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RECENT CONTRIBUTIONS TO FUSION REACTOR DESIGN
AND TECHNOLOGY DEVELOPMENT

Fusion Power Program

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FOREWORD

The report contains a collection of recent fusion technology papers on the STARFIRE Project, the study of alternate fusion fuel cycles, a maintainability study, magnet safety, neutral beam power supplies and pulsed superconducting magnets and energy transfer.

This collection of papers contains contributions for Argonne National Laboratory, McDonnell Douglas Astronautics Company, General Atomic Company, The Ralph M. Parsons Company, the University of Illinois and the University of Wisconsin.

STARFIRE - A COMMERCIAL TOKAMAK REACTOR*

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Introduction

The basic objective of the STARFIRE Project¹ is to develop a design concept for a commercial tokamak fusion electric power plant based on the deuterium/tritium/lithium fuel cycle. The key technical objective is to develop the best embodiment of the tokamak as a power reactor consistent with credible engineering solutions to design problems. Another key goal of the project is to give careful attention to the safety and environmental features of a commercial fusion reactor.

The basic design guidelines for STARFIRE assume the successful operation of a tokamak engineering test facility (ETF) and a demonstration power plant. STARFIRE is considered to be the tenth plant in a series of commercial reactors. It is, therefore, assumed that a well established vendor industry exists and that utilities have gained experience with the operation of fusion plants.

The STARFIRE Project¹ was initiated in May, 1979, with the goal of completing the design study by October, 1980. The purpose of this paper is to present an overview of the major parameters and design features that have been tentatively selected for STARFIRE. Several papers presented in this conference provide more details on the design characteristics of key reactor components.

Major Parameters

A primary goal of the STARFIRE study is to select the most attractive set of design parameters and concepts that make tokamaks economically competitive and environmentally acceptable. Results provided by and experience gained from previous fusion reactor design studies in the United States and worldwide have provided an excellent starting point for this project. In addition, extensive trade-off studies² were performed to support the design selection process for STARFIRE.

Figure 1 shows a cross section of the reactor. A summary of the major reactor parameters and design features is given in Table 1. The reactor thermal power is ~ 3800 MW and the net electrical power is ~ 1150 MW. This power level is expected to be in the most desirable range of power rating to the utilities in the STARFIRE time frame.

The neutron wall load is 3.5 MW/m^2 . Based on results from detailed systems studies, this moderately high wall load is a reasonable choice that results in a small size reactor without excessive requirements on the first-wall cooling capability, maximum toroidal magnetic field and frequency of structural material replacement.

A D-shaped plasma with a height-to-width ratio of 1.6 has been selected. This was found to be nearly the highest elongation that is consistent with the STARFIRE goal of locating almost all the equilibrium-field (EF) coils outside the toroidal-field (TF) coils.

With an aspect ratio of 3.6 the major radius is 7 m. The average plasma toroidal beta is 0.067 and the maximum toroidal magnetic field is 11 T.

Operating Mode

Steady-state has been selected as the mode of operation for STARFIRE. Although experimental information on plasma current drive by means other than inductive OH coils are very limited, results from recent theoretical studies justify the assumption that continuous plasma burn can be achieved in the STARFIRE time frame.

The potential advantages of steady-state reactor operation include: (a) increased component and system reliability, (b) eliminating fatigue as a serious concern for the structural material in the first wall and blanket, (c) no thermal energy storage is required and the need for an intermediate coolant loop is reduced, and (d) no electrical energy storage is necessary.

Plasma Engineering

The basic plasma parameters of the STARFIRE design are listed in Table 2. Lower hybrid waves constitute the reference plasma current driver due to the more developed theoretical understanding of this option. Other wave drive candidates considered include relativistic electron beams and magnetosonic waves.

The ratio of fusion power to rf power (Q) required to maintain the steady-state toroidal current of a tokamak is limited by the plasma's accessibility to lower hybrid waves. In particular, low density plasmas are more easily penetrated and result in higher Q than high density plasma. This is the reason that, for a fixed B_0 , relatively high temperatures and low densities are attractive for STARFIRE. However, for $T > 15 \text{ keV}$ the fusion power density decreases so quickly that relatively high magnetic fields are necessary

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

Fig. 1. STARFIRE reference design.

Table 1. STARFIRE Major Design Features

Net electrical power	1150 MW
Gross electrical power	1600 MW
Fusion power	3200 MW
Thermal power (nominal)	3800 MW
Thermodynamic efficiency	41%
Overall availability	75%
Average neutron wall load	3.5 MN/m^2
Major radius	7.0 m
Plasma half-width	1.94 m
Plasma elongation (b/a)	1.6
Max. toroidal field (nominal)	11.0 T
Number of TF coils	12
Plasma burn mode	Continuous
Current drive method	rf
Plasma heating method	rf
TF coils material	Nb ₃ Sn/NbTi/Cu/SS
Wall structural material	Ferritic steel
Blanket structural material	Ferritic steel
Wall coolant	D ₂ O
Tritium breeding medium	Li ₂ O
Blanket coolant	Helium
Plasma impurity control	Low-Z coating + limiter and vacuum system + enhanced radiation + field margin
Primary vacuum boundary	At inner edge of shield

Table 2. STARFIRE Plasma Parameters

Major radius	$R = 7 \text{ m}$
Plasma half width	$a = 1.94 \text{ m}$
Elongation (b/a)	$\kappa = 1.6$
Plasma shape	"D"
Average toroidal beta	$\beta_t = 0.067$
Plasma current	$I = 11 \text{ MA}$
Safety factor (center)	$q_0 = 1.4$
Safety factor at edge	$q_a = 4.2$
Toroidal field on axis	$B_{to} = 5.62 \text{ T}$
Average DT ion density	$\bar{n}_{DT} = 1.0 \times 10^{20} \text{ m}^{-3}$
Average electron density	$\bar{n}_e = 1.3 \times 10^{20} \text{ m}^{-3}$
Average ion temperature	$\bar{T}_i = 17 \text{ keV}$
Average electron temperature	$\bar{T}_e = 22 \text{ keV}$
Fractional impurity concentration	$n_{Be}/n_{DT} = 0.04$
Fractional alpha concentration	$n_\alpha/n_{DT} = 0.09$
Fractional burnup	$f_b = 0.11$

to keep the reactor size attractively small. A related strategy to maximize Q is to drive the highest current densities where the plasma density is low, i.e., near the plasma surface. Particular attention must be given in this case to the MHD stability of such current density profiles.

Of the two candidate rf sources, crossed-field amplifiers (CFA) and klystrons, the former may promise to operate at higher efficiencies (70-90%), but, due to the large amounts of power dissipated in the rf structure, the CFA option may increase the engineering difficulties. The transmission line is envisioned to be a high power pressurized waveguide with a power splitting labyrinth to provide the grill phase shifts. The system will be designed with the ECR layer in the high pressure region; the vacuum window will be placed out of the direct line-of-sight of neutron radiation. Phase shifters will be included to provide spectral turning for the reactor startup period.

Our basic approach for impurity control is to use a system with fairly low removal efficiency, ~ 30%. This is sufficient to maintain a stable operating point, while at the same time reducing the amount of pumped tritium.

The reference system chosen is a limiter/vacuum system which together serves to concentrate and pump some of the plasma particle outflux. In addition, the TF field coil is designed with enough field margin to contain the excess pressure of the alpha particles and electrons. There are two other basic features of the impurity control system; low-Z coatings are used on all exposed surfaces; and the plasma is operated so as to maximize the heat radiated from it and to minimize the transported heat. This approach, therefore, results in four techniques working in unison. These are: (a) low-Z coatings, (b) magnetic field margin, (c) enhanced radiation, and (d) limiter/vacuum system.

First-Wall Blanket

The technological and design aspects of various first-wall/blanket concepts have been considered in the selection of potentially viable designs for STARFIRE. The major emphasis has been placed on the development of a blanket design that is safe and environmentally acceptable. The primary guidelines established to meet these criteria are low tritium inventory in the blanket, minimal long-lived activation products and minimal stored chemical energy.

A comprehensive assessment of potential tritium breeding materials, coolants and structural materials was carried out. The scope of assessment included material properties, neutronics, compatibility and operating temperature limitations, generic safety aspects and tritium recovery methods. Based on the results of this assessment, the most viable combinations of breeder/coolant/structural material/neutron multiplier are given in Table 3. The reference blanket system is selected on the basis of perceived safety advantages associated with helium coolant and the solid breeding materials. Helium is selected only for the blanket coolant since water provides several advantages as the first-wall coolant. The ceramic breeding materials with their high temperature properties are most appropriate for the helium coolant. Lithium oxide is proposed as the breeding material since it is the only ceramic with potential for breeding without a neutron multiplier. The ferritic steels are compatible with both helium and water coolant and are selected for both first-wall and blanket structure. A low pressure helium purge stream over the Li_2O is used for tritium processing since direct contact of high pressure helium coolant and Li_2O is not acceptable.

Since the thermal-hydraulic characteristics of pressurized water are superior to those of helium in temperature-limited systems, an alternate first-wall/blanket materials option is proposed. The alternate concept utilizes D_2O coolant in both the first wall and the blanket. Lithium oxide is retained as the breeding material and ferritic steel as the first-wall/blanket structure.

Liquid lithium, which can be used as both coolant and breeder, provides a unique blanket option. However, the inherent safety of this system has been questioned and maintenance-related problems have been identified. An intermediate coolant loop may also be desirable. However, because the thermal-hydraulics, neutronics, radiation behavior, and design simplicity of this system are generally regarded as superior to other blanket materials options, this system is suggested as a backup. The backup design will be considered if irresolvable technical or engineering problems are identified in the early phase of the project for the reference and alternate designs. Selected vanadium alloys, which are proposed for the structural material, are believed to be the most resistant to radiation damage of the candidate structural materials,

Table 3. Selected First-Wall/Blanket Materials Options

Coolant	Coolant			Structure*		Neutron Multiplier
	FW	Blanket	Breeder	FW	Blanket	
Reference	D_2O	He	Li_2O	FS	FS	PbO^{**}
Alternate	D_2O	D_2O	Li_2O	FS	FS	PbO^{**}
Backup	Li	Li	Li	V	V	NR ^{***}

* Austenitic stainless steel is an alternate selection for the first-wall structure and both austenitic stainless steel and titanium alloys are possible alternatives for the blanket structure.

** If required.

*** Not required.

produce low long-term activation products, are compatible with lithium, and possess adequate elevated temperature mechanical properties. Long blanket lifetime may be attainable since the liquid lithium is not sensitive to radiation damage and tritium-release from the liquid also does not present a problem.

Two mechanical design concepts are being considered for the reference blanket. In the first concept, the module walls are pressurized to the coolant static pressure. The solid breeder is held in sealed tubes, arranged in a staggered rod bank pattern, which are cooled by cross-flowing the helium over them. In the second concept, the helium coolant flows inside tubes each of which is surrounded by solid breeder throughout the module. In both concepts, the tritium produced in the solid breeder is removed continuously through a helium purge gas system. The two concepts will be compared in the near future after further evaluation to determine which is superior from an overall reactor design standpoint.

Heat Transport System

The thermal energy deposited in the blanket and first wall is delivered via the heat transport system to the power conversion system where electricity is generated. The heat transport and power cycle systems consist of the primary helium (blanket coolant) loops, primary water (first wall and nonbreeding regions coolant) loops, auxiliary cooling loops, and the steam/power conversion system as shown schematically in Fig. 2.

