slowing down is treated. Radiation (bremsstrahlung, line, recombination) and transport losses are treated, the latter with a multi-regime (neoclassical ions/empirical electrons at large collision frequencies and trapped-particle mode at small collision frequencies) confinement model. The overall model is described in Appendix C of Ref. 2. The inductance matrix for the poloidal coil system was computed on the basis of the coil locations shown in Fig. 1. The inductance values are $L_p = 13.75 \mu\text{H}$, $L_{OH} = 3.07 N_{OH}^2 \mu\text{H}$, and $L_{EF} = 3.09 N_{EF}^2 \mu\text{H}$, where N_{OH} and N_{EF} are the number of turns in the OH and EF coils respectively. The coefficients of coupling for the system are $K_{OH,p} = 0.47$, $K_{EF,p} = 0.27$, and $K_{EF,OH} = -0.01$.

The focus of the analysis was on the startup phase of the burn cycle during which most of the driving system requirements are set. The nature of the startup is dependent on the OH transfer device. For each transfer device burn cycle simulations were performed for a range of ohmic heating ramp times, Δt_{OH} , and neutral beam turns on times, t_{B_0} . In general, values of $t_{B_0} = \Delta t_{OH}$ gave the best results and these are the cases discussed here.

Startup Using Homopolar Generator

An example of the startup, using a homopolar generator, is shown in Fig. 4 for the typical case of $\Delta t_{OH} = t_{B_0} = 2 \text{ s}$. (V_{OH} and V_{EF} are the coil voltages, I_p is the plasma current, etc.) The homopolar generator is connected to the previously charged OH coil at the start of the cycle, $t = 0$ and disconnected at $t = 2 \text{ s}$. During this period the OH current increase serves to induce current in the plasma. Neutral beam heating is initiated at $t = 2 \text{ s}$ and terminated at about $t = 8 \text{ s}$ when the plasma reaches an ignited equilibrium. The EF current begins at zero at the start of the cycle, and rises in accordance with the needs of the MHD equilibrium, as determined by the plasma pressure and current. The requirements on the driving system are computed from these waveforms where, for example, the maximum value of V_{OH} determines the maximum field change at the OH coil, B_{OH} , and the homopolar generator storage capacity, U_{OH} , the amount of ohmic heating flux swing used for startup determines the maximum burn time, etc. The driving system requirements, for this case, are: $U_{OH} = 937 \text{ MJ}$, $B_{OH} = 2.9 \text{ T/s}$, the maximum EF power requirement $P_{EF} = 2.1 \text{ GVA}$, the ESI storage requirement $\approx 9 \text{ GJ}$. Based on the OH flux swing used for startup, $\Delta \Phi_{OH} = 121 \text{ V-s}$, a maximum burn time of 28 min would be possible.

Figure 5 shows the sensitivity of some of the requirements for Configuration 1 for a range of Δt_{OH} . Short reversal times reduce the flux swing $\Delta \Phi_{OH}$ because the plasma is cool for a shorter time, thus $\Delta \Phi_{OH}$ and hence $U_{OH} \sim I_{OH}^2$ are correspondingly low. B_{OH} and P_{EF} , however, scale as Δt_{OH}^{-1} and so increase with short Δt_{OH} . Long reversal times increase $\Delta \Phi_{OH}$ and U_{OH} . The resulting increase in $\Delta \Phi_{OH}$ tends to make $B \sim \Delta \Phi_{OH} / \Delta t_{OH}$ become flat. For $\Delta t_{OH} \geq 3$, the maximum value of V_{EF} occurs after the OH reversal, i.e. during beam heating, and so increasing Δt_{OH} has no further effect on P_{EF} . Reversal times greater than 4 s produce unstable results in the sense that small changes in the initial OH current produce large changes in the plasma current. This is due to the large resistive losses encountered for long Δt_{OH} , which makes for a highly nonlinear interaction between plasma resistance, plasma current, and plasma temperature. Basically, very short reversal times impose power and B problems, while long reversal times impose volt-second problems.

Startup Using Dump Resistor

The motivation of using a resistor as the OH transfer device is to enable use of the full potential OH

flux swing by starting the OH current at its maximum (negative) value, i.e. corresponding to a field of $B_{OH} = -8 \text{ T}$. This could potentially be done with a homopolar generator or a rectifier power supply, but then the cost of the transfer devices would be prohibitive, compared to the resistor. When the resistor is connected to the OH coil it forms an equivalent R-L circuit; the resistor dissipates magnetic energy as heat, and the current decays. Since only one polarity of the OH current can be used, the use of a dump resistor, by itself, is restricted to situations where enough flux is available in one direction to support startup.

Figure 6 shows a typical startup using a single dump resistor. This case has a resistor value adjusted to give $|V_{OH}|_{\text{max}} = 115 \text{ V/turn}$ at $t = 0$. The OH current is started at -90 MA-turns , corresponding to $B_{OH} = -5 \text{ T}$. Both V_{OH} and I_{OH} show, to first order, the exponential decay characteristic of a conventional L-R circuit. By $t = 1.5 \text{ s} = \Delta t_{OH}$, the proper amount of volt-seconds has been delivered. At this time, therefore, the resistor is removed from the circuit by closing a shorting switch, thereby reducing V_{OH} to 0 and terminating the OH current decay. The resistor is then switched out and the auxiliary power supply switched in. The shorting switch is then opened completing the process. The shorting switch must handle a maximum current of 90 MA-turns and must isolate against a maximum voltage of 115 V/turn for a maximum reactive power capability of 10.3 GVA. The other features of the startup are similar to those for the other configurations except that V_{EF} peaks at $t = 0$, when V_{OH} is maximum. The maximum value of V_{EF} is also more than for the other configurations. Another difference is that energy is dissipated in the resistor, a total of $\Delta U_{OH} = 8071 \text{ MJ}$ for this case, instead of being returned to the OH coil. This is probably not significant, at least for long burn pulses, since it represents only 8 s worth of output from the reactor.

The advantage of the dump resistor is evident from Fig. 6; at the end of the startup I_{OH} is much lower than for the other transfer devices. The OH coil can therefore swing through much more flux, nearly double that for the other devices. This gives a maximum burn time capability of 57 min, for this case. Thus, the major advantage of using a dump resistor is to achieve a much longer burn time for a given B_{OH}^{max} .

The sensitivity of some of the driving system requirements to the parameter V_{OH}^{max} , for Configuration 4a, is shown in Fig. 7. In this figure the lowest value of $|V_{OH}|_{\text{max}}$ shown is the lowest that works; lower values result in complete resistive current decay in the plasma.

SCR Power Supply

A similar study was performed for the configurations using an SCR power supply as the OH transfer element. In general, the EF supply and the ESI have similar requirements to the homopolar generator cases. B_{OH} is somewhat lower if a flat V_{OH} waveform is used. The requirement on the OH-SCR supply itself is about 2.0 GVA. The OH volt-second consumption during startup, and hence the maximum burn time, is similar to the homopolar generator cases.

Power Supply Costs

Figure 8 summarizes the total driving system power supply cost as a function of the OH ramp time for the different configurations. This data has been generated by applying cost algorithms developed for the various hardware components to the technological requirements found by the burn cycle analysis. This cost covers the OH transfer device, the OH auxiliary supply, the EF supply, the central energy storage device, and all switch costs. (While the heating system costs are not included

the energy storage requirement on the central storage device due to the neutral beam is included. Figure 8 is for the case of the neutral beam turned on just after the OH ramp, generally the best operating mode. The cost of each configuration is extremely sensitive to the ramp time; this illustrates the need for a very precise and coupled analysis of the plasma and the driving system. In general, the cheapest operating point is the most cost effective.

Configuration 1 results in the smallest power supply cost: 105 \$M for a Δt_{OH} of 3 s.

In Configuration 2, at the cheapest operating point, $\Delta t_{OH} = 2.3$ s, the cost is about 30 \$M more than for Configuration 1. This is basically because the SCR supply costs more than the homopolar generator. The requirements for this case are $P_{OH} = 2.1$ GVA, $P_{EF} = 1.6$ GVA, $B_{OH} = 2.0$ T/s, and ESI storage = 8.4 GJ.

Configuration 3 is the most conventional in terms of available technology. MGF sets are presently used. The rectifier supply for the MGF system is a conventional ac-dc multiphase converter, rather than a dc-ac-dc inductor-converter of the type needed for use with the ESI. Although the requirements for this configuration, exclusive of the MGF set, are the same as for Configuration 2, the cost is much higher and nearly double that of the homopolar generator-ESI combination at the cheapest operating point. This large additional cost is due almost entirely to the cost of the generator portion of the MGF set. Furthermore, the cost estimates for this configuration assume a voltage compatible power supply-MGF-set combination, i.e. with no transformers used. If transformers were needed the cost of Configuration No. 3 would be even greater.

In Configuration 4a the use of a dump resistor, which has an essentially trivial cost, eliminates the need for a symmetric OH current swing during startup (needed for the other transfer devices), and so permits the full design value of the OH flux swing to be used. The potential burn time is, therefore, doubled, relative to the other transfer devices. The use of a dump resistor, however, requires a voltage isolating-shorting switch, in the OH circuit. If this switch must be solid state, as has been assumed, it will be expensive. For example, for the example described earlier of $|V_{OH}|_{max} = 115$ V/turn, the switch cost is estimated to be 82 \$M whereas the cost of the resistor, at 0.6 \$M is negligible in comparison. Also, the range of ramp times, for a single dump resistor, is limited to fairly short times, and this increases the EF requirements. For these reasons, Configuration 4a is comparatively expensive, about 185 \$M for the cheapest case. This may or may not be worth the doubled burn time. Requirements for this case are $B_{OH} = 5.4$ T/s, $P_{OH}^{max} = 2.6$ GVA. If four resistors are used, to obtain a variable resistance during startup, as in Configuration 4b, the maximum OH voltage during startup can be lowered and the ramp time increased. This saves on EF supply cost relative to Configuration 4a but involves higher switch costs. The cheapest case for Configuration 4b costs about the same as Configuration 4a. The burn time is the same, $B_{OH} = 2.0$ T/s and $P_{OH}^{max} = 1.6$ GVA are lower, while circuit complexity and control would be greater.

Conclusion

In general, feasible startup times for this typical commercial reactor are about the same as for an EPR; OH reversal times of 1-4 s, followed by a beam heating period of about 5 s. The B requirements on the OH coil are considerably less than for an EPR, basically due to the larger radius of the OH coil. The OH power and energy requirements are similar to an EPR but the EF requirements increase considerably. In general, there is a strong degree of coupling between the driving sys-

tem requirements and the details of the plasma physics, particularly the temperature behavior and the MW field requirements of the plasma.

Naturally, the results described here are subject to uncertainties in the plasma physics and in the cost algorithms. However, the relative cost data between the different power supply technologies seems clear. Conventional technology, in the form of an MGF set and conventional SCR supply can be used for the driving system but the cost will be high. Development of superconducting energy storage systems and homopolar generators appear to offer very substantial savings; in the neighborhood of 100 \$M per reactor.

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Fig. 1. Schematic view of OH and EF coil locations.

Fig. 3. Power supply Configuration 3.

Fig. 2. Power supply Configuration 1.

Fig. 4. Typical startup using a homopolar generator as the OH transfer element. $\Delta t_{OH} = 2$ s, $t_{B_0} = 2$ s.

Fig. 5. Sensitivity of the driving system requirements to the ohmic heating ramp time, Δt_{OH} . For a homopolar generator used as the OH energy transfer device.

Fig. 6. Typical startup using a dump resistor as the OH transfer element; $V_{OH}^{max} = 115$ V/turn, $t_{B0} = \Delta t_{OH}$.

Fig. 7. Sensitivity of the driving system requirements to the maximum OH voltage. For a single dump resistor used as the OH transfer element.

Fig. 8. Total driving system power supply cost as a function of the OH ramp time, Δt_{OH} and power supply configuration. For the neutral beam turn-on time, $t_{B0} = \Delta t_{OH}$.

ENERGY STORAGE AND TRANSFER SYSTEM FOR EXPERIMENTAL POWER REACTOR*

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Abstract

The conceptual design for an integrated Experimental Power Reactor (EPR) energy storage and transfer system is described. The system includes homopolar generators for ohmic heating (OH) coil field reversal and inductor-converter bridge circuits to transfer energy from a central superconducting energy storage inductor to the equilibrium field (EF) coil, the OH coil during burn, and the neutral beam power supplies.

I. Introduction

The reference design for the Argonne National Laboratory (ANL) EPR¹ has superconducting OH and EF coils, with the EF coil located outside the toroidal field (TF) coils. The energy storage and transfer (EST) system consists of a central energy storage inductor (ESI), thyristor rectifier systems to transfer energy between the ESI and the OH coil, EF coil, and neutral beam and/or RF heating systems, a homopolar device to reverse the OH field at the start of the burn phase, and an ac-dc converter to resupply system losses over the cycle.

The energy storage and transfer system is depicted in Fig. 1. OH and EF coil parameters relevant to the design of the EST system are listed in Table I.

II. OH Coil EST System

It is planned to reverse the field in the OH coil in a period of 2 sec using a homopolar type resonant capacitor. Figure 2 illustrates the system concept. The capacitor represents the homopolar generator used to reverse the field at the start of the burn cycle. A capacitance of 1.312 F provides field reversal in 2.0 sec with a peak generated field of 24.1 kV.

The conceptual design for the homopolar generator is an air-supported multiple drum-type or counter-cyclonic generator (CCG) configuration.² The usable field region allows drums of length 0.55 m to be radially stacked in a radial region from 0.55 m to 0.35 m. Type 17-4 stainless steel is the material envisioned for the conducting cylinders. The density of type 17-4 stainless steel is 7800 kg/m³. The thickness of each drum is adjusted so that the surface velocity of each cylinder reaches 236 m/sec for 56 000 A of OH current.

A single CCG module will develop 3750 V with six shells radially stacked and series connected. The radial thickness of the six drums varies from 2.4 to 2.1 cm from the innermost to the outermost drum. Each CCG module can store 66.3 MJ and represents a minimum effective capacitance of 9.5 F. Eight CCG modules in series will develop 30 000 V at a minimum capacitance of 1.188 F. The minimum field reversal time in the absence of stray inductances and circuit losses would be 1.79 sec.

The CCG is disconnected during the burn. The OH coil is maintained with a separate power supply driven from a central energy storage inductor described in a later section. The power supply is

an actively controlled inductor-converter bridge³ of the type described in the next section. A total of 350 MJ is taken from the ESI during the burn. The highest average voltage required for the OH coil during the burn is 700 V.

III. EF Coil Energy Storage and Transfer System

The peak EF coil voltage that is required in the ANL EPR startup scenario is 9.4 kV at 1.25 sec into the startup phase. An actively controlled version of an inductor-converter bridge³ driven from the ESI is envisioned for this function.

A circuit diagram for an inductor-converter bridge is shown in Fig. 3. The EF coil is represented by L_E , and the ESI is represented by L_S . The two bridges shown in Fig. 3 commute the respective coil currents through the capacitors to create three-phase voltages. The average rate and direction of energy transfer is dependent on the relative phase between the switching steps in the two bridges.

Figure 4 gives the normalized average voltage across the EF coil for different values of relative phase angle between switching steps on the EF side and the storage side of the bridge and ratio of the EF coil current to storage coil current. The inductor-converter bridge is designed to generate an average value of voltage on the EF coil of 13 500 V across L_E . Since only 9400 V is needed at 1.25 sec, the bridge would be operated at about 46° of phase advance of the EF side of the bridge to the storage side. Advancing the phase to 90° would allow an additional 3100 V to increase the di/dt on the EF coil if needed. During the rest of the startup period, the di/dt demand is less, and the bridge would be operated with smaller phase advance, 20° or less.

IV. Neutral Beam Power Supply

The design of the neutral beam power supplies is based on the concept suggested by Praeg.⁴ A magnetic core device is used to suppress current surges in the event of sparking, and a high frequency, thyristor-type rectifier is used to energize the beam injector with the capability of deenergizing the device in about 100 μ sec. A circuit diagram is shown in Fig. 5.

Some form of inductor-converter is used to extract energy for the ESI to the high frequency rectifiers. The neutral beam injectors require 119 MW from the ESI for 6.5 sec during startup. The total energy withdrawn from the ESI is 770 MJ.

V. Central Superconducting Energy Storage Inductor

As shown in Fig. 1, with the exception of the OH coil field reversal, all energy is extracted from the ESI for changing the OH and EF coils and the auxiliary heating systems. The heavy instantaneous demands in power for these systems are buffered from the utility grid, with only the average losses appearing as a demand on the grid.

The ESI design is essentially modeled after that of F. E. Mills et al. The peak design parameters are 80 000 A current, 5 T magnetic field, 10⁷ A/m² current density, maximum stored energy is 2525 MJ, and

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extractable energy of 1900 MJ. The ESI is a torus with a minor radius of 0.8 m, a major radius of 4.9 m, and an inductance of 0.32 H.

A rectifier power supply removes an average of 13.3 MW from the utility grid to resupply losses in the energy storage and transfer system.

VI. Discussion

The energy storage and transfer system depicted in this paper is an integrated design based on non-conventional technology. A summary of the system parameters is listed in Table 2. There is reason to believe that a considerable cost savings will result from application of this technology, although until these devices are developed to a commercial level, or nearly so, full confidence in this belief cannot be extended. Alternately, if superconducting magnet systems and/or homopolar generator systems do not realize the expected potential in economy and efficiency, a design for EPR can be based on the use of conventional motor-generator-flywheel sets with ac-dc converters at considerably higher cost and reduced efficiency of operation.

Table 1. OH and EF Coil Parameters for the ANL EPR

OH Coil Parameters

Inductance	0.274 H
Peak Current at Start of Burn	56 kA
Peak Current at End of Burn	75 kA
Stored Energy at Start of Burn	430 MJ
Peak Stored Energy	780 MJ

EF Coil Parameters

Inductance	0.229 H
Peak Current	80 kA
Peak Stored Energy	732 MJ

Table 2. Energy Storage and Transfer Systems - Maximum Ratings

Omic Heating Systems

Homopolar Generators	
No. of generators in series	8
No. of drums per generator	5
Total energy transfer (MJ)	534
Peak power (MW)	840
Peak voltage (kV)	30
Peak current (kA)	56
Equivalent capacitance (F)	1.188

Rectifier System

Type	3 ϕ Inductor Converter Bridge
Energy transfer (MJ)	350
Peak power (MW)	56
Peak current (kA)	80
Peak voltage (kV)	0.7
Switching frequency (Hz)	100

Equilibrium Field System

Type	3 ϕ Inductor-Converter Bridge
Energy transfer (MJ)	732
Peak power (MW)	225
Peak current (kA)	80
Peak voltage (kV)	13.5
Switching frequency (Hz)	100

Neutral Beam System

Type	SCR, DC/AC/DC @ 10 kHz
Energy transfer (MJ)	770
Voltage (kV)	180
Power (MW)	120

Central Energy Storage Inductor

Type	Superconductive Ring Dipole Inductor
Energy stored (MJ)	2525
Energy transfer (MJ)	1900
Peak current (kA)	80
Peak power (MW)	350
Average power from utility grid or power plant (MW)	13.3

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Figure 1. Energy Storage and Transfer System

Figure 2. Homopolar Generator Circuit

Figure 3. Inductor-Converter Bridge Circuit Diagram

Figure 4. Average coil voltage for different values of the current ratios of field coil to storage coil.

ARGONNE NATIONAL LABORATORY ENERGY STORAGE AND TRANSFER EXPERIMENTAL PROGRAM*

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Introduction

Magnetic fusion reactor, equilibrium field, and ohmic heating (OH) coils require the coil energy to be cycled in relatively short periods of time. For large fusion reactor systems, the energy can be in the thousands of MJ range. These large amounts of energy cannot be removed from or returned to the power grid without having an adverse effect on the grid.