With the water-cooled first wall and inboard blanket, approximately 40 percent of the thermal energy is in the form of 300°C heat. This energy is utilized in the power conversion system for feedwater heating and for steam generation. The remaining 60 percent of the thermal energy is transported via

the helium blanket coolant to the steam generators. The primary loops consist therefore of both helium and water circuits.

Because STARFIRE operates steady-state rather than in a pulsed mode, a thermal energy storage system is not required. Furthermore, no intermediate coolant loop is provided and the helium loop interfaces directly with the steam generator. The consequences of steam ingress into the helium loop were previously addressed for the high-temperature gas cooled reactor (HTGR). It was found that adequate safeguards against steam leakage can be provided. A calculation of permeation losses from the helium coolant through the steam generator was performed. It has been concluded that, due to the fact that the tritium is predominantly in the oxidized form, and due to the fairly low temperature, tritium permeation losses will be very small and an intermediate coolant loop is not required.

Vacuum Pumping System

The primary vacuum boundary of the STARFIRE vacuum system is at the inner wall of the shield. A pair of ~ 60 meter long toroidal limiters pass circumferentially around the outer edge of the plasma region, and deflect ions from the scrape-off zone into the adjacent slots in the first wall. A cross-sectional view of these slots is shown in Fig. 3. The slots are 60 meters long and 20 cm wide and penetrate the first wall and blanket. The location and configuration of the limiter is optimized to maximize the probability of a molecule entering the slot after striking the limiter. Each slot contains a step to reduce neutron streaming as shown in Fig. 3. In the molecular flow region this step has minimal effects on the conductance, the only consideration being the additional slot length required to provide the step. A large volume plenum exists between the outside of the

Fig. 2. Power conversion system schematic.

Fig. 3. Limiter/vacuum duct concept.

first wall blanket and the inside of the shield. Twenty-four 1.7 meter diameter, 1.5 meter long vacuum ducts penetrate the shield and provide access for liquid helium cryogenic pumps.

The liquid helium cryogenic pumps will be of the compound variety in which hydrogen and its isotopes are pumped by crycondensation on a liquid helium cooled panel and helium is crysorbed on a 4.2°K molecular sieve surfaces. The cryosorption surface on such a pump can become saturated with helium and will require regeneration after approximately eight hours of operation. Regeneration will be accomplished by stopping the liquid helium flow and allowing the sorption surface to warm whereupon the helium is released and pumped away by another pumping system.

Other Engineering Features

The reactor magnet system design includes twelve 11 Tesla TF coils. The EF coil system includes 4 segmented copper coils inside the TF coils for plasma stability control, but most of the EF coils are superconducting and located outside the TF coils for plasma equilibrium. The plasma startup, heating and current drive is accomplished by a lower hybrid rf system. The shield incorporates dielectric breaks to prevent toroidal current flow and permit the EF coils that are located inside the TF coil to have adequate response time for plasma stability control.

The reactor shield is designed for life-of-plant and is not removed for normal maintenance operations.

The shield concept is shown in Fig. 4. The reactor shield is water cooled and serves as the primary vacuum chamber. The shield protrudes between the TF coils at the top and bottom of the reactor to provide a vacuum duct to 24 cryosorption pumps. The vacuum duct opening through the shield is oversized to permit routing of coolant lines through the opening without significantly restricting pumping capacity. Each helium coolant line is shielded at the penetration through the shield by a bed of rods in a staggered pattern that limits neutron leakage. The shield contains poloidal dielectric breaks under every third TF coil. The dielectric joint, which is at the outside of the shield and completes the vacuum barrier, is exposed to about 10^{10} rads irradiation. The dielectric break will also be redundant and incorporate intermediate pumping. The shield pieces are designed to keep all seal welds between shield pieces in a single plane without welded corners, in order to improve reliability and simplify the installation operation. The shield doors utilize redundant Buna-N seals to provide the vacuum barrier. The seals are located near the outside of the shield; however, degradation by exposure to tritium and neutrons is expected to result in the need for seal replacement each time the door is removed during maintenance.

The rf wave guides form a near continuous toroidal ring at the base of the reactor. Approximately 100 MW of rf power is required to start and drive the plasma. The power density at the plasma is 8 MW/m². A mechanical rf waveguide connector is required to permit blanket removal.

Fig. 4. Shield concept for STARFIRE.

The blanket system is divided into 24 wedge shaped sectors to obtain a size that permits removal of sectors between TF coils. Each blanket sector incorporates an rf waveguide, two limiter segments, and two stepped toroidal slots 20 cm wide for vacuum pumping. Each sector is mounted on an air bearing pad to permit removal and replacement.

The reactor building is metal lined to provide a boundary against tritium release to the atmosphere. Access doors are provided to the interior through airlocks to permit ingress of reactor components. Continuous cleanup of the reactor building air environment is provided. The building is designed for a maximum overpressure of 3 psi which could result from an accident in which the liquid helium from one TF coil and the coolant from one first wall/blanket sector are released into the building.

Maintenance Approach

Availability goals have been established as 85% for the reactor and 75% for the complete plant including the reactor. These goals provide a basis for design of maintenance equipment. The maintenance scenario incorporates the current utility practice of shutting down annually for one month and a four month shutdown approximately every 5-10 years.

The design philosophy being followed is to minimize the radiation levels within the reactor building; to design all components for complete remote maintenance, and to identify contact maintenance operations where personnel can safely be used with significant economic savings.

The number of different maintenance operations planned in the reactor building are minimized by using a component "remove and replace" approach. This permits each maintenance action to be preplanned and designed for use with simple push, pull, etc., operations. This approach increases the confidence in the speed of maintenance operations and simplifies maintenance equipment design requirements. Once the damaged or end-of-life components are removed from the reactor they are transported to a hot cell where more time is available for checkout, repair or disposal.

Redundancy is planned for reactor auxiliary subsystems to permit continued operation of the plant until a scheduled maintenance period or until the component can be replaced in-service. More details on the maintenance plan for STARFIRE are given in another paper in this session.

Acknowledgements

The authors of this paper are the project managers of the various organizations involved in STARFIRE and thus represent many individual contributors. These individual contributors are listed in the various companion papers presented at this meeting.

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RESULTS OF SYSTEMS STUDIES FOR THE STARFIRE COMMERCIAL TOKAMAK*

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Summary

Extensive system and tradeoff studies were performed to support the selection process for the major parameters and design features of the STARFIRE commercial reactor. With a thermal power of 3800 MW, a neutron wall load of 3.5 MW/m² results in a relatively small-size reactor without imposing excessive requirements on the first-wall cooling capability, maximum toroidal-magnetic field, and frequency of structural material requirements. This moderately high-wall load requires that the first-wall coolant be liquid (water or lithium) and the lifetime of the structural material is >15 MW-yr/m². With moderate plasma elongation and beta the required maximum toroidal-field is ~11 T. STARFIRE is operated steady-state with no OH coil. The absence of an OH coil makes it possible to design the reactor with a low-aspect ratio (~2.5) and small major radius. However, higher aspect ratios (~3.5-4) are favored when the plasma current is driven with rf because the power required for the current drive, P_{rf}, is much larger at lower aspect ratio. Since P_{rf} increases at lower plasma temperature, the optimum design for STARFIRE requires operation with plasma temperatures higher than those normally selected for deuterium with OH-driven current.

1. Introduction

Previous fusion reactor design and systems studies have demonstrated the presence of a wide range of design parameters and a diversity of design concepts. A primary goal of the STARFIRE study¹ is to select, based on present knowledge, the most attractive set of design parameters and concepts that make tokamaks economically competitive and environmentally acceptable. In addition to experience gained from design and systems studies in the United States and worldwide, extensive tradeoff analyses were carried out to guide the selection process for STARFIRE. A primary tool for these tradeoff studies is a comprehensive systems computer program that is capable of predicting the performance characteristics and economics of the entire tokamak power plant. The ANL Systems Code² supplemented by the MDAC Code³ was utilized for this purpose.

The major design parameters that characterize a tokamak reactor are the reactor power, the neutron wall load, aspect ratio, plasma elongation, major radius, plasma beta, magnetic field, scrape-off region thickness, and blanket/shield thickness. A brief review of the considerations that were factored into selection of these major parameters is presented in this paper.

2. Reactor Power

It has been shown that tokamak reactors exhibit an economy of scale; i.e., larger power reactors have lower cost of energy. However, three considerations important to the utilities limit the desirable power rating of a plant. The first is the difficulty of raising the capital for larger power plants. The

second relates to the cost of reserve electric power capacity that the utility must provide to compensate for scheduled and unscheduled outages. The cost of reserve capacity increases with the size of the individual power plant. The third is the maximum capacity of a single turbine generator. Based on recommendations by the Utility Advisory Committee for STARFIRE, the most desirable power rating at present is in the range of 3000-4000 MW for thermal power and ~1250 MW for electrical power. Therefore, the fusion power for STARFIRE was selected as 3200 MW. This corresponds to a nominal thermal power of ~3800 MW, based on a 21-MeV per fusion reaction, and a net electric power of ~1150 MW. The recoverable thermal power will be modified by the addition of rf power for current drive and the loss of low-temperature heat such as that in the limiter system.

3. Neutron Wall Load and Structure Life

A key parameter that has a substantial impact on the physical size of the reactor is the neutron wall load. The neutron wall load, P_{nw}, is related to the fusion power, P_f, as

$$P_f = P_{nw} A_f \left(\frac{17.6}{14.1} \right) = P_p V,$$

where A_f is the surface area of the first wall, P_p is the average fusion power density in the plasma, and V is the plasma volume. For the same P_f, higher P_{nw} results in a smaller surface area, higher power density, smaller reactor volume, and potentially lower cost. This underlines the motivation for developing designs with higher wall loads. Figure 1 shows the relationship between the major radius and the neutron wall load for P_f = 3200 MW and plasma elongation of 1.6.

There are limitations, however, on both the ability to produce and the ability to use high-wall loads. The upper limits on the use of high-wall loads are dictated primarily by the first-wall cooling capability and the structure lifetime. Constraints such as the maximum operating temperature and thermal stresses place an upper bound on the allowable wall load. For typical structural materials such as ferritic steels in pulsed reactor systems, the neutron wall load should be limited to ~2.5 MW/m² for helium coolant. Higher wall loads are possible with water and lithium coolants. In general, the maximum allowable wall load is higher for reactors such as STARFIRE operating in a steady-state mode.

For a given fluence lifetime, the neutron wall load should be limited so that the frequency of structure replacement is not excessive. In order to limit the fractional increase in the cost of energy due to the plant downtime for replacement of the structural material to δ, the structure lifetime must be sufficiently long to satisfy the following inequality:⁴

$$t_w > \frac{t_d}{365 \delta},$$

*

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Fig. 1. Major radius as a function of neutron wall load at four values of the aspect ratio. Results based on: fusion power = 3200 MW and plasma elongation = 1.6.

where t_v is the structure lifetime in years and t_d is the total cumulative downtime in days for replacement of the structural material. For example, in order to limit the increase in the cost of energy to 10% (i.e. $\delta = 0.1$) when the downtime is 125 days the structure lifetime must be greater than 3.4 yr.