Several schemes have been proposed which can minimize the amount of energy required from the power grid over a fusion-reactor cycle. They include the flying capacitor,¹ the inductor-converter bridge,^{2,3} the homopolar generator,⁴ and the motor-generator (flywheel) (MGF). The MGF is best understood and has been in use for this purpose for many years. It requires the least amount of development. The other schemes have not been applied to the energy buffering problem and require considerable development. Of the three remaining schemes, the homopolar generator and the inductor-converter bridge seem to be the most desirable.

Each system has certain advantages and disadvantages. The homopolar generator is best suited for application where the system element can be allowed to operate in a passive sense that is operating at their natural frequency. An example would be the cycling of the OH coil current. The inductor-converter bridge is a bit more complicated in its operation, but it has the advantage of being controllable, that is, it allows the transfer of energy at a controlled and variable rate.

Inductor-Converter Bridge

Two superconducting solenoids, each storing 45 kJ at 150 A and capable of being charged at 150 V, along with an inductor-converter bridge, were constructed. This circuit models the mechanism whereby energy can be extracted from one inductor and transferred, at a controlled rate, to the other inductor. A three-phase bridge circuit is shown in Fig. 1, and a block diagram of the digital controller providing the proper thyristor firing sequence is shown in Fig. 2. By controlling the phase delay between the switching sequences of the two sides of the bridge, net average voltage across the capacitor network can charge and discharge the inductors at controlled rates.

The energy the capacitors are allowed to accumulate is limited to a small fraction of the total energy in the system. An important feature of the bridge circuit is that if the energy stored in the capacitors is not removed by the load coil it goes back into the storage coil. Experiments were performed on this system and pictures of the voltage across the load coils at various times are shown in Fig. 3. The results obtained from these experiments agree nicely with the predicted theory.

An analysis of the bridge circuit is simplified when the theory of superposition is applied. Each inductor acting alone will, when provided with a proper switching sequence to the thyristors, generate a trapezoidal voltage across the capacitors. The summation of these two voltage waveforms will produce the correct voltage waveform appearing across the capacitors at any instant in time. From these waveforms, the voltage across the individual inductors can be obtained. The resulting capacitor waveforms in general will not be trapezoidal, but will depend on the relative magnitude of the currents in each coil and the phase displacement between the waveforms. Analysis shows that a phase displacement of from 0 to 90° will produce energy transfer in one direction, and 0 to -90° will produce energy transfer in the opposite direction.

At zero degrees, no net energy transfer takes place, and as the phase angles are increased, net energy transfer increases and reaches a maximum when the phase angles are at $\pm 90^\circ$. Computer analysis of the circuit has produced graphs which relate the voltage across the inductors as a function of coil current ratios and phase angles. These graphs are shown in Fig. 4.

If the energy transfer rate is to be variable during a transfer cycle, computer analysis shows that large capacitor voltage unbalances will be created, depending on the magnitude of the phase change. To minimize and eliminate any unbalance occurring, the phase change should be made to the bridge side containing the least current. Also, the phase change should be applied in at least two equal steps at 180° intervals. Figure 5 shows the unbalances created by changes in relative phase and the elimination of the unbalances by making the change at the proper time.

A microprocessor has been obtained to function as the controller for the inductor-converter bridge. The microprocessor will examine the coil currents and, with information about the desired coil voltage, determine the proper operating phase angle. It will then make any necessary phase angle adjustments to the proper bridge thyristors at the proper interval. It will also function as a watch dog over the entire system and take appropriate action upon a system failure.

Homopolar Generator

A 40 kJ, 2500 A model of an air-supported aluminum drum-type homopolar generator has been constructed. The switching circuits for the system are installed and a schematic of the entire system is shown in Fig. 6. A photograph of the generator system is shown in Fig. 7.

A conventional magnet has been modified with special pole tips to provide a radial magnetic field through the surface of a central iron column. An aluminum (6061-T6) cylinder and air-bearing system is

mounted on the iron column. The dimensions of the drum are scaled from the full-sized machine required by the TEPR OH coils. With a magnetic field of 1.5 T, the drum will have a capacitance of 21.3 F; and with a peak excitation of 2340 radians per second angular velocity, the drum will develop 61 V. A load inductor of 13.6 mH storing 40 kJ at 2425 A can be switched to the terminals of the generator which will reverse this load current in 1.7 s.

This model system will permit the study of the mechanical feasibility of a cylindrical rotor energy storage device, equipped with an air-bearing support structure in the presence of considerable radial expansion of the rotating cylinder. The air-bearing system is simpler than that required by a horizontal axis machine since the weight of the drum acts only axially and need not be supported radially. Conventional copper-carbon brushes bear onto the copper-plated upper and lower edges of the aluminum cylinder. The brushes are loaded against the cylinder with an air jet system for uniform electrical contact.

Referring to Fig. 6, in the sequence of operation, with the power supply set to deliver a certain current, S1 is turned on causing the 13.6 mH inductor to charge. Turning on S2 disconnects the power supply from the circuit and turning on S3 allows the energy to be transferred to the generator. S2 and S3 must be turned on simultaneously. When all the energy is in the generator and S4 is switched on, the energy returns to the magnet. Turning S3 and S4 on alternately causes the energy to cycle between the magnet and the homopolar generator.

The first tests performed on the homopolar generator were designed to measure the brush voltage drop. A 10 V, 100 A power supply was connected across the cylindrical drum and brush support structure. The radial field was set at approximately 0.93 T. The air-bearing pressure was set at 10 psi and the brush pressure had to be set to 30 psi before we were fairly confident that the brushes were making electrical contact. With the high brush pressure, we were unable to rotate the drum by hand. The power supply was turned on and after several seconds the drum reached an angular velocity of 3500 rpm the current through the drum was 70 A. The voltage drop across the brushes was calculated at 2.3 to 2.4 V. Because of the high brush pressure and large voltage drops, the brush support structure is to be modified, but before disassembly energy transfer was attempted. The 13.6 mH inductor was energized to 500 A and the current was switched through the homopolar generator. These tests were performed several times and pictures taken of the magnet current and drum voltage are shown in Fig. 8.

The current waveform of Fig. 8 is of the shape expected, however, the voltage waveform for the homopolar has some discontinuities which are not easily explained. The trailing edge of the drum voltage waveform shows that energy was stored in the homopolar and that large losses are present. The system was dismantled and modification to the brush support structures are under way.

Following the testing and evaluation of the demonstration generator, a conceptual design will be made for a 100 MJ homopolar system. This machine will interface to the 100 MJ pulsed coil developed in the pulsed coil program. This prototype system will achieve tokamak level operation in all important aspects and serve as an engineering test facility for the TEPR OH coil power supply design effort.

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TOKAMAK DIVERTOR IMPACT ON THE TOROIDAL FIELD MAGNET AND
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Neutronic analysis for the Wisconsin Tokamak Engineering Test Reactor (TETR) blanket/shield design has been carried out in detail for the different sections of the reactor (test section, outer blanket, inner blanket, neutral beam injectors and the divertor). Special attention has been given to the divertor shield design since the divertor is one of the most difficult components of the reactor to design. The divertor slots represent a source of neutron leakage which requires careful analysis of the actual geometry and shield to minimize the effects on the reactor. The toroidal-field (TF) magnet is the most sensitive component that suffers from the divertor concept. The radiation leakage from the divertor increases the copper resistivity, the nuclear heating and causes degradation in the insulator properties. The other effects caused by the radiation leakage are the nuclear heating in the cryosorption panel and the increase in the radiation dose outside the reactor shield. Multidimensional neutronics calculations are used for the analysis of these effects with iteration on the divertor geometry to satisfy the following design criteria: (1) the maximum degradation in the insulator properties is 30% at 15 MW-yr/m^2 integrated neutron wall loading, (2) the maximum change in the copper resistivity is 50% at any point in a TF magnet at a 3.5 MW-yr/m^2 integrated neutron wall loading (3) the heat deposition in the TF magnet is $< 60 \text{ KW(th)}$ and (4) the maximum nuclear heat load for the cryosorption panels is $< 0.0001 \text{ W/cm}^3$ to minimize the refrigeration power. It is found that the maximum damage in a TF magnet is located near the divertor area. The ratio of the maximum change in the copper resistivity to the change in the resistivity at the midplane of the reactor (where the design is usually carried out) is about 9. The heat deposition in the cryosorption panel is reduced by two orders of magnitude by using a special shield for the vacuum system which reduces the initial and the operational costs. The refrigeration power and the dose to the insulator satisfy the design requirements after an iteration on the divertor geometry and its shield.

INTRODUCTION

The fusion community has proposed various Tokamak reactor designs which can be classified into two different categories, the experimental devices and the power reactors. At first glance, one can characterize the power reactors by a long burn cycle and a divertor concept (although some of the experimental devices utilize the divertor concept). The Tokamak fusion reactors employ the divertor concept to reduce the erosion of the first wall due to energetic charged particle bombardment and to protect the plasma from high Z impurities as is essential to achieve a long burn cycle. Various types of divertors have been proposed¹, single null, double null, and bundle. The different types require a large opening (divertor slots) in the blanket/shield for particle collection. The TETR divertor design has been carried out in detail to illustrate and minimize the divertor impact on the reactor. In particular the effects on the toroidal field (TF) magnets and the vacuum system since these two subsystems suffer greatly from the divertor concept.

The TETR design parameters required for this paper are given in Table 1 and a cross sectional view of the TETR reactor is shown in Figure 1. The TETR has been designed to be a mid term experimental device² to provide engineering data for the fusion power program. This objective lead us to consider the existing state of the art without extrapolation for the design. The reactor chamber of TETR is designed for 1 MW/m^2 average neutron wall loading to get the required data in a reasonable timeframe for the fusion program. The TF magnets are "D" shaped superconducting coils. The conductor consists of NbTi filaments with OFHC copper stabilizer using stainless steel as the structural material. Cryosorption panels are used in the vacuum system.