For a given structural material and a fluence lifetime, the loss of energy production resulting from choosing high P_{nw} and short t_v must be weighed against the economic gain realized by designing a small size reactor. Figure 2 shows the cost of energy as a function of the neutron wall load at two values of the integral neutron wall load, I_w , of 5 and 20 MW-yr/m² and at two different values for the total cumulative downtime, t_d , for replacement of the structural material. For $I_w = 5$ MW-yr/m² and downtime of 125 days the neutron wall load should be kept in the range of 2-2.5 MW/m². For $I_w \sim 20$ MW-yr/m² the cost of energy decreases significantly as the neutron wall load is increased from 1 to 2 MW/m². A smaller, but significant, saving in the cost of energy (COE) is realizable by increasing P_{nw} from 2 to 3 MW/m². A slight change in COE is noticeable in the range $P_{nw} \sim 3-4$ MW/m². The reasons for the modest increase in COE as P_{nw} is increased beyond ~ 4 MW/m² will become evident from discussions later in this paper.

It is clear from Fig. 2 that the achievable lifetime of the structural material has a significant impact on the cost of energy. By eliminating short plasma pulses and designing STARFIRE for steady-state operation, it is anticipated that a fluence lifetime of 20 MW-yr/m² or greater is obtainable with selected candidate structural materials. There are several important advantages for such long life: (a) the cost of energy is substantially reduced because of less frequent replacement and higher availability factor; and (b) when the frequency of replacement is substantially

Fig. 2. Cost of energy as a function of neutron wall load. I_w is the integral neutron wall load in MW-yr/m² and t_d is the total downtime in days for replacement of the structural material. Results are based on fusion power of 3200 MW, aspect ratio of 3.6, plasma elongation of 1.6, and $\epsilon_c = 0.067$.

reduced, the cost of energy becomes less sensitive to moderate variations in the downtime. This is quite important as it permits flexibility in the reactor design not available otherwise for designs driven primarily by the need for achieving very short downtime; (c) a less frequent replacement of the structural material results in a lower inventory of radioactive materials for which storage has to be provided. (It should be noted that even for the candidate structural materials with no long-term activation, adequate radioactive storage is necessary for two to five decades); and (d) the demand on material resources is less with longer life.

For a given fusion power, plasma elongation and aspect ratio, a higher wall load implies a higher plasma density, P_p . This varies as $P_p \sim \delta^2 n^4$. Since the plasma δ is limited by stability considerations, a higher P_{nw} is obtainable only by providing a higher magnetic field as shown in Fig. 3. This figure shows the maximum toroidal magnetic field, B_m , required as a function of aspect ratio and neutron wall load. The two different scales for B_m on the left and right of Fig. 3 correspond to two different plasma impurity control schemes as discussed in Sec. 5. For $P_{nw} > 2$ MW/m² there is ~ 1 T increase in the required B_m for every 1 MW/m² increase in P_{nw} . The economically attractive range for P_{nw} of 3-4 MW/m² requires a maximum toroidal field in the range 10-12 T which is considered acceptable for the STARFIRE design as discussed shortly.

Fig. 3. B_m as a function of A at several values of P_{nw} . The LHS scale is for $n_\alpha/n_{DT} = 0.01$ and the RHS scale is for $n_\alpha/n_{DT} = 0.15$. ($P_f = 3200$ MW, $B_t = 0.24$ A, $\Delta_{BS}^1 = 1.2$ m, $\Delta_v = 0.1$ m, $T = 8$ keV, $\kappa = 1.6$.)

In light of the above considerations, a neutron wall load of 3.5 MW/m² has been selected for STARFIRE. This moderately high-wall load appears to be a reasonable choice that makes it possible to design a relatively small-size reactor without excessive requirements on first-wall cooling capability, maximum toroidal magnetic field, and frequency of structural material replacement.

4. Plasma Beta

Previous systems studies⁵ have indicated significant economic benefits for operating at high-average plasma-toroidal beta, β_t . However, the maximum realizable β_t is limited by plasma stability considerations. Based on recent theoretical analysis, the relationship $\beta_t = 0.24/A$, where A is the aspect ratio, has been assumed for the STARFIRE study.

5. Toroidal Magnetic Field

Figure 3 shows the maximum toroidal field (B_m) required as a function of aspect ratio and neutron wall load for $P_f = 3200$ MW, $\kappa = 1.6$, $B_t = 0.24$ A, $\Delta_v = 0.1$ m, and $\Delta_{BS}^1 = 1.2$ m. The scale on the left side of Fig. 3 shows the required B_m if an efficient plasma impurity control mechanism (e.g. divertor) is provided such that n_α/n_{DT} is ~ 0.01 . The scale on the right shows the B_m required if less efficient plasma impurity control mechanism (e.g. limiter/vacuum system, see Ref. 1) is utilized such that n_α/n_{DT} is ~ 0.15 . Figure 4 provides more details about the variation of B_m with the impurity level for the case of $P_{nw} = 3.5$ MW/m², aspect ratio of 3.6 and $T_1 \sim T_e = 14$ keV. The rf power required to drive the plasma current decreases with T_e as discussed shortly. The dependence of B_m on T_e is shown in Fig. 5.

There exists at present considerable experience with NbTi superconductor. However, it has been shown that a magnetic field of ~ 10 T is the maximum practical limit for NbTi cooled to 4.2 K at atmospheric pressure. On the other hand, Nb₃Sn is capable of generating higher fields. Although present experience with Nb₃Sn is limited, the progress in the current technology development program indicates that the Nb₃Sn technology will be available in the STARFIRE time frame.

Fig. 4. Variation of the maximum magnetic field with the alpha-particle concentration, n_α/n_{DT} , and other impurity concentration, n_β/DT . ($P_f = 3200$ MW, $P_{nw} = 3.5$ MW/m², $\kappa = 1.6$, $A = 3.6$, $B_t = 0.067$, $T_1 \sim T_e = 14$ keV, $\Delta_{BS}^1 = 1.2$ m, $\Delta_v = 0.1$ m, $R = 7$ m.)

Fig. 5. Variation of the maximum magnetic field and plasma current with the average electron temperature, T_e . ($P_f = 3200$ MW, $\kappa = 1.6$, $A = 3.6$, $R = 7$ m, $B_t = 0.067$, $\Delta_{BS}^1 = 1.2$ m, $\Delta_v = 0.1$ m.)

Therefore, the 10-T limit of NbTi was not imposed as a constraint in the STARFIRE study; the required value of the toroidal field was determined from engineering and economic tradeoffs for the overall reactor system.

6. Plasma Elongation

The elongation ($\kappa = b/a$) of a D-shaped plasma has a significant impact on the plasma performance, the reactor design characteristics, and economics. In particular, at higher κ the achievable β_t is higher but the required EF coil system becomes more complex and costly. Based in previous work,^{4,6} a value of $\kappa = 1.6$ was selected for STARFIRE. This is believed to be nearly the upper limit on elongation if the important design goal of locating most of the EF coils external to the TF coils is to be achievable.

7. Power Requirements for Current Drive

Since STARFIRE will operate in a steady-state mode, a particularly important aspect of the design is the mechanism for plasma current drive. As discussed in Ref. 1, lower-hybrid rf is the selected option for current drive. Relativistic electron beams are also being examined as a backup option. An important impact of the current drive system on the reactor performance and economics is the electrical power requirements for this system. The dependence of the magnitude of this power for the rf system on key plasma and reactor design parameters is discussed below.

The theory of lower-hybrid-wave-drive currents^{7,8} indicates the ratio of rf power density to current density is proportional to the electron density. Thus, rf power is reduced by operating at higher plasma temperatures (lower densities for a fixed beta). However, as the plasma temperature increases above ~10 keV, the fusion power density starts to decrease and the ratio of rf power to fusion power is a minimum in the range 15-18 keV.^{8,9} In surveying reactor operation at various temperatures, the desire to minimize rf power calls for considering temperatures above 10 keV, but larger toroidal fields needed to keep the total fusion power at 3200 MW must also be acknowledged at these higher temperatures (see Fig. 5).

In determining the dependence of reactor design on aspect ratio (A), for a fixed total power and wall load and for $\beta_t = A^{-1}$, the total current increases going to lower aspect ratio, while the major radius and plasma density decrease. Based on the analytic formula⁸

$$P_{rf} = C R \bar{n}_e I,$$

where R is the major radius, $\bar{n}_e$ is the average electron density and I is the plasma current, the rf power required to drive reactors for a series of equilibria in the range $3 \leq A \leq 4$ was computed. The coefficient C is a function of the plasma profiles, spectral width, and degree of current penetration; for typical reactor parameters, and for both centrally peaked and surface current density profiles, the rf power increases by 30-50% if the aspect ratio is reduced from 4.0 to 3.0. Hence, large values of A are preferred from the rf power point of view.

8. Major Radius and Aspect Ratio

With the fusion power and neutron wall load selected, the surface area of the plasma is defined. For a given κ , the aspect ratio (A) or the major radius (R) should be selected in order to fully define the plasma geometry. As shown previously in Fig. 1, at $P_{nw} = 3.5 \text{ MW/m}^2$, the major radius increases from 6 m to ~7.3 m if A is increased from 2.6 to 4.0.

The size of the reactor building, length of piping, etc., are strongly affected by the size of the reactor, in particular, by the value of $R + a$, where a is the plasma minor radius. Notice that the variation of $R + a$ as the aspect ratio is changed is less than the variation in R alone. Despite the reduction in the capital cost of several items sensitive to size when R is smaller, economic optimization does not necessarily favor the selection of minimum R. This is true for both pulsed and steady-state reactors although for different reasons.

For pulsed reactors, the optimum size is significantly affected by the central core radius, r_v . For a given magnetic field for the OH coil, decreasing r_v reduces the available volt-seconds and shortens the burn time resulting in a lower reactor electrical output.

For steady-state reactors with no OH coil the problem of the central core radius disappears. In this case, the plasma current has to be driven by external means (e.g. rf or REB). If the electrical power requirement for the current driver were negligibly small, then the smallest aspect ratio should be chosen so that the major radius is minimum, provided of course that there is adequate space on the inner side of the torus to accommodate the TF coils and support cylinder. This is illustrated by the case $P_{rf} = 10 \text{ MW}$ in Fig. 6 which displays the cost of energy as a function of the aspect ratio.

Fig. 6. Cost of energy as a function of aspect ratio for a steady-state reactor in two cases for the electrical power, P_{rf} , required for the current-drive system: $P_{rf} = 10$ and $P_{rf} = 502-84 \text{ A}$ where A is the aspect ratio and P_{rf} is in MWe.

As discussed earlier in Sec. 7, the electrical power requirement, P_{rf} , for the rf current drive system appears to be relatively large and increases with lower aspect ratio. Figure 6 shows the dependence of the cost of energy on aspect ratio for the case where $P_{rf} = 502-84 \text{ A}$, with P_{rf} in MW electric. In this case the cost of energy decreases as A increases up to A ~ 4.

It is anticipated, however, that as a result of further work the rf power requirements will be reduced significantly. This would reduce the optimum aspect ratio somewhat below $A = 4$. Therefore, $A = 3.6$ was adopted for STARFIRE. This results in a major radius $R = 7$ m. The maximum magnetic field required is nearly minimum at $A = 3.6$ as evident from results in Fig. 3.