DESIGN CONSTRAINTS

The design philosophy for TETR requires the achievement of an integrated neutron wall loading of 15 MW/m^2 within 15 years of operation ($1.4 \text{ MW/m}^2/\text{yr}$ neutron wall loading with a plant factor of 0.7). The requirement of operation for 15 years with 70% plant factor imposes many design constraints which are used in this work.

The TF magnets are designed to operate without replacement for 15 years, but under the expected radiation environment the material properties will be degraded. The TF magnets consist of superconducting material (NbTi) embedded in copper (the stabilizer) using stainless steel as the structural material. Two insulator materials are used, epoxy as an electrical insulator and mylar as a thermal insulator. The neutron flux environment reduces the critical temperature (T_c) and the critical current (J_c) for the superconductor. The change in T_c is very small and can be neglected. On the other hand the change in the critical current affects the super conductor size required to carry the same current after irradiation. The experimental study by Soell⁵ shows a 10% reduction in the critical current when the conductor receives a neutron dose of $3 \times 10^{18} \text{ n/cm}^2$. This reduction in the critical current can be accommodated during the design stage or by use of the annealing process every few years of operation. The experimental data⁵ shows the annealing process at 300° K restores the critical current to 98% of the value before the irradiation. The TETR toroidal field magnets are designed to require annealing every five years of operation. This requires a 4% increase in the conductor size to accommodate the reduction in the critical current after 5 years of operation.

The resistivity of the normal conductor (stabilizer) is more sensitive to the neutron dose¹ at low temperature. The increase in the copper resis-

tivity is about 13 times the original value following irradiation to the 10^{-3} dpa damage level. This implies that the change in copper resistivity cannot be accommodated during the design process. The design criterion for TETR is to allow a maximum 50% increase in the copper resistivity after 5 years of operation since the experimental data⁹ show the annealing process at room temperature restores most of the original value of the resistivity (80% recovery for copper). Further, the annealing process at 77°K every few years is considered as a possibility for TETR. With these operational plans, the copper stabilizer is designed for a 50% increase in the copper resistivity.

Radiation damage to the insulators is more serious, since there is no way to restore the degradation in mechanical properties. The useful radiation limit is about 10^{17} n/cm² (1.2×10^8 rads) and 10^{19} n/cm² (10^{10} rads) for mylar and epoxy respectively. The organic material can be combined with inorganic reinforcement to increase the limit to 10^{18} n/cm² (10^9 rads).

The blanket/shield is designed to minimize the energy deposition in the TF magnets since each watt deposited at 4°K requires about 300 to 500 watts of refrigeration power.

The last constraint is related to the heat deposited in the cryosorption panels by neutron and gamma interactions. The panels are designed for a 0.001 w/cm^3 maximum heat load at the panel surface due to these interactions to limit the refrigeration power.

CALCULATIONAL MODEL

The large opening (divertor slots) used for the divertor represent a source of neutron and gamma leakage which affects the vacuum system and the TF magnets. In addition, these slots occupy a significant amount of space

which would otherwise shield the inner leg of the TF magnets. The usual one dimensional model was used for calculations based on an infinite cylinder resulting from a vertical cut through the torus. This model is acceptable for a system which has poloidal symmetry, a centered plasma, and a high aspect ratio. Another model which accounts for an off-center plasma and a low aspect ratio is based on an infinite cylinder resulting from a horizontal cut through the torus at the midplane. This model was developed for UWMak-III⁶ and used for TETR to obtain the midplane results. In general these one dimensional models can be used for a parametric study at an early stage in the design; but it is unrealistic to use such models for detailed information. Finally, a two dimensional R-Z geometry model was used to represent the actual geometric details as shown in Figure 1.

The parametric study for TETR³ shows clearly that a heavy material must be used for the blanket/shield to satisfy the design constraints and to minimize the operating cost. A combination of tungsten, stainless steel, lead, and boron carbide gives the best performance, and minimum radioactivity and dose outside the reactor³, which reduces the maintenance costs. Stainless steel has been chosen as a structural material for TETR, the basis for the choice is the available data about this material with a minimum extrapolation for the fusion environment.

The calculations employ a neutron cross section data set of 46 energy groups based on DLC-37 with the P_3 approximation. A modified version of the DOT program⁸ was used to perform the calculations in the S_6 approximation.

RESULTS

First, blanket/shield calculations have been executed based on 1-D transport model at the midplane. These calculations determined the

shielding composition and thickness.³ Then a detailed 2-D toroidal transport calculation using the actual geometry shown in Figure 1 and the neutron source distribution over space was carried out. The results from the 1-D calculations at the midplane satisfy the design criteria. A sample of the results from the 1-D calculations and the corresponding results from 2-D calculations are given in Table 2. The comparison between the 1-D and 2-D results at the midplane shows the 1-D model underestimates the integral quantities by a factor of 4 to 6. In fact, these differences are expected because the 2-D model considers a finite source and toroidal geometry compared to the 1-D model which considers an infinite cylinder geometry and source distribution.

Figure 2 shows the neutron flux ($E > 0.1$ MeV) in the divertor area where the magnet receives a maximum dose at 2.0 meters above the midplane. The magnet receives a maximum dose of 1.2×10^{10} n/cm² sec compared to 3.4×10^9 at the midplane and 2.3×10^9 from the 1-D calculations at $R = 1.6$ meters. The average dose results in a 1.0% decrease in the critical current after 5 years of operation. This reduction in the critical current is accounted for during the design stage. The average displacement rate in the copper conductor satisfies the design criterion, but the displacement rate in the first ten centimeters facing the plasma is relatively high as shown in Figure 3. A combination of two different approaches is considered to accommodate the change in the copper resistivity. The first approach is to design the copper conductor to accommodate a change in the resistivity greater than the average in this area. The second approach is to warm the magnet to reduce the copper resistivity.⁹ The actual criterion is to operate the magnet with a heat flux less than 0.4 watt/cm² when all the current is passing through the copper conductor.

The heat deposition rate in the toroidal field magnet is plotted in Figure 4. This shows a maximum of 0.0003 watt/cm^3 which is quite satisfactory. The maximum dose in mylar exceeds the maximum allowable by a factor of five which means another insulation material must be used in the area where the maximum dose exceeds the limit. On the other hand the maximum dose for the epoxy is much less than the limit as can be seen from inspection of Figure 2.

Figure 5 shows the neutron and gamma heat deposition rate in the cryosorption panels. The panels are designed for 0.1 watt/cm^3 maximum heat load which means the nuclear heating limit is of the order of 0.001 watt/cm^3 maximum neutron and gamma heating rate from Figure 5 is about $0.00007 \text{ watt/cm}^3$ which satisfies the design requirement. This low heating rate is achieved by using a shadow shield (chevron shield) in front of the cryosorption panels.

CONCLUSIONS

This nuclear analysis of a tokamak reactor employing the divertor concept points out the following important results. It is found that:

1. The maximum damage in the toroidal field magnets is located in the divertor area.
2. The organic insulators require a factor 5 to 10 improvement in the maximum allowable dose while the inorganic insulators appear to be quite satisfactory.
3. The increase in the electrical resistivity of the stabilizer requires the toroidal field magnets to be warmed periodically for annealing. This process effects the availability of the reactor but lowers the magnet cost.

4. The one dimensional analysis represents an underestimate of the integral quantities for the toroidal field magnets by an order of magnitude.

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Table 1 -- TETR Design Parameters

Fuel Cycle	D-T
Burn Time	60 sec
Total Cycle Time	72.5 sec
Major Radius	3.25 m
Impurity Control	Double Null Divertor
TF Superconductor	NbTi
Number of TF Coils	16
Magnet Insulation	Micarta, Epoxy, Fiberglass
Magnet Stabilizer	OFHC Copper
Magnet Structure	316 SS
Conductor Current	9838 A
Thermal Power Output	346 MW
Average Neutron Wall Loading	1 Mw/m ² /yr
Basic Structure	316 SS

Table 2 -- 1-D and 2-D Results Compared at the Midplane
to the Design Constraints

	<u>Design Requirement</u>	<u>1-D</u>	<u>2-D</u>
Neutron flux at R = 160 cm E > 0.1	--	7.2×10^8	3.4×10^9
Neutron flux at R = 160 cm E > 0.0	--	2.3×10^9	9.3×10^9
Maximum displacement rate in Cu at R = 160 cm dpa/YR	--	1.2×10^{-5}	5.5×10^{-5}
Average displacement rate in Cu dpa/YR	4×10^{-5}	1.2×10^{-6}	7.0×10^{-6}
Maximum dose in Mylar n/cm ²	10^{18} (10^9 rads)	1.2×10^{18} (1.95×10^8 rads)	4.74×10^{18}
Maximum heat load in the TF magnets w/cm ³	0.01	5.0×10^{-5}	3.0×10^{-4}

Figure (1) Cross Section View of the TETR Reactor

1.2.7

RADIUS (CH)

Figure (5) Heat Deposition Rate in the Cryosorption Panel

FIGURE 4 HEAT DEPOSITION RATE IN THE TOROIDAL FIELD MAGNET (W/CMS)

TOKAMAK EXPERIMENTAL POWER REACTOR

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ABSTRACT. A tokamak experimental power reactor has been designed that is capable of producing net electric power over a wide range of possible operating conditions. A net production of 81 MW of electricity is expected from the design reference conditions that assume a value of 0.07 for beta-toroidal, a maximum toroidal magnetic field of 9 T and a thermal conversion efficiency of 30%. Impurity control is achieved through the use of a low-Z first wall coating. This approach allows a burn time of 60 seconds without the incorporation of a divertor.

The system is cooled by a dual pressurized water/steam system that could potentially provide thermal efficiencies as high as 39%. The first surface facing the plasma is a low-Z coated water cooled panel that is attached to a 20 cm thick blanket module. The vacuum boundary is removed a total of 22 cm from the plasma, thereby minimizing the amount of radiation damage in this vital component.

Consideration is given in the design to the possible use of the EPR as a materials test reactor.

It is estimated that the total system could be built for less than 550 million dollars.

1. INTRODUCTION

Three design studies [1-4] for a tokamak Experimental Power Reactor (EPR) were conducted in the USA during 1974-1976. The general objective of these studies was to define the characteristics of a net electric power producing experimental reactor that could operate in the mid- to late-1980's. The designs that evolved from these studies were, in general, characterized by relatively conservative assumptions about plasma energy confinement and β -limits and by technology extrapolations that were constrained to be consistent with the proposed operating date. The conservative β -limits (3-5%) led to rather large devices (major radii greater than 6 m) in two designs, [2,4] while the third design [3] achieved a smaller size by assuming that β of 10% could be obtained with a highly elongated ($b/a = 3$) doublet-shaped plasma. These studies were, to varying degrees, carried out in sufficient detail to identify the design and technological problems and to define "first-cut" design solutions. A follow-on study at ANL has concentrated on simplifications and cost reductions which can be achieved based upon more favorable β -limits predicted by recent theoretical studies and upon improved engineering design.

2. DESIGN SUMMARY

An EPR design has been developed which meets the general objectives given in Table 1. A perspective view of this design is shown in Figure 1 and a vertical section view is shown in Figure 2. The principal geometric and performance parameters are given in Table 2.

Table 1. EPR Design Objectives

-
- 10-50 MWe Net Electrical Power
 - 1990-95 Initial Operation *
 - Improved Engineering Design
 - Cost Reduction*
 - Engineering and Materials Testing Capability
-

* Relative to previous EPR design.

Table 2. EPR Design Summary

Major radius, R(m)	4.7
Plasma radius, a(m)	1.34
Aspect ratio, A	3.5
Plasma elongation, b/a	1.65
Blanket/shield thickness (m) inner/outer	0.80/1.30
TF coil bore (m)	8.73 x 5.64
Maximum toroidal field, B_{MAX}^{TFC} (T)	10 (9*)
Average beta toroidal, β_t	0.07
Plasma current, I(MA)	7.3
Burn pulse (s)	60
Duty cycle (%)	80
Net electrical power (MWe)	81
Volt-second to plasma (V-s)	57
First-wall neutron load (MW/m ²)	0.7
First-wall lifetime (yr.)	≥ 5
Tritium inventory (kg)	1.3
Reactor containment building height x diameter (m)	55 x 54
Total costs (\$M)	539

* Operating reference point.

Figure 1. Perspective View of ANL EPR.

Figure 2. Vertical Elevation Showing Torus and Neutral Beam Design.

The EPR is an ignition device which uses 316 stainless steel as the structural material with energy conversion via a water/steam system. The toroidal and poloidal fields are provided by NbTi superconducting magnets. The plasma is heated to ignition by D^0 injection, although rf heating is an option. Energy is transferred into and out of a superconducting energy storage inductor to operate the poloidal coil and plasma heating systems, and a separate set of homopolar generators is used for current reversal in the ohmic heating system. A tritium system is provided which collects the reactor exhaust; separates, purifies, and enriches the fuel; and recycles it to the vacuum chamber.

3. REACTOR PHYSICS AND POWER PERFORMANCES

There is recent theoretical evidence [5-9] for the existence of MHD equilibria which support a significantly larger plasma pressure, and, hence, a larger β , than previously had been supposed. Thus, it appears that the β -limit for tokamak plasmas will be determined from MHD stability constraints.

An extensive trade-off study was performed [10] to determine the size and geometrical configuration of an EPR which would be able to produce net electrical power and achieve ignition even if confinement is as poor as is predicted by the trapped-ion-mode (TIM) theory. Two configurations were chosen for detailed study: (1) ($R = 5.0$ m, $A = 3.0$, $b/a = 1.3$); and (2) ($R = 4.7$ m, $A = 3.5$, $b/a = 1.65$). In both cases a D-shaped plasma with a shape factor (d) of 0.25 was chosen. See Table 2 for a summary of the geometric parameters for the second configuration, which was chosen as the reference.

Reasonable equilibria (i.e. those with centrally peaked current profiles no current reversal, and no excessive distortion of flux surfaces) with $q(0) =$ and $q(a) = 3$ were found which could support a toroidal beta, β_t , up to $\lesssim 10\%$,

for both designs. PEST code [11] calculations* indicate an upper limit of $\beta_t < 6\%$ for plasmas which can be stabilized against kink and ballooning modes; however, a number of important effects are not included in these linear stability analyses. This work led to the choice of $\beta_t = 7\%$ as the design basis for EPR, with provision for achieving the design objectives even if $\beta_t < 6\%$.

A reactor design based on the more elongated (1.65) plasma conditions was chosen as the reference point. Plasma parameters for the reference case are given in Table 3 for a range of β_t and $B_{\max}^{\text{TFC}}$ values. There are a range of design points which should achieve ignition with confinement poorer than that predicted by TIM theory (α_{TIM} is the ratio of energy confinement required for ignition, $\eta\tau_E = 3.4 \times 10^{20} \text{ s/m}^3$, to that predicted by TIM).

A series of burn cycle dynamic simulations were carried out in order to evaluate the cycle averaged power performance and the requirements upon the plasma driving and heating systems. For this purpose a reference plasma condition that yielded a maximum thermal power output of 606 MWth and had a maximum plasma current of 7.3 MA was chosen. Trade-off studies were performed to optimize the startup procedure. The length of the burn pulse is determined by the accumulation of impurities -- a pulse length of 60 s is felt to be achievable when a beryllium coating is used on the vacuum chamber surface, as discussed in the next section. Beam penetration calculations were performed to determine the required energy of the deuteron beam, based upon a reduced (1/5 to 1/10) density startup. The power performance parameters are summarized in Table 4, and the plasma driving and heating system requirements are specified in Table 5. The net energy flow during the burn cycle

* Performed by A. M. M. Todd, PPPL.

Table 3. Plasma Parameters for ANL EPR-77

β_t	$B_{\max}^{\text{TFC}}$ (T)	P_T (MW)	P_W (MW/m ²)	n_{DT} (10 ²⁰ m ⁻³)	I_P (MA)	α_{TIM}
0.05	8	194	0.4	0.8	6.4	1.6
	9	311	0.7	1.0	7.2	0.8
	10	474	1.0	1.2	8.0	0.4
0.06	8	279	0.6	1.0	6.4	1.1
	9	447	0.9	1.2	7.2	0.5
	10	681	1.4	1.5	8.0	0.3
0.07*	8	378	0.8	1.1	6.5	0.8
	9	606	1.3	1.4	7.3	0.4
	10	923	2.0	1.8	8.1	0.2
0.08	8	491	1.0	1.3	6.6	0.6
	9	786	1.7	1.6	7.4	0.3
	10	1200	2.5	2.0	8.2	0.2
0.09	8	619	1.3	1.5	6.7	0.5
	9	992	2.1	1.8	7.5	0.3
	10	1510	3.2	2.3	8.3	0.1
0.10	8	761	1.6	1.6	6.8	0.4
	9	1220	2.6	2.1	7.6	0.2
	10	1860	3.9	2.5	8.5	0.1

* Reference case.

Table 4. EPR Power Performance (Nominal)

Burn pulse length (s)	60
Dwell time (s)	15
Fusion energy/pulse (GJth)	23.4
Gross electrical power (MWe)	117
Net electrical power (MWe)	81

Table 5. EPR Plasma Driving and Heating System Requirements for Complete Burn Pulse

Volt-second to plasma (V-s)	57.0
Magnetic Energy (GJ)	2.7
OHC current reversal time (s)	2
Maximum field in OHC (T)	5.7
Maximum field rise in OHC (T/s)	6.7
Auxiliary heating power (MW)	40
Auxiliary heating time (s)	4.1
Neutral beam energy (keV)	180

is indicated in Figure 3. The reference operating point for the above mentioned calculations is $\beta_t = 7\%$ and $B_{\max}^{\text{TFC}} = 9\text{T}$ and assumes a thermal to electrical conversion efficiency, ϵ_{TH} , of 0.30.

Figure 4 shows the variation in the net electrical power, for a 60 s burn cycle, for different values of the principal parameters. There is clearly a wide range of $\beta_t - B_{\max}^{\text{TFC}} - \epsilon_{\text{TH}}$ for which this design would meet the power performance requirements for an EPR.

4. IMPURITY CONTROL

A passive impurity control measure -- coating the vacuum chamber wall with a low-Z material -- is proposed for EPR. The net electrical power averaged over the burn pulse has been computed [12] as a function of the surface material and the length of the burn pulse, the latter being determined by the accumulation of impurities. With a bare stainless steel wall, the plasma was quenched immediately upon the termination of auxiliary heating, and the net electrical power was a negative value of several hundred megawatts.

Figure 3. Net Energy Flow in Reference Burn Cycle.

Figure 4. Net Electrical Power Production as a Function of β_T , Toroidal Magnetic Field and Thermal Conversion Efficiency.

Calculations indicate that coating the first wall with a low-Z material will provide sufficient impurity control for the operation of an EPR. It is concluded that a ~ 1-2 minute burn pulse and net electrical power production is possible in an EPR using a low-Z coating for impurity control.

5. PLASMA HEATING SYSTEMS

A number of different alternatives are being considered for heating the EPR plasma from breakdown to ignition. These methods must be capable of producing 40 MW of power for about 4 to 6 seconds in order to heat the plasma to ignition. The most promising alternatives are neutral beam and radio frequency (rf) heating, with neutral beams presently considered to be the primary option.