A major incentive for considering relativistic electron beams (REB) as a backup option for plasma current drive is the much lower electrical power requirements compared to those needed for the rf system. Present estimates indicate that REB electrical power requirements are in the range 10-20 MW compared to ~150-200 MW for the rf system. Assuming that the capital cost is roughly the same for both systems, the cost per unit power is ~8% lower with REB compared to that with the rf system. However, the present state-of-the-art for REB makes it difficult to quantitatively evaluate the technical and operational problems that might arise as the concept is investigated in more detail.

9. Inner Blanket/Shield Thickness

The thickness, Δ_{BS}^1 , of the blanket and shield on the inner side of the torus, or more precisely the distance in midplane from the plasma side of the first wall to the location of the maximum toroidal magnetic field, has a substantial impact on the reactor size, the required strength of the magnetic field and reactor economics. A comprehensive investigation of the optimum value for Δ_{BS}^1 has been carried out previously.¹⁰ The details of this previous work will not be reported here, but the results are briefly stated.

For a given P_{FC} , P_{FW} , R_2 , and major radius, the advantages of a smaller Δ_{BS}^1 are (1) lower maximum magnetic field; and (2) larger central core (OH) radius, r_v . In pulsed tokamaks, the impact of the value of Δ_{BS}^1 on r_v is large and becomes critical for low P_{FC} -high P_{FW} designs. In a steady-state tokamak with no OH solenoid, the primary incentive for reducing Δ_{BS}^1 is to reduce the maximum magnetic field required.

On the other hand, there are several penalties for making Δ_{BS}^1 too small. The increase in the radiation field when Δ_{BS}^1 is decreased results in (a) increase in the resistivity of the stabilizer material and a need for increasing the amount of the stabilizer to satisfy the cryogenic stability requirements; (b) a decrease in the critical current density of the superconductor necessitating the use of more superconductor; and (c) an increase in the heat-generation rate in the TF coil since the increase in the power requirements for the TF coil cryogenic system can be so large that the reactor net electrical power output is seriously reduced.

¹ For the reference design of STARFIRE, a value of $\Delta_{BS}^1 = 1.2$ m was selected based on careful considerations of the above tradeoffs. This value includes ~25% void to account for the vacuum gap in the TF coil and the use of helium coolant in a portion of the inner blanket, if necessary. The shield consists of a combination of tungsten, boron carbide, lead, and a structural material.

10. Outer Leg of the TF Coils

For a given major radius (R), plasma inner radius (a) and inner blanket/shield thickness (Δ_{BS}^0) the position of the inner leg of the TF coil is defined. In order to fully define the D-shape of the TF coil, the size of the vertical or horizontal bore must be selected. We will discuss this choice in terms of R_2 , the major radius of the midpoint of the outer leg of the TF coil. R_2 is the sum of the major radius, first-wall

minor radius, outer blanket/shield thickness (Δ_{BS}^0), clearance (Δ_C) in the midplane from the outer edge of the shield to the TF coil and half of the TF coil thickness. The choice of R_2 has a significant impact on many of the reactor characteristics and cost as discussed below.

With the reactor parameters defined in the previous sections (R , a , Δ_{BS}^0 , R_m , Δ_{BS}^1) the only remaining parameters that affect R_2 are Δ_{BS}^0 and Δ_C . The necessary value of Δ_{BS}^0 varies with blanket and shield material and coolant choices. As a goal, the materials in the shield should be chosen to have inherently low long-lived radioactivity even if they are less efficient in radiation attenuation. For typical material choices, the required Δ_{BS}^0 is ~1.3 m and ~1.8 m for blanket with water (or lithium) and helium coolants, respectively. The clearance from the outer edge of the shield to the TF coils is required for several engineering reasons; the most dominant of which is to accommodate the coolant manifolds. The required R_2 is ~12 m for water or lithium coolants compared to ~13 m necessary for helium coolants.

The economics analysis shows a substantial penalty for increasing R_2 . The cost of energy increases by ~3% for each additional meter increase in the value of R_2 beyond 12 m. The cause of this penalty is that the value of R_2 directly influences the size and weight of the TF coils and their support structure, the size of the reactor building, the length of the piping for the heat transport system, and the size of the externally located EF coils. For example, by increasing R_2 from 12 to 14 m, the ampere-turns and stored energy in the EF coils nearly double.

It should be noted that in order to keep the field ripple at the plasma to an acceptable level, the value of R_2 should be greater than a certain minimum. The minimum value for R_2 is larger for a smaller number of TF coils. A relatively small number, 12, of TF coils has been chosen for STARFIRE to enhance reactor maintainability. This makes extending the TF coils as a means of satisfying the field ripple criteria economically unattractive. Therefore, the alternative of an imposed field-ripple correction system (e.g. saddle coils or iron blocks) will be provided if necessary.

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THE IMPURITY CONTROL SYSTEM FOR THE STARFIRE COMMERCIAL FUSION REACTOR*

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Summary

The STARFIRE impurity control system is based on a low-Z coated limiter/vacuum system which collects and pumps about 30% of the particle flux at the edge of the plasma. This pumping efficiency, when combined with about a 1.5-T additional margin in the toroidal magnetic field, is sufficient to maintain the steady-state helium concentration to $\sim 10\%$ while permitting a tritium burnup fraction at $\sim 10\%$. In order to keep the heat load on the limiter to reasonable limits, about 80% of the alpha-particle energy is radiated to the first wall by injecting a very small amount of appropriate impurity ions.

Introduction

STARFIRE is a conceptual design study of a commercial tokamak fusion reactor.¹ The major parameters of STARFIRE are $R = 7$ m, $A = 3$, $k = 1.6$, $I_p = 11$ MA, and $\beta = 0.07$. The fusion power is 3200 MW and the net electrical power is ~ 1150 MWe. The reactor is operated in steady state using a lower-hybrid rf system to induce and maintain the plasma current. In keeping with the general goal of this project, the impurity control system has been designed so as to be as simple as possible. Another goal is to minimize the plant tritium inventory and this has also influenced the choice of the impurity control system. The reference impurity control system is based on a limiter/vacuum system together with low-Z coatings used on all exposed surfaces.

Limiter/Vacuum System

The function of the limiter/vacuum system is based on the general principles outlined in Ref. 2 which is basically to collect outward diffusing particles on the limiter and to pump some fraction of them. The limiter/vacuum system is shown conceptually in Fig. 1. Two "belt-type" limiters are used, located at about the top and bottom of the plasma chamber. The toroidal limiters are fairly small in the poloidal cross section; typically the total limiter area would be less than 5% of the first-wall area. Each limiter has an adjacent vacuum slot which also extends around the torus and penetrates the first wall and blanket. Each slot contains a step, as shown, to reduce neutron streaming. In the molecular flow region this step has minimal effects on the conductance, the only consideration being the additional slot length required to provide the step. A large volume plenum exists between the outside of the first-wall blanket and the inside of the shield. Twenty-four 1.7-m diameter, 1.5-m long vacuum ducts penetrate the shield and provide access for liquid helium cryogenic pumps which are used for all of the pumping. The liquid helium cryogenic pump will be of the compound variety in which hydrogen and its isotopes are pumped by cryocondensation on a liquid helium-cooled panel and helium is cryosorbed on a 4.2-K molecular sieve surface.

The limiter in Fig. 1 is shown as triangular for illustration only. While the limiter has not been

designed in detail, the actual shape should resemble that shown in Fig. 2. The key factor affecting the design of the limiter and its feasibility is the peak heat load on the limiter leading edge. The peak load depends on the scrape-off thickness, the heat transported to the limiter, and on the limiter shape. These have been analyzed as follows:

Scrape-off Thickness

The width of the scrape-off thickness can be computed by considering a single limiter, for simplicity, and using the coordinate system shown in Fig. 2. Consider a test particle that, on its orbit around the plasma, has just missed the limiter and is initially at $x = 0$, $y = 0$. In order to return to the vicinity of the limiter, the particle must make one revolution poloidally and q revolutions toroidally, where q is the safety factor. In doing so, the particle diffuses outward radially and so does not return to $x = 0$ but instead hits the limiter at a distance, $x = \delta$ from the front face. This distance which can be interpreted as an e-folding length for particle transport in the scrape-off zone can be computed by:

$$\delta = \sqrt{D_{\parallel} \tau}, \quad (1)$$

where $D_{\parallel}$ is the parallel diffusion coefficient characteristic of the edge region, and τ is the "flight" time for the particle to return to the limiter. For a path length, L , and using the ion thermal velocity, v is given by:

$$\tau = L/v_i, \quad (2)$$

where $v_i = \sqrt{KT_i/m_i}$. Using a Bohm diffusion coefficient for DT ions:

$$D_{\parallel} (\text{m}^2/\text{s}) = 0.0625 \frac{T_e (\text{eV})}{B(T)}. \quad (3)$$

The distance δ is found from Eqs. (1)-(3) to be:

$$\delta (\text{m}) = 3.176 \times 10^{-3} \sqrt{L/B} \times T_e^{1/4}. \quad (4)$$

For the system of two limiters each edge particle only has to go half as far and so $L = \pi R q$. For STARFIRE, $q = 3.6$, $L = 80$ m, and $B_0 = 5.6$ T in the plasma center which can be taken as an average B for use in Eq. (4). Based on the estimated heat flux (to be discussed), particle flux, and on the sheath model of Ref. 4 under the assumption of space-charge limited electron re-emission at the limiter, the computed electron-edge temperature is $T_e = 1500$ eV. These parameters give $\delta = 7.5$ cm from Eq. (4). This value of δ is for the particle flux in the scrapeoff. Although theoretically one might argue for a faster fall-off for the energy flux, recent measurements³ for PLT show nearly identical particle and energy fall-off rates and this has been assumed for this study.

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Fig. 1. Limiter/vacuum duct concept.

Heat Flow

The heat flow to the limiters can be estimated using two power balance equations, one for the core plasma and one for the edge region where the latter might be defined as perhaps the last 5 or 10% of the plasma volume. For the core region:

$$P_{\alpha} + P_{RF} = P_{TR}^{core} + P_{rad}^{core} \quad (5)$$

where P_{α} is the alpha-heating power, P_{RF} is the radio-frequency power used to drive the steady-state plasma current, P_{TR}^{core} is the transport power leaving the core, and P_{rad}^{core} is the power radiated from the core. Equation (5) must be satisfied for the plasma to be in overall thermal equilibrium. Our approach to satisfy Eq. (5) and at the same time to minimize the transport power, is to radiate as much power as possible, by injecting small amounts of high-Z material into the plasma. One of the noble gases would be a typical material for this purpose. This "enhanced radiation" mode not only makes the limiter design easier, by reducing the transport heat to it, but may also be more attractive than enhancing the energy diffusion by such means as ripple coils or the like.

The transport power from the core is given by:

$$P_{TR}^{core} = \frac{U_e}{\tau_e} + \frac{U_i}{\tau_i}$$

Fig. 2. Limiter geometry.

where U_e and U_i are the electron and ion kinetic energy contents in the plasma, respectively, and τ_e and τ_i are the electron and ion-energy containment times. It is, of course, not clear what the containment times will be in future tokamaks but the common prescription of using Alcator-type scaling for the electrons and one-third neoclassical scaling for the ions is a typical choice that might hopefully yield conservative estimates. For STARFIRE this gives:

$$\tau_e = 5 \times 10^{-21} N_e a^{-2} = 4 \text{ s},$$

$$\tau_i = \frac{\tau_{BAN}}{3} = \frac{9.8 \times 10^6 \sqrt{A T_e} I_p^2}{N_e Z_{eff}} \times \frac{1}{3} = 19 \text{ s},$$

where τ_{BAN} refers to the banana regime scaling law of Ref. 5. The plasma kinetic energies are: $U_e = 620 \text{ MJ}$ and $U_i = 553 \text{ MJ}$, which yield $P_{TR}^{core} = 184 \text{ MW}$.