Detailed designs of several neutral beam injector systems which would be applicable to a large machine have been developed. Three options, requiring varying degrees of extrapolation beyond the state of the art for TFTR ion sources, are summarized in Table 6. Two of the options assume the use of positive deuterium ion sources with D^+ fractions of 75 and 95 percent, respectively; these designs may be regarded as lower and upper limits to what might be achieved with sources of positive deuterium ions. Design 1 is the current reference. Design 2 represents a worthy but perhaps not quite attainable goal for further ion source development work. The third option is based on the possibility that a suitable extraction D^- ion source will be developed for the next generation of machines. To produce the monoenergetic neutral beams required by these machines from a beam consisting initially of positive atomic and molecular deuterium ions, we consider a basic system of an ion source that produces a diverging beam followed by a double focusing D^+

Table 6. 40 MW Neutral Beam Injection System for Tokamak EPR

	<u>Design 1</u>	<u>Design 2</u>	<u>Design 3</u>
Atomic ion [*]	180 keV D ⁺	180 keV D ⁺	400 keV D ⁻
Beam composition (D ⁺ , D ₂ ⁺ , D ₃ ⁺)	(0.75, 0.18, 0.07)	(0.95, 0.03, 0.02)	95% D ⁻ Assumed
Target For D ⁺ → D ⁰	D gas	D gas	D gas
Number of injectors	12	12	12
Number of ion sources/injector	2	2	2
Ion beam current density (A/cm ²)	0.135 avg.	0.135 avg.	0.080 avg.
Ion beam power (MW)	261	204	76
Source gas efficiency	0.45	0.50	0.10
Gas load/injector (Torr-ℓ/s)	70.4	41.8	15.3
Pumping speed/injector (ℓ/s)	4.2 x 10 ⁶	4.0 x 10 ⁶	2.6 x 10 ⁵
Zr-Al panel area/injector ^{**} (m ²)	126	120	8
Electrical power efficiency	0.33	0.44	0.65
Overall power efficiency ^{***}	0.38	0.48	0.70
Net power input (MW)	121	91	62

* Circular, initially diverging D⁺ beam; square, initially converging D⁻ beam.

** The required area has been doubled to allow for regeneration.

*** Assumes direct conversion efficiency = 0.85, thermal conversion efficiency = 0.30.

bending magnet, a tapered neutralizer, a second bending magnet and direct energy convertors to recover energy from unused molecular and atomic ions. Zirconium-aluminum getter pumps are presently planned for all systems because of their relative insensitivity to neutron bombardment and their ability to operate at elevated temperatures.

A system to deliver 40 MW of ion-cyclotron heating (ICRH) using ridged waveguides is under study. The guide should be capable of delivering 2.5 MW to the plasma at 58.5 MHz. Sixteen of these would be required to deliver 40 MW. The waveguide could be coupled to a 9-inch coaxial cable inside the outer blanket. Any system used in an EPR must be relatively simple, rugged, efficient and flexible enough to follow changes in density, temperature and major and minor radii. The radiation environment also imposes severe constraints.

The lower-hybrid heating (LHRH) system plan would be similar to that proposed in the previous design [2]. A phased array, the Lallia-Brambilla "Grill", [1] of reduced height, WR 510 waveguide is under study. This may allow plasma to be heated at a frequency of around twice the lower hybrid resonance value, which would avoid power limitations caused by parametric instabilities [2].

6. ENERGY STORAGE AND TRANSFER

The energy storage and transfer (EST) system consists of a central energy storage inductor (ESI), a thyristor bridge system to transfer energy between the ESI and the OH coil, EF coil and neutral beam (or rf) systems, and a rectifier to transfer makeup energy from a power substation into the ESI.

A separate inertial energy storage unit, consisting of radial-stacked homopolar generators, is used to transfer inductive energy into and out of the OH coil system. The EST design requirements were determined from burn cycle simulations, as discussed in Section 3. The EST system is summarized in Table 7 and described in more detail in Reference 2.

6.1 Ohmic Heating System

The conceptual design for the EST device for the OH coil is based on using counter cyclonic generator (CCG) type homopolar generators with NbTi coils in a magnetic yoke [2]. Ten modules in series will develop 51,300 volts and will have a minimum capacitance of 0.523 farads. The minimum field reversal time in the absence of stray inductances and circuit losses would be 1.92 s.

The CCG is disconnected from the OH coil during the burn. The OH coil current and $\dot{B}$ are maintained with a separate power supply driven from a central energy storage inductor. An actively controlled inductor-converter of the type described for the EF coils is used for this application. A total of 490 MJ is taken from the ESI during the burn. The inductor-converter will provide a peak voltage of 800 V and the energy transfer is reversible.

6.2 Equilibrium Field System

An actively controlled version of an inductor-converter bridge [13,14] driven from a central ESI is selected as the power supply and energy storage choice. A circuit diagram for a 3-phase inductor-converter bridge is shown in Figure 5. The EF coil is represented by L_E and the central energy storage coil is represented by L_S . The capacitors in the circuit are relatively small in the sense that they only store a small fraction of the total magnetic energy in the circuit. The left-hand (LH) bridge commutates the L_S current

Figure 5. Inductor-Converter Bridge Circuit Diagram for Equilibrium-Field Coil System.

Table 7. Energy Storage and Transfer Systems - Maximum Ratings for Reference Burn Cycle

Ohmic Heating Systems

Homopolar Generators

No. of generators in series	10
No. of drums per generation	8
Total energy transfer (MJ)	607
Peak power (MW)	957
Peak available voltage (kV)	51.3
Peak available current (kA)	41.1
Minimum equivalent capacitance (f)	0.523

Rectifier System

Type	3 $\emptyset$ inductor-converto bridge
Energy transfer (MJ)	490
Peak power (MW)	56
Peak current (kA)	70
Peak voltage (kV)	0.8
Switching frequency (Hz)	100

Equilibrium Field System

Type	3 $\emptyset$ inductor-converto bridge
Energy transfer (MJ)	1770
Peak power (MW)	448
Peak current (kA)	70
Peak available voltage (kV)	26
Switching frequency (Hz)	100

Neutral Beam System

Type	SCR, DC/AC/DC at kHz
Energy transfer (MJ)	480
Voltage (kV)	180
Power (MW)	120

Central Energy Storage Inductor

Type	Superconducting ring dipole
Energy stored (MJ)	3700
Energy transfer (MJ)	2750
Peak current (kA)	70
Peak power (MW)	570
Average power from utility grid or power plant (MW)	10.7

through the three capacitors so as to create cyclic voltages on each capacitor, which are separated 120° in phase from each other. The right-hand (RH) bridge is switched in the same sequence but at a different relative time from the OH bridge. The current in L_E affects the voltage waveshape on the capacitors. The peak voltage required for normal charging of the EF coil is 18,500 V. The bridge is designed to operate with a switching frequency on the right hand bridge leading the left hand bridge by 45° when 18,500 volts is generated across the EF coil. The phase can be advanced to 90° to generate 26 kilovolts across the EF coil or phased back to where -26 kilovolts can be generated.

6.3 Neutral Beam Power Supply

The neutral beam injector energy transfer system is composed of two major components; a saturated time-delay transformer (STDT), [15] which uses the saturation effects of magnetic cores to act as a current surge limiter, and a high frequency, polyphase-controlled rectifier [16] using SCR switches to enable rapid de-energization of the beam in periods of less than 100 μ s. A circuit diagram is shown in Figure 6.

The injector power supply extracts energy from the same central ESI that the EF coil power supply uses. An inductor-converter similar to that described for the EF coil system drives a high frequency, polyphase inverter. A 10 kHz voltage is developed in a summing transformer, filtered and subsequently rectified. The output lead is connected to the injector through an STDT. In the event of a spark, the STDT prevents energy transfer into the spark and, if necessary, the polyphase rectifier can be phased back quickly enough to remove power from the injector in the event that the discharge path has not cleared in a time beyond which the STDT is unable to absorb the energy.

Figure 6. HV Power Supply for NBI Block Diagram.

Nonsaturated, longitudinal reactors [17] limit the discharge current of the energy stored in the circuit capacitance of the filament and of the arc power supplies.

6.4 Central Energy Storage Inductor

The neutral beam injector and/or rf amplifier power supplies and EF and OH coil power supplies extract energy from a central energy storage inductor (ESI). The ESI is a superconducting ring dipole (torus) with a minor radius of 0.8 m, a major radius of 3.9 m, a peak field of 5 T and an inductance of 1.51 h. The design of the conductor for the ESI is based on the Rutherford cable concept with 200 1-mm diameter wires wound on an 0.003 m by 0.097 m fiberglass support strip. The conductors would be the same as those developed for the poloidal coil design. A rectifier power supply draws an average power of 10.7 MW from the substation to resupply losses in the energy storage and transfer system.

7. MAGNETS

The toroidal and poloidal field coil systems are summarized in this section. These systems are described in Reference 16.

7.1 Toroidal Field Coils

The superconducting toroidal-field (TF) coil parameters are given in Table 8. The TF coils are designed for nominal operation at 9 T and 4.2 K, but with provision to operate at 10 T by pumping down to 3.7 K in order to compensate if β is lower than is anticipated.

The TF coils are designed for cryogenic stability. Because most of the resistivity of the copper stabilizer is magnetoresistivity and radiation-induc

Table 8. Toroidal Field Coil Parameters

Number of coils	16
Coil shape	Pure-tension D
Superconductor	NbTi
Stabilizer	Copper
Support material	Stainless steel
Support cylinder	Fiberglass-reinforced plastic
Coil bore, vertical x horizontal	8.73 m x 5.64 m
Allowed stress in copper and SC	15,000 psi
Allowed stress in stainless steel	60,000 psi
Operating temperature	3.7 K (4.2 K)
Peak field	10 T (9 T)
Field at plasma	4.81 T (4.33 T)
Total amp-turns	115 MA-turns (104 MA turns)
Operating current	60,000 A (54,000 A)
Turns per coil	60 x 2
Average current density	1540 A/cm ² (1386 A/cm ²)
Inductance	7.9 Henry
Stored energy	14.3 GJ (11.6 GJ)
Weight of NbTi	43 Mg
Weight of copper	552 Mg
Weight of stainless steel	328 Mg

resistivity, both of which vary across the coil, the copper is graded with more copper on the inner turns where the radiation level and magnetic field are largest. The NbTi is also graded; less is required on the outer turns where the field is lower and the critical current density is higher. A 50 kA sheet conductor is specified, consisting of a sheet of copper with NbTi-copper composite wires stranded around it and soldered to it. Stainless steel reinforcement is wound with the sheet conductor into two pancakes per coil with a support rib between them. The conductor is wound in precompression and the stainless steel in pretension so that the stainless steel will support 60,000 psi tensile stress without stressing the copper beyond 15,000 psi when the coils are energized. Such a conductor is inherently a dc conductor and is enclosed by a high-purity aluminum field shield operating at 18 K to reduce the ac losses from the pulsed plasma and equilibrium fields.

The TF coils operate in series to eliminate the possibility of carrying unequal currents, which would produce large bending moments and out-of-plane forces at the coils. Protection circuitry includes a dump resistor which is always connected. A surge in magnet terminal voltage in any coil due to a normal region developing will serve as a signal to disconnect the power supply. The dump resistor is sized to prevent the development of excessive voltage during the discharge.

7.2 Poloidal Field Coils

The ohmic-heating (OH) and equilibrium-field (EF) coils are located external to the TF coils to facilitate installation and maintenance and to allow them to be superconducting. The location of these coils is shown in Figure 2, and the principal parameters are given in Table 9.

Table 9. Poloidal Coil Parameters at Maximum Design Ratings

	<u>OH Coils</u>	<u>EF Coils</u>
Number of coils	Solenoid + 10	12
Location	Outside TF coils	Outside TF coils
Superconductor/Stabilizer	NbTi/Cu	NbTi/Cu
Operating temperature	4.2 K	4.2 K
Current density	1730 A/cm ²	1730 A/cm ²
Operating current	70 kA	70 kA
Total amp-turns	82.3 MA turns	54.4 MA turns
Maximum Volt-seconds	90 V-s	37 V-s
Central field	8 T	0.3 T
Maximum rate of change of field	6.7 T/S	---
Field at plasma	< 10 ⁻³ T	0.56 T
Inductance	0.73 H	0.94
Coupling coefficient to plasma	- 0.25	- 0.175
Self inductive energy	1.75 GJ	2.3 GJ
Inductive energy: OH & EF and plasma	3.7 GJ	

The OH coils consist of a central solenoid and ten additional coaxial coils. The central solenoid is between the TF coils and the central support cylinder; it is interrupted every 25 cm axially by a 5-cm high steel ring which transmits the TF coil centering force to the fiberglass-reinforced plastic support cylinder. This configuration maximizes the central flux core area for a solenoidal OH coil that provides the necessary volt-seconds. The OH coils are energized to give a negative flux before the plasma is formed, then the current in the OH coils is reversed, inducing a current in the plasma.

The EF coils are decoupled magnetically from the OH coils by locating them all on the same flux line of the OH coils, operating them in series, and requiring that the numbers of positive and negative amp-turns in the EF coil system be equal. Selection of the flux line and of the coil positions along the flux line are governed by engineering constraints. The currents are selected so as to establish the required equilibrium field configuration in the plasma, using a free-boundary MHD equilibrium code, but subject to the constraint of minimizing energy in the EF coil system. The required field pattern could be achieved for plasma shape factors (d) of 0.25, but not for $d = 0.5$, with the EF coils excluded from the central flux core and located externally to the TF coils.

The EF and OH coils are cryogenically stable. A 70 kA fully-transposed cable of copper and superconducting composite strands around a stainless steel solid or stranded strip is specified. This design is described in Reference 2.

A system of water-cooled copper coils is located within the blanket and shield. This coil system provides the ~ 500 V, 5 ms voltage pulse necessary to break down the plasma and also provides the trimming fields necessary to control the plasma position.

8. FIRST WALL/BLANKET/SHIELD AND ENERGY CONVERSION SYSTEMS

The basic features of the present design point for the first wall/blanket/shield (FW/B/S) are illustrated in Figure 7, summarized in Table 10 and described in more detail in References 19 and 20. The vacuum vessel is an irregular toroidal shell, fabricated from 7-cm thick stainless steel plates that are welded together in sections. This shell forms the primary vacuum boundary for the plasma chamber, is located outside the blanket assembly and

Figure 7. Design of Segmented Blanket, Vacuum Vessel and Shield Components.

Table 10. Major Design Concepts and Parameters for the EPR First Wall/Blanket/Shield System

First Wall

Type	Cooling panel
Thickness	1-2 cm
Material	316 SS, BeO coated
Maximum temperature	450°C
Neutron damage	11 dpa/(MW-yr/m ²)
Helium generation	208 appm/(MW-yr/m ²)
Hydrogen generation	512 appm/(MW-yr/m ²)
Nuclear heating	7.6 W/cm ³
Neutron Wall Load	0.7 MW/m ²

Blanket

Type	stacked rectangular slabs
Material	316 SS
Thickness	0.2 m
Coolants	pressurized H ₂ O/steam
Maximum temperature	550°C
Maximum nuclear heating	6.2 W/cm ³
Weight	~ 700 Mg

Vacuum Vessel

Type	modular sections, welded plate
Location	outside blanket
Number of sections	16
Number of plates/section	12
Material	316 SS
Dimensions	
Thickness	0.07 m
Vertical bore	5.4 m
Horizontal bore	3.1 m
Section semiangle	22.5°
Maximum temperature	550°C
Coolant	pressurized H ₂ O
Coolant geometry	gun-bored channels
Weight	~ 260 Mg
Heating Rate	1.0 W/cm ³

Bulk Shield

Material	
inner	304 SS, B ₄ C
outer	B, C, Pb-mortar, 304 SS, Al
Thickness	
inner	0.52 m
outer	1.0 m
Weight	~ 2000 Mg
Maximum nuclear heating	0.55 W/cm ³

provides the principal support for that assembly. The section shown in Figure 7 is one of sixteen which join together by bolting and seam welding to form the complete torus enclosure. The entire vacuum vessel is supported from below the reactor and is anchored to the lower (bottom) shield blocks. The top plates of each vacuum vessel section are removable and provide the access for all operations internal to the vessel. This large, relatively unobstructed aperture provides for simple remote operated tools and machines to be used in removal and replacement operations. All liners, blanket blocks and other internals are removed through the top closure with simple hoists and guide frames. Each vacuum vessel plate is fabricated with appropriately dispersed, gun-bored coolant channels that terminate with connections to the coolant manifold system. By locating the vacuum vessel outside the blanket and, hence, in a zone of lower radiation exposure and reduced thermal energy generation, it becomes more accessible (e.g. for leak repair), will have longer lifetime, requires less (8 MW) cooling capacity and is in a region considerably less subject to thermal fatigue than the region inside the blanket.

The blanket assembly is comprised of stainless steel blocks with transverse gun-bored coolant channels. There are a total of 8 blocks per vacuum vessel section (supported by the vacuum vessel) which are installed and removed through the upper vacuum vessel closure.

The EPR power conversion system incorporates a dual cycle in which an indirect pressurized water system is coupled to a direct cycle superheated steam system. Pressurized water from the first wall assembly and the inner and outer blanket regions delivers thermal energy to an evaporator operating at 340°C and 13.8 MPa (2-00 psi) and utilizing much of the same technology and conditions as are presently employed in pressurized water reactors (PWRs). The

steam generated in the evaporator is fed to the upper and lower (superheater) blanket blocks where it is heated to 410°C at ~ 8.6 MPa (1250 psi). This steam is fed directly to the turbo-generator in a fashion comparable to boiling water reactors (BWRs) and results in an overall cycle efficiency of ~ 39%. The difficulties associated with the use of a direct cycle steam, (stemming mainly from radioactivity transport) need to be more carefully analyzed, but with the exception of a potentially higher tritium level they do not appear to be more severe than those encountered in BWRs. The principal features of this dual cycle energy conversion system are summarized in Table 11.

Table 11. EPR Energy Conversion System

Heat Transport System		
Capacity	360 MWth	
Pressurized Water Circuit		
Pressure	15.2 MPa	(2200 psi)
Inlet Temperature	238°C	(460°F)
Outlet Temperature	340°C	(644°F)
Steam Circuit		
Pressure	8.2 MPa	(1200 psi)
Inlet Temperature	300°C	(535°F)
Outlet Temperature	410°C	(770°F)
Power Plant Rating	120 MWe	(270 MWth)
Cooling Tower Capacity	315 MWth	

Thermomechanical analyses [21] of the blanket blocks have shown that pressurized water, helium and steam would all be satisfactory coolants for near-term operating scenarios. By properly grading the length and size distribution of the coolant channels in the blanket block, it is possible to

achieve realistic performance parameters (in terms of present day technology) for each of these coolants and to maintain stress levels within the blanket blocks at values that correspond to a strain of less than 0.2%. Because of the large thermal inertia of the blanket blocks, the cyclic strain variation is insignificant during normal reactor operation. The overall unconstrained thermal deformation of each block is $\sim 0.6\%$ of the room-temperature dimensions with the blocks tending to become convex toward the plasma. Blanket coolant temperature drops of $< 1^\circ\text{C/s}$ are experienced during the dwell time.

A high-performance water-cooled stainless steel panel, which is attached to the plasma side of each blanket block, serves as the first wall for the reference design. These coolant panels are designed to accept the high surface heating (equivalent to $\sim 25\%$ of the neutron wall load) produced by particle transport and electromagnetic radiation from the plasma. The plasma-side surfaces of the panel walls are coated with a low-Z material, e.g., Be, BeO, or B_4C , to control the influx of high-Z transition-metal impurities into the plasma from the steel panels. Cyclic thermal stresses in the coolant panel are minimized by restricting the plasma-side panel wall to thicknesses < 0.25 cm. Calculations [22] of the thermal response of the first wall during off-normal operation indicate that the panel wall will withstand several plasma dumps if the plasma energy is evenly distributed over $\geq 10\%$ of the wall (for a plasma with a nominal 1 MW/m^2 neutron wall load). A plasma dump of this type will vaporize or ablate a 5-20 μm layer of the coating, depending on the material, from the wall. Much of the vaporized material is expected to re-deposit on the wall during cooling after the dump. The frequency with which a 200- μm thick coating would have to be replaced would be determined by the uniformity of the redeposition and the probability of dumps occurring on the same area of the wall.