Since $P_a = 640 \text{ MW}$ and $P_{RF} = 120 \text{ MW}$, the required radiation power, given by Eq. (5), is then $P_{RF}^{core} = 650 \text{ MW}$. A global analysis using typical peaked temperature and density profiles and using the coronal equilibrium radiation model of Ref. 6 indicates that this amount of radiation can be achieved with $\leq 0.1\%$ of xenon added to the plasma or a somewhat higher amount of krypton. This level of xenon seems acceptably small but the general question of profile control needs to be examined more closely.

The power balance for the edge region can be written as:

$$P_{TR}^{core} = P_{TR}^{lim} + (P_{cx} + P_{rad}^{edge} + P_{misc}^{edge}), \quad (6)$$

where P_{TR}^{core} is the input power to the edge, P_{TR}^{lim} is the transport power from the edge to the limiter, P_{cx} is the power associated with charge-exchange events, P_{rad}^{edge} is the edge radiation power, and the last term is a miscellaneous power which would include, for example, ionization of refluxing ions and impurities. Because of the terms in the brackets of Eq. (6), P_{TR}^{lim} will be, in general, some fraction γ less than one of P_{TR}^{core} . Using a typical value of $\gamma = 0.5$ then gives for the transport power to the limiters: $P_{TR}^{lim} = 92 \text{ MW}$. The power flux to each limiter is determined from this power and from the limiter geometry. Since there are four limiter surfaces, the heat flux, along the plane $y = \pm a$ is:

$$q(x) = \frac{P_{TR}^{lim} e^{-x/\delta}}{8\pi R a}, \quad \text{for } 0 < x,$$

for which $\delta = 0.075 \text{ m}$ gives:

$$q(x) = 7.0 e^{-x/\delta} (\text{MW/m}^2). \quad (7)$$

The heat flux on the surface of the limiters will be decreased substantially by the area factor associated with the sloped surfaces. However, the leading edges will receive the full heat flux and so the location of the leading edge is a critical choice in the limiter design. Using a criterion, for example, that the peak heat load not exceed 4 MW/m^2 , the leading edge must be displaced a distance of $b = a/2$ from the front face. In addition to the transport heat flux, the limiter will receive an X-ray radiation flux of $\sim 0.7 \text{ MW/m}^2$ from the plasma and a neutron flux of 3.5 MW/m^2 . Because of the high heat load, the leading edge will be constructed of a high thermal conductivity tube, such as AMZIRC Copper No. C15000. This alloy

(OHFC copper plus zirconium) is suggested because it has higher creep strength than OHFC alone. Analysis of a OHFC tube for the General Atomic TNS limiter⁷ under similar conditions has shown that a 1.25-cm diameter $\times$ 0.125 cm wall tube with 10 M/S cooling water resulted in a maximum metal temperature of 280°C . At the predicted stress and temperature the OHFC copper would elongate $\sim 1\%$ in 2 yr of operation. The rest of the limiter could be constructed of a double-wall sandwich with water coolant. The smaller heat load on the rest of the limiter will result in significantly lower temperatures which should avoid the creep limitations of copper. It should be noted that heat transfer and heat transport offer no extreme design hardship up to 8 MW/m^2 for medium-to-high thermal conductivity materials. The limiter lifetime with these materials is most dependent on radiation damage. Several design geometries are now being investigated along with the prospects of realistically considering the use of the molybdenum alloy TZM.

Further design and analysis efforts are required to quantify the limiter configuration and lifetime. Both the time required to replace a limiter and its lifetime are critical to its viability in power reactor operations. A life goal of 4 yr has been established for the limiters.

Limiter Performance

The number of particles removed by the limiter/vacuum system depends on three factors: (1) the fraction f of the particles from the plasma impinging on the back surfaces of the limiter, i.e. $b < x < c$; (2) the probability that an ion impinging and reflecting off the back surface (as a neutral) will enter the vacuum slot; and (3) the probability that an ion entering the vacuum slot will ultimately be pumped out.

The net removal probability depends on a combination of these factors. Considering the fact that particles can make multiple bounces between the vacuum slots and the limiter surface, the cumulative probability that a particle, diffusing from the plasma will be pumped is:

$$P = f \{ P_1 P_2 + P_1 (1 - P_2) P_1 P_2 + P_1^3 \times (1 - P_2) P_2 + \dots \},$$

which sums to:

$$P = \frac{f P_1 P_2}{1 - P_1 (1 - P_2)}. \quad (8)$$

In general, the vacuum slot, port conductance, and pump capture probability, and hence P_2 is fairly low, but P_1 can be made fairly high so that atoms can have many chances to enter the vacuum slots and be pumped. A problem common to all impurity control schemes is that it is easier to pump hydrogen than helium; just the opposite of the desired situation. Nevertheless, calculations⁸ using the vacuum system described earlier indicate that a value of $P_2 = 0.25$ for helium is obtainable. For a limiter design with $b = 0.5\delta$ and a width $c = 2\delta$, about 40% ($e^{-0.5}$) of the particle flux will hit the front faces and about 10% will be lost to the first wall, giving a fraction $f = 0.5$ going on the back surfaces. The probability P_1 depends on the limiter width to vacuum slot height ratio and depends on the scattering angle distribution for ions and neutrals impinging on the limiter surface. Preliminary calculations show that a value of $P_1 = 0.85$ is obtainable without using an excessively large vacuum slot. This combination of parameters results in an overall probability of $P = 0.3$. Alternately, this result means that the reflection coefficient for helium is $R_g = 0.7$.

Helium Concentration

The steady-state helium concentration in the plasma can be found by equating the alpha-particle production rate with the helium removal rate:

$$I_{\alpha} = \frac{N_{\alpha} V}{\tau_p} (1 - R_{\alpha}), \quad (9)$$

where I_{α} is the alpha-production rate, N_{α} is the average helium density in the plasma, V is the plasma volume, and τ_p is the particle containment time. Solving Eq. (9) for the fractional helium concentration gives:

$$\frac{N_{\alpha}}{N_{DT}} = \frac{I_{\alpha} \tau_p}{V N_{DT} (1 - R_{\alpha})}. \quad (10)$$

For the STARFIRE parameters:

$$I_{\alpha} = 1.1 \times 10^{21} \text{ s}^{-1}$$

$$\tau_p = 2 \text{ s}$$

$$V = 820 \text{ m}^3$$

$$N_{DT} = 1.0 \times 10^{20} \text{ m}^{-3}$$

and for $R_{\alpha} = 0.7$, the fractional concentration of helium is about 10%.

Sputtered Impurity Concentration

For the STARFIRE design all surfaces exposed to the plasma, i.e. the first wall and limiters, are coated with a low-Z material. The motivation for low-Z coatings have been discussed in several reactor studies, e.g. Ref. 5. Unless the plasma-edge temperature can be kept very low (<50 eV) there may be too much radiation from the sputtered bare metal (copper, molybdenum, etc.) to permit ignition.⁵ In addition, at even moderate edge temperatures there is the possibility of a runaway sputtering situation because the self-sputtering coefficients of the bare metal can be greater than unity.⁹ Low-Z coatings avoid these problems: the self-sputtering coefficients are considerably less than unity,³ for all edge temperatures; and secondly, the low-Z ions will be fully stripped over all or most of the plasma resulting in much less radiation than from the bare metal. A typical choice of coating material is beryllium but others such as BeO, Be₃C, C, and SiC are also candidates. (For convenience, the coating material will be referred to as beryllium.)

A key concern of the low-Z coating is the question of the erosion of the coating by sputtering. However, this concern may be ameliorated by the fact that beryllium (and other materials) tend to stick very well to themselves. If the sticking probability were unity, which it nearly is, then if as much beryllium impinged on the limiter, from the plasma, as was sputtered, there would be no net erosion of the limiter. The required beryllium concentration might occur, from charge-exchange sputtering of the coating on the first wall (which because of its large surface area could probably be recoated annually, or it might be necessary to add small amounts of beryllium directly to the plasma, e.g. with the fuel stream. The minimum concentration of beryllium under this assumption is given as follows:

The number of beryllium atoms sputtered from the coating per second is given by:

$$Y_z^- = \left(\frac{N_{DT} V}{\tau_p} \right) S_{DT} + \left(\frac{N_{\alpha} V}{\tau_p} \right) S_{\alpha} + \left(\frac{N_z V}{\tau_p} \right) S_z,$$

where the terms in parentheses represent the fluxes of DT, helium, and beryllium to the limiter and the S 's represent the sputtering coefficients. The net source of beryllium to the limiter is given by:

$$Y_z^+ = \left(\frac{N_z V}{\tau_p} \right) (1 - R_z),$$

where R_z is the reflection coefficient of beryllium. For no net erosion:

$$Y_z^+ = Y_z^-,$$

which therefore gives for the fractional steady-state beryllium concentration:

$$\frac{N_z}{N_{DT}} = \frac{S_{DT} + (N_{\alpha}/N_{DT}) S_{\alpha}}{1 - R_z - S_z}. \quad (11)$$

Using the following values of sputtering coefficients^{4,9} $S_{DT} = 0.0118$, $S_{\alpha} = 0.048$, $S_z = 0.271$; and for $R_z = 0.05$, and $N_{\alpha}/N_{DT} = 0.1$ gives $N_z/N_{DT} = 0.025$ as the required fractional concentration of beryllium in the plasma, for the no-erosion condition. A reference value of $N_z/N_{DT} = 0.04$, however, has been assumed for the design, to be somewhat conservative.

Conclusion

The impurity control system for STARFIRE consists of a limiter/vacuum system together with low-Z coatings. Although more work is needed on the detailed design and on the plasma engineering, the calculations outlined here indicate that the limiter concept is feasible for a tokamak reactor. The limiter/vacuum system also appears to be much simpler than any type of divertor, or alternate impurity control system.

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ASSESSMENT OF SOLID BREEDING BLANKET OPTIONS FOR COMMERCIAL TOKAMAK REACTORS*

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Summary

This study examines the materials and design implications regarding the use of solid breeding materials with respect to compatibility with structure and coolant, tritium processing, chemical and radiation stability and thermal-hydraulics. Four solid breeding materials considered, viz., Li_7Pb_2 , Li_2O , Li_2SiO_3 and LiAlO_2 , are representative of the metallic and ceramic compounds available. The major design problems regarding the use of solid breeding materials relate to the limited range of operating temperatures acceptable for tritium release and chemical stability or compatibility. The allowable ranges of breeder temperatures for which in-situ tritium recovery is potentially viable are evaluated for the candidate breeding materials.

Introduction

Lithium in some form appears to be the only material suitable for breeding tritium in a commercial fusion reactor. In addition to liquid lithium, several solid lithium compounds are potentially viable tritium breeding materials. Of the relatively large number of stable lithium compounds, three types of lithium compounds are the most promising. These include the metallic compound Li_7Pb_2 , lithium oxide (Li_2O), and the complex oxides Li_2SiO_3 and LiAlO_2 . Although other compounds have been proposed, the compounds listed here are the favored compounds of each type. For example, LiAl is similar to Li_7Pb_2 in many respects, however, Li_7Pb_2 provides superior breeding capability because of the neutron multiplication of the lead and is the favored compound of this type. Lithium oxide is unique because of its high lithium atom density and higher melting temperature compared to compounds such as Li_2Si and Li_2C_2 . The Li_2SiO_3 and LiAlO_2 generally provide a better combination of high temperature stability and high lithium atom density compared to other complex compounds.