The severe constraints on space in the high field regions of small-size reactors has been resolved for the EPR by segmenting the bulk shield into inner and outer regions that employ different shield materials. In all, there are 12 shield blocks per vacuum vessel section. All of the blanket and bulk shield blocks are electrically insulated from one another to provide the required [2] disruption of eddy currents generated by the pulsed magnetic fields. The inner blanket/shield design evolved from an extensive parametric study [23,24] including radiation effects, local nuclear heating and refrigeration power requirements in the superconducting TF magnets. Based on this study: (1) the inner shield composition is an optimized combination of stainless steel and boron carbide, and (2) the inner blanket/shield thickness is set at 0.9 m. The TF coils would be annealed at the end of each 1 MW-yr/m^2 irradiation period with the maximum radiation induced resistivity in the copper stabilizer reaching $4.2 \times 10^{-8} \text{ } \Omega\text{-cm}$ and with only $\sim 3\%$ change in the critical current density of NbTi. The maximum radiation dose to the epoxy insulators inside the conductor is $4 \times 10^9 \text{ rad}$ after 4 MW-yr/m^2 of operation. Low temperature irradiation measurements on organic insulators are required to provide a definitive radiation damage limit.

Effective shielding is provided for all penetrations that have to be accommodated in the blanket/bulk shield. The neutral beam ducts are surrounded by an 0.8-m thick local penetration shield as they emerge from the bulk shield. This penetration shield extends to the beam injectors and is tapered off in proportion with the radiation attenuation. Movable shield plugs are used where necessary to block penetrations that need not be open during the plasma burn. The concept of connecting the torus evacuation ducts to the neutral beam ducts at locations outside the blanket has been adopted because it offers

several advantages over the use of separate torus evacuation ducts that extend directly to the first wall.

9. MATERIALS

It appears at this time that there are no materials limitations that preclude the viability of the EPR design. This is in part due to the fact that a conscious effort has been made in the design to remove the vacuum vessel from the high damage region and to allow remote repairs to be made in a straight-forward manner. The relative amounts of damage expected in the different structural components are presented in Table 10.

The most vulnerable component in the EPR is the first wall cooling panel which is subjected to the highest fatigue loading while enduring the most intense radiation damage. In predicting the lifetime of this component, swelling, ductility loss and fatigue were considered. The operating temperature is an important factor in assessing all three of these areas. For example, in the temperature regime of 300 to 500°C, the form of radiation damage changes dramatically, going from a high concentration of very small point defect clusters at 300°C to an array of relatively large faulted dislocation loops and voids at 500°C. Assuming an allowable swelling of 10% and a reduction of ductility to 0.5% uniform strain, the region of the wall at temperatures in excess of 400°C would be limited to approximately 4 MW-yr/m² of service. Thus, based on these considerations at the nominal* EPR wall loading of 0.7 MW/m² and plant factor of 50%, the first wall coolant panels would require replacement every 11.4 years. Defining the lifetime of the

* The maximum wall load is 1.3 MW/m², but the average wall load over the burn pulse is 0.7 MW/m².

regions at temperatures near 300°C is more difficult, as considerably less information is available. The two properties that need better definition at this irradiation temperature are ductility and fatigue. The design limit of 0.5% uniform ductility is based upon fission reactor data. There are several factors that could cause a higher limit to ultimately be set for EPR. A more conservative limit of 1% ductility would correspond to 2.5 MW-yr/m², which would require wall replacement every 7.8 years. A fatigue analysis of the front coolant panels indicates that if the irradiated properties are at least the same as are currently assumed by the ASME code for 316 stainless steel then the lifetime of this component will be limited to 5 years.

There are several other areas of concern relating to the ability of different components to perform for long periods in the EPR environment. More experimental results are needed in each of the following areas before an EPR can be confidently designed.

1. Fusion welds lead to compositional and microstructural variations that could have important effects on the resistance of austenitic stainless steels to radiation damage. The extent of this problem can only be determined by experimental studies that involve the irradiation of prototypic welded samples in mixed spectrum fission reactors where the expected helium/dpa generation rates can be duplicated.
2. Another area concerns the radiation damage of high temperature inorganic insulators, since the EPR design includes a ceramic circuit breaker in the first wall. While yttria-based materials look very attractive for this application at this time, much more needs to be done to develop the necessary property data before an accurate estimate of an expected lifetime can be established.

Existing information does indicate that the yttria is stable to at least 2 MW-yr/m², or 6.25 years of EPR service.

3. The third area where improved information would help is in the selection of a cryogenic insulator for use in the superconducting magnets. Commonly used organic insulators may be limited to $\sim 10^9$ rads of exposure, which dictates the use of a thicker than otherwise optimum blanket and shield. More detailed information is needed on conventional glass-bonded epoxy insulators as well as on attractive alternates such as glass-bonded mica.
4. The fourth area concerns the development of a low-Z first-wall coating that either is long-lived or can be easily replaced. Sputtering rates, especially at lower projective energies, for both deuterium and helium are needed before the burn cycle can be fully defined.

Considerations of the potential materials limitations for the EPR highlights the need of the fusion program for a large capacity test facility. The EPR could fulfill this function, as several million cubic centimeters of test volume could be made available for use. At a wall loading of 1 MW/m² and a 50% plant factor, quantities of test specimens sufficient to statistically document the response of a wide variety of materials to a fusion environment could be exposed at the rate of about 6 dpa per year. The potential for higher wall loadings is included in the current design. Upon comparison of the EPR with other possible test facilities, one is immediately struck by the limitation of the (D,Li) and jet sources to benchmarking experiments due to their very limited experimental volume (hundreds of cc's) and the restriction of mixed spectrum fission reactors such as ORR to the testing

of nickel containing alloys. It is therefore reasonable that the EPR be used as an instrumented materials test facility.

10. TRITIUM SYSTEMS

The technical basis for the design of the EPR tritium handling systems has been described in previous publications [2,10,25,26]. In essence, these systems must provide for: (1) recycle and processing of the plasma exhaust, (2) recovery and consolidation of tritiated wastes, and (3) emergency air handling and detritiation of the reactor building and other major facility enclosures. A key attribute of the design is provision for multiple levels of containment around all components and hardware that contact sizeable quantities of tritium. This section describes the principal features of the EPR tritium facility.

A layout of the tritium handling and fuel processing facility for a near-term tokamak reactor like the EPR is illustrated schematically in Figure 8. Unburned DT fuel plus impurities collected by the torus evacuation system are transferred to a consolidation manifold. The gas mixture is then compressed to 0.1 MPa by a series of oil-free metal bellows pumps. The fuel is purified by trapping the condensible impurities at 30°K. The trapped impurities are processed to recover residual tritium by hot gettering or by a series of cryochemical separations [26]. Helium is removed by a cryogenic stripping column. The purified fuel is isotopically enriched by a cryogenic distillation cascade, [25] then prepared for delivery to the torus as ambient gas, frozen pellets or neutral beams.

Every effort is made to minimize the likelihood and consequences of leaks. The tritium handling components and hardware are enclosed in dry

gloveboxes or are jacketed in pipe casings. Further, the tritium processing components indicated in Figure 8 and the emergency air detritiation system are located in a separate, reinforced containment building. The jacketing pipes and gloveboxes are purged with an inert gas stream which is processed by a tritium waste treatment system of the type described in Reference 26.

The total tritium inventory is ~ 1300 g. Of this amount, the quantity (~ 130 g) that is present in the vacuum pumps and the fuel preparation units during normal operation could conceivably be released to the facility buildings (volume = 6×10^4 m³). Should that quantity be accidentally released to the building, the 50 m³/s emergency cleanup system would take about 50 hours to reduce the ambient tritium levels to recommended radiation control guidelines of 5 $\mu\text{Ci}/\text{m}^3$. A 1-cm thick steel membrane inhibits tritium escape through the walls of the containment building, in the event of a leak.

The principal parameters which characterize the tritium and vacuum systems are given in Table 12.

11. REACTOR FACILITY

The overall reactor facility is shown in Figure 9. It consists of the reactor building and seven major support buildings and structures. The reactor support facilities are located in an attached building adjacent to the main reactor cargo doors. This building houses the repair/maintenance hot cell, the mockup/test equipment and other special facilities for initial winding and cryostat preparation of the TF coils. Adequate cranes and access to equipment are provided for handling all reactor components prior to their installation. A third large structure also located adjacent to the reactor building houses the reactor magnet power supplies and compressors for the

Table 12. Tritium Facility and Vacuum Parameters

Toroidal Vacuum System

Volume (m ³)	450
Past-burn gas load (Pa-m ³)	490
Dwell time (s)	15
No. cryosorption pumps	12
Speed/pump (m ³ /s)	80

Tritium Inventory

Storage (30 days) (g)	1192
Fuel preparation (g)	110
Vacuum pumps (g)	20
Total (g)	1344

Mass Flow Rates

	<u>D</u>	<u>T</u>	<u>He</u>	<u>H</u>
Cold gas load (g/day)	58	126		
Fuel makeup (g/day)	56	121		
Neutral beam injection (g/day)	51	---		
Exhaust (g/day)	138	207	53	2
Neutral beam exhaust (g/day)	99	3		

cryogenic units. Power leads are run through seals in the reactor containment to the various reactor components. An energy storage ring, remotely located, is part of the reactor power distribution system. The facility has a tritium facility for storage, preparation and reprocessing of reactor fuel and clean-up of tritium contamination. A 120-MWe power plant is a major facility feature. Other facility structures include an office building housing the control center, a waste heat cooling tower array and a 120 MWe power sub-station.

Figure 9. Plan View of Facility.

12. COSTS AND SCHEDULE

A cost estimate for EPR is given in Table 13.

A construction schedule from the beginning of site construction to initial startup is given in Figure 10. Several years of design would precede the beginning of site construction.

Table 13. Plant Capital Investment Direct Cost Estimate
(in millions of 1977 dollars)

1.0	Structures & Site Facilities	\$ 46.0
2.0	Reactor	160.6
3.0	Reactor Plant Facilities	144.9
4.0	Turbine Plant	<u>20.1</u>
	<u>TOTAL</u>	\$371.6
	Engineering (20%)	74.3
	Contingency (25%)	<u>92.9</u>
	<u>GRAND TOTAL</u>	\$538.8

Figure 10. Reactor Construction Schedule.

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