The critical properties of these primary candidate breeding materials are summarized and their viability for tritium breeding in a commercial tokamak reactor blanket is assessed in this paper. Aspects considered include physical properties, neutronics properties, chemical stability and compatibility and tritium release characteristics.

Physical Properties

Key physical properties of the primary candidate breeding materials are summarized in Table 1. Also shown for comparison are the corresponding properties for liquid lithium. Of the solid breeding materials considered, Li_7Pb_2 has the lowest melting temperature, and hence, the lowest maximum operating temperature.

This compound also has a significantly higher density than the ceramics. The unique feature of Li_2O is the high lithium-atom density. However, as will be discussed in more detail later, the effective density of Li_2O and the other ceramics will be reduced because of tritium release requirements. Thermal conductivity is an important property, however, the data for these compounds are very limited. The value for Li_7Pb_2 is estimated from data for similar types of compounds. Figure 1 summarizes available data for Li_7Pb_2 at two densities. Since no data were found for Li_2SiO_3 and LiAlO_2 , data for other silicates and aluminates are shown.² In the present analyses, the thermal conductivities of the two complex oxides are assumed to be similar to that of Li_2O . As indicated by the results, the thermal conductivity is significantly affected by the density and, in general, is quite low for these ceramics.

Neutronics Properties

Two neutronics characteristics of the solid breeding materials have been evaluated, viz., breeding capability and long-lived activation products. Since the breeding capability or breeding ratio is sensitive to the specific design details, only a general assessment is presented here. The Li_7Pb_2 possesses very good breeding characteristics because of the neutron multiplication provided by the lead. The Li_2O has an inherently high lithium atom density; however, this advantage is somewhat reduced since the actual material used may contain ~20% porosity. Also, the effective breeding capability of the ceramics is impacted by the relatively high structural material fraction required because of the low thermal conductivity. It is generally concluded that Li_2SiO_3 and LiAlO_2 will require a neutron multiplier to obtain adequate breeding because of the parasitic absorption of the other elements present.

Of the candidate breeding materials, only ^{205}Pb from Li_7Pb_2 and ^{26}Al from LiAlO_2 present long-term activation problems unless significant amounts of impurities are present.

Chemical Stability and Compatibility

The chemical stability and compatibility of the solid compounds in the reactor environment are important considerations in the selection of the most viable options. Water and helium are the prime candidate coolants for use with solid breeding materials since lithium would most likely be used as the breeding material if it were selected as the coolant. The major consideration regarding a water-cooled blanket relates to the fact that all of the candidate breeding materials react to some degree with H_2O .

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Table 1. Summary of Properties of Candidate Breeding Materials

	Lithium	Li ₇ Pb ₂	Li ₂ O	Li ₂ SiO ₃	LiAlO ₂
<u>Physical and Chemical Properties</u>					
Melting point, °K	453	999	1970	1470	1880
Density, g/cm ³	0.51	4.39	2.01	2.52	2.60
Li atom density, g/cm ³	0.51	0.49	0.93	0.36	0.27
Thermal cond. W/m-°K	~ 50	~ 20	~ 3	~ 3	~ 3
Stability	Significant vapor pressure above 600°C	Reduced solidus if not stoichiometric	Significant vapor pressure above 1200°C (sublimes)	Reduced solidus (~ 1025) if not stoichiometric	
<u>Chemical Compatibility</u>					
Helium coolant	Oxidizes	Oxidizes	Impurity interactions		
Water coolant	Very reactive → H ₂	Very reactive → H ₂	Reacts → LiOH	Soluble - hot water	Soluble - hot water
Air (elevated temperature)	Very reactive	Very reactive	Reacts	Hygroscopic	Hygroscopic
<u>Neutronics</u>					
Breeding	Very good	Very good	Good	Requires neutron multiplier	Requires neutron multiplier
Activation	No long term	²⁰⁵ Pb	No long term	No long term	²⁶ Al
<u>Radiation Effects</u>					
Tritium breeding	Helium generation	Changes stoichiometry Lowers solidus temperature Helium production may cause swelling and T-retention	Forms LiOT	Changes stoichiometry Lower solidus temperature Phase instability	Changes stoichiometry Phase instability
Neutron damage	None	Swelling/densification (?) Reduced T-release Enhanced sintering	Swelling/densification (?) Reduced T-release Enhanced sintering	Swelling/densification (?) Reduced T-release Enhanced sintering	Swelling/densification (?) Reduced T-release Enhanced sintering

Fig. 1. Thermal conductivities of oxides with effective densities given in parentheses.

The following hydrolysis reactions will occur on contact of water with Li₇Pb₂ and Li₂O.

$$\Delta H_{\text{hyd}}(298^\circ\text{K}) = -47.9 \text{ Kcal/mole}$$

$$\Delta H_{\text{hyd}}(298^\circ\text{K}) = -15.4 \text{ Kcal/mole}$$

Both of these compounds react exothermically with water to form LiOH which has a melting temperature of 470°C. Since liquid LiOH is extremely corrosive to nearly all structural materials, the integrity of the structure would be severely degraded in the event of a reaction that forms LiOH. In addition, hydrogen formed in the reaction of H₂O with Li₇Pb₂ presents a safety problem which probably eliminates this system as a viable blanket option. The reaction of Li₂O with water is considerably less exothermic and, since hydrogen is not evolved, this reaction does not pose a major safety problem. As shown in Fig. 2, the free energy of the Li₂O reaction decreases to zero at ~ 1200°K, which indicates that the reaction should stop as the

Fig. 2. Thermodynamics of the reaction $\text{Li}_2\text{O (C)} + \text{H}_2\text{O (g)} \rightleftharpoons 2 \text{LiOH (C or l)}$

temperature is increased. The reactions of H_2O with Li_2SiO_3 and LiAlO_2 are even less severe. The Li_2SiO_3 will decompose in hot water and probably form some hydroxide. Although data are limited, LiAlO_2 appears to be more stable than the silicate.

Although helium is chemically inert to the candidate breeding materials, trace impurities typical of large heat-transport systems can react. The Li_7Fb_2 will react with minute levels of oxygen in helium to form an oxide film that will inhibit tritium release. Subsequent changes in stoichiometry of the breeding material caused by oxidation of the lithium will lower the melting temperature of the system.

Under certain conditions, trace amounts of water vapor in helium will react with Li_2O to form solid or liquid LiOH . Figure 3 is a plot of the calculated $\text{Li}_2\text{O/LiOH}$ phase equilibria as a function of $P_{\text{H}_2\text{O}}$ and temperature. The shaded region indicates the conditions anticipated in a fusion reactor environment. At the lower temperatures and higher water-vapor pressures, formation of LiOH is predicted. Further calculations indicate that the vapor pressure of LiOH over Li_2O is significant at temperatures of $> 1000^\circ\text{K}$ if water vapor is present. Experimental results in Table 2, which were reported by A. Johnson, et al.,³ tend to support the calculations. These results show significant weight losses of Li_2O when exposed to 1270°K to low water-vapor pressures. At water-vapor pressures required to maintain low tritium pressures, i.e., 0.01 to 1 Pa, the weight losses are excessive and direct contact of helium coolant with Li_2O is not considered acceptable at the higher temperatures. Although Li_2SiO_3 and LiAlO_2 are less susceptible to this reaction, the present data base is insufficient for a reliable evaluation of the problem.

The data are also very limited on the compatibility of the candidate solid breeding materials with potential structure or cladding materials. Recent

Fig. 3. Calculated $\text{Li}_2\text{O/LiOH}$ phase equilibria as a function of $P_{\text{H}_2\text{O}}$ and temperature.

Table 2. Weight Loss of Li_2O Exposed at 1000°C to Low Water-Vapor Pressures

$P_{\text{H}_2\text{O}}$ Pa	Exposure Time hours	Weight Loss %
Evacuation	2.5	3.3
10^{-5}	2	3.2
10^{-2}	2.5	6.4
10^0	2.0	12.4
10^2	0.5	18.4

data⁴ on the compatibility of Li_2O with Fe-Ni-Cr alloys are given in Fig. 4. These corrosion rates are much higher than typical corrosion rates of stainless steel in reactor grade sodium and are probably excessive at temperatures above $\sim 500^\circ\text{C}$. Another corrosion problem of concern relates to the formation of LiOH as discussed above or LiOT from the braiding reaction. The LiOH is highly corrosive to most structural materials and could present problems in the heat transport or tritium processing circuits.

Tritium Release from Solid Breeding Materials

For solid breeders, tritium most likely will be recovered as T_2O from a helium purge stream by sorption on molecular sieves. The tritium inventory in the solid breeder will be determined by both diffusion rates and thermodynamic constraints.

Fig. 4. Corrosion of structural alloys exposed to Li_2O for 100 h.

Diffusion Considerations

In a fusion blanket, the tritium concentration in the solid breeder will build up until the rate of diffusion out of the solid is equal to the tritium generation rate, and steady state is achieved. For infinite slab geometry with zero surface concentration, the steady state relationship is described by

$$I = \frac{1}{3} \bar{i} \frac{r^2}{D} \quad (1)$$

where I = tritium inventory (g),
 $\bar{i}$ = tritium generation rate (g/s),
 r = half-thickness of the slab, and
 D = diffusivity.

In typical experiments,⁵ when diffusion rates are measured by first irradiating the sample and then heating to remove tritium, the diffusion kinetics are not steady state. The steady state diffusivity, which will give a conservative estimate of the tritium inventory, is given by

$$D = \frac{r^2}{15\tau} \quad (2)$$

where τ = mean residence time for tritium in solid, i.e., the time to extract 87.4% of the tritium.

In the experiments, τ can be readily measured; but, it is much more difficult to determine r , and therefore, D . However, assuming the r in the experiments is representative of material in a reactor blanket, equations (1) and (2) can be combined to eliminate r and D . The tritium inventory is then given by

$$I = 5 \bar{i} \tau \quad (3)$$

For a 1000 MWth reactor (steady state) at 20 MeV/fusion and a breeding ratio of 1.20, the tritium generation rate ($\bar{i}$) is 1.87×10^{-3} g/s. If τ is expressed in hours, then

$$I(\text{g}) = 33.6 \cdot \tau(\text{h}) \quad (4)$$

Using this relationship, the calculated tritium inventories for candidate solid breeders are shown in Fig. 5. In view of the very strong temperature dependence and the fact that the inventory is extremely sensitive to the diffusion path length r ($I \propto r^2$), it is not surprising that some analyses conclude that solids will have very low tritium inventories, while others conclude that solids may have very high inventories.

Fig. 5. Predicted tritium inventory in candidate solid breeding materials (20 mesh) as a function of temperature after low-fluence irradiation.

The design solution to this dilemma is proper temperature control. The minimum temperature must be sufficiently high that tritium will diffuse out at an adequate rate, and the maximum temperature must not permit excessive agglomeration and sintering, which could increase the diffusion path length and thereby reduce diffusion rates. It is suggested that, as a design criterion, the inventory of any 5% of the blanket not exceed 500 g per GWth. Therefore, on the basis of thermal diffusion, not more than 5% of the blanket can be below the minimum temperatures indicated in Fig. 5. These minimum temperatures are given in Table 3 for the same breeding materials. Increases in particle size caused by thermal sintering will tend to inhibit tritium release. Estimated temperatures below which thermal sintering will not be important are given in Table 3. Wiswall⁵ performed prolonged heat treatments on several materials at 80- to 90% of the absolute melting temperature and observed only limited structural changes.

It is expected that the radiation environment may have some effect on the tritium release characteristics. Radiation-induced trapping of solutes has been observed in irradiated materials. Wiswall observed that tritium release rates from certain oxides decreased with increasing fluences (up to 10^{20} n/cm²). Figure 6 shows the effect of neutron fluence on the tritium release characteristics of LiAlO_2 at 650°C. At the higher fluences the fractional release under similar conditions is reduced by about an order of magnitude. From Fig. 5, a decrease in inventory by an order of magnitude translates to about a 50°C increase in temperature. Therefore, minimum release temperatures under irradiation are increased as indicated in Table 3. The effects of radiation-induced

Table 3. Temperature Limits (°C) for Solid Breeders

Breeder	m.p. °C	Thermal Effects		Radiation Effects	
		T _{min} ¹	T _{max} ²	T _{min} ³	T _{max} ⁴
Li ₂ O	1700 ⁵	410	1000	460	910
LiAlO ₂	1610	500	1000	530	850
Li ₂ SiO ₃	1200 ⁶	420	900	470	610
Li ₂ Si	760	480	550	530	420
LiAl	700	300	500	350	380
Li ₂ Pb ₂	726	320	530	370	390

¹ Diffusion limited.

² Thermal sintering.

³ Radiation-induced trapping (factor of 10 degradation in tritium release estimated).

⁴ Radiation enhanced sintering (0.6 T_m for oxides and 0.67 T_m for other compounds).

⁵ Li₂O reportedly sublimates at temperatures below the melting point.

⁶ Li₂SiO₃ is a line compound with a solidus temperature of ~ 1030°C for slightly hypo- or hyperstoichiometric compositions.

Fig. 6. Effect of neutron fluence on the tritium release characteristics of LiAlO₂ at 650°C.

sintering are also considered. The maximum allowable temperature for ceramics is estimated from results on oxide fuels in fission reactors, wherein radiation-enhanced sintering occurs at ~ 0.6 of the absolute melting temperature. For the metallic compounds, the radiation sintering temperature is assumed to be 2/3 of the absolute melting temperature.

Provided the breeding materials are maintained within the listed temperature constraints, the estimated tritium holdup by diffusion will be less than 1 kg/GWth. It is evident that the ceramic breeding materials provide the largest operating temperature

ranges. Of the three ceramics, Li₂O appears to be the most favorable.

Thermodynamic Considerations

The tritium release and recovery characteristics are also affected by thermodynamic considerations. Since experimental results on the phase equilibria are limited, the following calculational approach has been used to evaluate this effect. The vapor phase composition above Li₂O was calculated using the available thermochemical data.⁶ The tritium is assumed to be present as LiOT in ideal solid solution in Li₂O. The tritium content of the Li₂O was taken to be 1.0 wppm (mole fraction = 1 × 10⁻⁵) and the mole fraction of H (as LiOH) in Li₂O was assumed to be 1 × 10⁻³. The relevant equilibria are:

Roult's Law was used to estimate LiOH and LiOT vapor pressures. Accordingly, the calculated vapor phase compositions are shown in Fig. 7. At 800°K, the HTO pressure is 10⁻⁶ torr and the T₂O pressure is 10⁻⁸ torr. From mass transfer considerations, a T₂O (or HTO) pressure of about 10⁻³ torr is required in the purge stream, in order to remove the tritium at reasonable purge flow rates. It appears that the tritium concentration in the solid Li₂O would have to be increased to ~ 300 wppm in order to effectively recover tritium by a purge stream. This would correspond to a tritium inventory of about 40 kg per GWth. The compounds LiAlO₂ and Li₂SiO₃ are expected to have significantly higher T₂O vapor pressures, and hence, lower inventories. Additional data are required to reliably determine these values.

Fig. 7. Vapor phase species in equilibrium with Li₂O.

Conclusions

The major design problems regarding the use of solid breeding materials in fusion reactor blankets relate to the capability to remove tritium from the solid. The temperature window for tritium release appears to be acceptable only for the ceramics. Of the ceramics, Li_2O has the largest temperature range. However, thermodynamic calculations indicate that the tritium concentration in Li_2O at anticipated T_2O pressures will be greater than 10 kg/GWth. The corresponding T_2O pressures over Li_2SiO_3 and LiAlO_2 are higher, and therefore, lower tritium concentrations in these solids may be attainable. Additional thermodynamic data are required to more accurately determine the tritium holdup in solid breeders.

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FIRST WALL AND BLANKET DESIGN FOR THE STARFIRE COMMERCIAL TOKAMAK POWER REACTOR

by

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The first wall and blanket design concepts being evaluated for the STARFIRE commercial tokamak reactor study are presented. The two concepts represent different approaches to the mechanical design of a tritium breeding blanket using the reference materials options. Each concept has a separate ferritic steel first wall cooled by heavy water (D_2O), and a ferritic steel blanket with solid lithium oxide breeder cooled by helium. A separate helium purge system is used in both concepts to extract tritium. The two concepts are compared and relative advantages and disadvantages for each are discussed.

Introduction

First wall and blanket mechanical design options are being developed for the selected reference materials option. The rationale for the materials selection is the subject of a separate paper¹ at this Symposium and will not be discussed here. The table below briefly summarizes the selected materials.

First Wall

Structure: Ferritic steel (Alternate: Austenitic stainless steel)
Coolant: D_2O , heavy water

Blanket

Structure: Ferritic steel (Alternates: Titanium alloy or austenitic stainless steel)
Coolant: Helium
Breeder: Lithium oxide

Two different mechanical design concepts for the reference materials option are presently being developed and further analyzed to assure that design requirements are satisfied in the areas of thermal hydraulics, power cycle efficiency, tritium breeding, tritium recovery, fabricability, and structural integrity. The concepts will also be compared to determine how well they satisfy these important STARFIRE design objectives:

- o minimize probability/consequences of accidental contact of incompatible materials;
- o maximize reliability of system and components, to increase availability;
- o maximize maintainability by minimizing need for, and time required for, standard maintenance operations;
- o minimize inner wall/blanket/shield thickness (δ_{NS}^1), to reduce reactor size and maximum magnetic field; and

- o minimize outer wall/blanket/shield thickness, to minimize TF and EF coil size.

The final choice for the mechanical design concept will be made following these analyses and comparisons.

Selection of the mechanical design concept for the helium cooled blanket is strongly dependent on the individual and combined characteristics of the selected structural material, coolant and breeder. Of critical importance is assuring satisfactory adherence to minimum and maximum temperature constraints placed on the solid breeders¹. Minimizing the blanket thickness, manifold and header size are also of major importance, in order to minimize required reactor size.

Both the design concepts being evaluated use the approach of individual blanket modules of similar shape and size which are built up into wedge-shaped first wall and blanket circumferential sectors. This approach, discussed in another paper at this Symposium², has several advantages. It permits more accurate tailoring of the blanket to match neutron wall load values at specific positions around the plasma cross section. Accommodation of local discontinuities such as vacuum ports and rf ducts is simplified. Replacement of individual modules can be performed in the hot cell, while the reactor continues normal operation following installation of a replacement sector. The reactor is thus shut down only for the time needed to remove the sector containing a faulty first wall or blanket component and to install the replacement sector.

A water-cooled first wall, mechanically and structurally separate from the blanket, was selected for both the blanket design concepts. The separate first wall concept permits simple replacement (in the hot cell) of a failed first wall panel without also requiring replacement of the more expensive blanket module behind it. The use of water coolant for the first wall in the reference design accommodates the selected neutron wall load (P_{nw}) value of 3.5 MW/m^2 with negligible pumping losses. The two additional disconnect/reconnect operations for the first wall water coolant headers, necessary for removal of a blanket sector, are not considered a major addition to the time required for that operation.

Discussion of Design Concepts

Two mechanical design concepts are being considered for the blanket. In the first concept, the module walls are pressurized to the coolant static pressure. The solid breeder is contained in sealed tubes, arranged in a staggered rod bank pattern, which are cooled by cross-flowing the helium over them. In the second concept, the helium coolant flows inside tubes each of which is surrounded throughout the module by the solid breeder. This section discusses the two concepts, and the water-cooled first wall common to both.

Pressurized Module Concept

The pressurized blanket module has essentially the form of a slightly tapered parallelepiped, as shown in Fig. 1. Four of the sides are aligned to follow the two planes formed by the wedge-shaped blanket sector and two planes extending radially, from approximately the plasma center. The front face is semicircular and the back face is a shallow semiellipsoid. Module width (distance along sector chord) is variable to match sector width at any point. Module depth and thermal hydraulic parameters (braeder zone details and coolant mass flowrate) may be adjusted to match the actual neutron wall load for any position in the blanket sector.

Each module has one coolant inlet and two coolant outlets at the rear face, which attach to manifolds wrapping around the blanket sector. The manifolds connect all modules in the sector together and run to vertically aligned headers (large diameter pipes) at the top and bottom of the sector. The headers are mechanically attached to inlet and outlet feed pipes which remain in the reactor when a sector is removed.

Helium coolant enters the module through the inlet at the rear face and flows radially toward the front face (nose) through plenums formed by the double walled module sides. The helium enters the semicircular nose area where the flow is divided into channels with flow direction alternating between adjacent

Figure 1. Helium-Cooled Blanket - Pressurized Module Concept.

channels. The helium then enters a plenum region through ports in the nose inner wall and flows radially through the breeding region. The flow then enters a second plenum region and exits to the outlet manifolds.

The breeding zone of the pressurized module contains circular breeder tubes, which extend across the full module width (toroidal direction). The tubes are in rows, with successive rows staggered to produce an equilateral pitch. Each tube consists of a structural shell, Li_2O breeder, and end fittings for the purge gas system. The shell is sized for a differential pressure (collapse) equal to the coolant static pressure. The compacted lithium oxide breeder inside the shell has a small central axial hole. The purge gas is introduced into the cell through the fitting at one end, flows through the breeder center hole, and exits through the fitting at the other end. The helium (at ~ 1 atm pressure) permeates the breeder, and collects the tritium in the form of T_2O or LiOT with oxygen supplied through a low partial pressure of oxygen in the helium.

The breeder tube diameter varies through the depth of the blanket, from ~ 1.8 cm near the nose to ~ 4.0 cm at the rear, in order to maintain breeder temperatures within minimum and maximum temperature limits¹ as volumetric heating rates decrease with depth through the blanket. The breeder temperatures are sensitive to changes in the heat conductance value (assumed to be $\sim U = 2000 \text{ W/m}^2\text{-K}$) for the structure/breeder interface. Further work is needed to optimize breeder tube design details to assure that required values of conductance can be reliably maintained throughout the blanket design life.

Heat generated within the breeder is conducted through the breeder to the tube structure surface. The helium coolant, flowing across the rows of tubes, removes this heat by convection. Thermal-hydraulic characteristics of the coolant flow across the breeder tubes are based on well-established empirical relationships for cross-flow tube bank heat exchangers. Spacing of the tubes is based on an equilateral pattern for adjacent tubes with $1.15 D$ spacing between tube centers (i.e., the gap between any two adjacent tubes is equal to 0.15 times the tube diameter). This permits a relatively high packing fraction for the tubes. Total pumping power required for the breeding zone only is ~ 0.5 MW (pump work) for the reactor, which is negligible in comparison to the power required for the rest of the helium coolant system. Total pumping power for all 24 blanket sectors is estimated to be ~ 50 MW (pump work), or $\sim 1.3\%$ of total reactor thermal power with nearly all the losses occurring in the manifolds. This value includes all pressure losses occurring in the sectors between the inlet and outlet header dis- connects.

The pressurized module makes efficient use of structural material to keep the structural volume fraction as low as possible in order to enhance tritium breeding. The module sides are designed with outer and inner walls joined by ribs. This design is structurally more efficient than a single pressurized wall. The use of two walls also provides a flow channel around the blanket perimeter through which relatively cool helium from the inlet manifold flows radially toward the blanket nose, keeping all the pressurized outer wall at $\sim 300^\circ\text{C}$ or less. The inner walls experience virtually no differential pressure. Tie rods span the long width direction of the module in a square pattern with ~ 10 cm spacing. These rods react the pressure loads applied to opposite walls, further reducing the required wall thickness. In the short width

direction, a combination of tie rods and thin bulkheads is used to react pressure loads on opposite walls. The bulkheads also provide support for the long breeding cells by holding them in close-fitting holes to prevent any significant deflection of breeder tubes between bulkheads. The possibility of using the breeder tubes as structural members, to eliminate tie rods in the long width (tube axial) direction, will be investigated.

Coolant inlet temperature and outlet temperature for the pressurized module concept are 250°C and 500°C , respectively. A key feature of this concept is that it permits the maximum coolant temperature to be approximately equal to the maximum temperature of the module wall structure. This is important because it maximizes the power cycle efficiency for the helium coolant. The only highly pressurized structure in the module with a temperature nearly equal to coolant outlet temperature is the outlet manifolds at the rear of the blanket. All other structure in the module, with the exception of the breeder tubes near the rear of the blanket, will experience temperatures no greater than the coolant outlet temperature. The breeder tubes near the rear of the blanket are estimated to have a structural temperature approximately 30°C higher than the local coolant, or $\sim 530^\circ\text{C}$. Most of the structure will be at considerably lower temperatures, and will be sized for loads resulting from thermal gradients and reactions to pressure loads.

The pressurized module concept for the reference blanket has been analyzed to determine a preliminary estimate for volume fractions, using both ferritic steel alloy and titanium alloy Ti-6242S as structure. The table below shows the results.

	<u>Ferritic</u>	<u>Ti-6242S</u>
X V Structure	14.9	11.1
X V Li_2O^*	57.2	57.2
X V Void/He	27.9	31.7

* Volume required by breeder without accounting for voids.

The lower structural fraction for the Ti-6242S results from the higher allowable stresses compared to the ferritic steel. From the standpoint of design and fabricability the titanium alloy seems superior to the ferritic steel because (a) its ductile-to-brittle transition is significantly less than room temperature, (b) no preweld heat treatment is required, and (c) post-weld heat treatment (other than annealing) is not required.

Pressurized Tube Concept

In the second concept for the helium cooled blanket (Fig. 2), helium coolant is contained in tubes which are surrounded by the breeder (essentially the reverse of the concept previously described). The tubes are each in a U-shape. The tube end regions are aligned radially along the module ends, with the center region running lengthwise through the module. The ends of the tubes are manifolded so that each module has only one inlet and one outlet fitting for the coolant. These fittings are located on the flat back face of the module. The purge gas system consists of a separate network of porous tubes, each located at the approximate center of any four adjacent tubes forming a square pattern. The tubes are connected to a separate header system at the module ends which connect to inlet and outlet fittings at the rear face of the blanket.

BLANKET CROSS SECTION

ISOMETRIC OF BLANKET MODULE

Figure 2. Helium-Cooled Blanket - Pressurized Tube Concept.

The outer wall of the module is sized to contain the ~ 1 atm static pressure of the helium purge gas. The wall is actively cooled by helium; construction is similar to that of the water-cooled first wall. Active cooling is considered necessary because the breeder conductivity is too low to adequately conduct heat away from the outer wall to maintain a satisfactory maximum structural temperature.

The module walls enclose the breeder which fills the module and surrounds the tubing network. Tube spacing, inside diameter, and length vary through the blanket depth to achieve equal coolant temperature changes and equal coolant pressure drops for all the tubes. Preliminary thermal-hydraulics analysis indicates that tube inside diameters should vary from ~ 1.2 cm at the front to ~ 0.8 cm at the rear of the

blanket. Approximately 170 tubes are required for each module. A narrow gap between the breeder and the tube outer surface has been conservatively assumed for heat transfer calculations. Coolant inlet and outlet temperatures have been assumed to be 225° and 475°C , respectively. Outlet temperature was reduced from the 500°C assumed for the previous concept to keep the maximum tube temperature to $\sim 530^\circ\text{C}$. Pressure drops for the blanket have been estimated to be 16 to 18 psi at the 750 psig coolant static pressure. This results in pumping power losses of $\sim 2\%$ of reactor thermal power.

Inner Blanket Concept

For both concepts, the inner blanket is presently assumed to be water-cooled and non-breeding, to minimize ΔT_{BS}^1 . However, should neutronics analysis show that additional breeding on the inboard wall is needed for either concept, a modified breeding blanket design would be adopted for that region. The design would essentially consist of the first 30 to 40 cm depth of the standard breeding blanket concept. Blanket depth and manifolding diameter would be adjusted in this region to maintain the desired ΔT_{BS}^1 and still achieve the necessary additional breeding and adequate heat recovery.

First Wall Concept

The first wall concept for the reference design is essentially a water-cooled flat panel. The basic concept is common to both the reference and alternate blanket concepts. Details of the design will be developed later in the study. For the present, the mechanical design concept used is that adopted previously in the ANL-EPR study³. In this design, cooling is accomplished by circulating pressurized water in a network of channels that line the plasma side of the panel. The channels are formed by bonding preformed steel sheet to a second, flat steel sheet. Each panel section spans several blanket modules, and has one inlet and one outlet coolant line which connect to manifolds at the rear of the blanket module. These manifolds connect all first wall panels and the water-cooled inboard blanket within a single blanket sector. The manifolds terminate in headers in the vicinity of the helium blanket coolant headers, and are in turn attached mechanically to inlet and outlet feed pipes which remain in the reactor when the sector is removed.

Comparison of Blanket Concepts

Figure 3 presents a comparison of the two blanket concepts previously discussed in terms of design requirements and design objectives. The pressurized tube concept has the relative advantage of structural simplicity compared to the pressurized module concept. The high pressure coolant is contained by tubes rather than by the irregular shape formed by the module walls. The circular shape is structurally more efficient for containing the high pressure. The reduction in the number of stress concentration regions could be expected to result in somewhat greater assurance against the occurrence of coolant leaks. Fabrication would be simpler for the pressurized tube concept, although the development of a method to fill the module with breeder to the desired 80% of theoretical density is a concern.

However, the pressurized tube concept has several inherent relative disadvantages. First, the maximum temperature of each tube will be ~ 40 - 50°C above the coolant outlet temperature. The wall thickness of the

CATEGORY	PRESSURIZED MODULE CONCEPT (P-MOD)	PRESSURIZED TUBE CONCEPT (P-TUBE)	COMMENT
DESIGN REQUIREMENTS			
o THERMAL-HYDRAULICS			
- COOLANT OUTLET TEMP.	/		~ 40-50°C HIGHER FOR P-MOD
- PUMPING POWER	/		~ 1-2% MW(th) IN BLANKET FOR P-TUBE
- STRUCTURE TEMPERATURE	/		P-MOD WALLS COOLED BY HELIUM TO <300°C
o POWER CYCLE EFFICIENCY	/		~ 2% HIGHER FOR P-MOD
o TRITIUM BREEDING	- - -	/	LOWER VOID FRACTION FOR P-TUBE
o TRITIUM RECOVERY	- - -	- - -	ADEQUATE TRITIUM RELEASE FOR BOTH CONCEPTS
o STRUCTURAL INTEGRITY		/	SIMPLICITY OF PRESSURIZED TUBES PARTLY OFF-SET BY NEED FOR ACTIVELY-COOLED MODULE WALL
o FABRICABILITY		/	SAME COMMENT AS ABOVE; FILLING MODULE WITH BREEDER WILL REQUIRE DEVELOPMENT PROGRAM
DESIGN OBJECTIVES			
o MINIMIZE PROBABILITY/CONSEQUENCES OF ACCIDENT	- - -	- - -	NO SIGNIFICANT DIFFERENCE
o MAXIMIZE RELIABILITY		/	SIMPLICITY OF PRESSURIZED TUBES PARTLY OFF-SET BY NEED FOR ACTIVELY-COOLED MODULE WALL
o MAXIMIZE MAINTAINABILITY	- - -	- - -	NO SIGNIFICANT DIFFERENCE
o MINIMIZE FIRST WALL/BLANKET THICKNESS		/	PRESSURIZED TUBE CONCEPT REDUCES TOTAL VOID FRACTION IN BLANKET

/ = RELATIVE
ADVANTAGE

Figure 3. Comparison of Helium-Cooled Blanket Concepts.

tubes must therefore be sized to the maximum allowable structure temperature and the coolant outlet temperature determined accordingly. For the pressurized module concept, maximum structure temperature of the pressurized outer wall is ~ 500°C which occurs in the outlet tube at the rear of the blanket. Given the inherent differences between maximum coolant temperature and maximum structure temperature for the two concepts, for any arbitrary maximum structure temperature limit the pressurized module concept should permit coolant outlet temperatures ~ 40°C higher than for the pressurized tube concept. Second, for the pressurized tube concept the pumping power required for the blankets and manifolds together is estimated to be ~ 3.0% of reactor thermal power; this compares to ~ 1.3% for the pressurized module concept. The difference amounts to ~ 66 MW (work), and would significantly reduce net efficiency for the power cycle. The requirement for an actively cooled module wall is a significant added complexity and may possibly result in a significant reduction in blanket breeding capability. This requirement will be further investigated.

Further evaluation of the two concepts will be performed in the areas of structural analysis, thermal-hydraulics, and neutronics to aid in the selection of the design concept to be adopted for the STARFIRE reactor and optimized as an integral part of the reactor design.

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TRITIUM HANDLING AND VACUUM CONSIDERATIONS FOR THE STARFIRE COMMERCIAL TOKAMAK REACTOR*

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Abstract

Tritium processing and vacuum pumping requirements were analyzed for the STARFIRE commercial fusion reactor design. It was found that vacuum pumps having a helium capture probability of 0.5 (total helium pump speed $1.2 \times 10^4 \text{ m}^3/\text{s}$) in combination with the proposed STARFIRE limiter-vacuum concept is sufficient to achieve plasma impurity control and, simultaneously, high fractional burnup (11%). The high fractional burnup and minimum fuel recycle time result in a very low fuel cycle tritium inventory, ~1300 g. A "Lean-T burn" method that can further reduce the fuel cycle inventory by 30-50% is discussed. D₂O is proposed as a first wall coolant from considerations of plasma contamination (due to hydrogen isotope permeation through coolant tubes) and enrichment of recycled tritium from the coolant circuit. Tritium recovery from solid breeders, under realistic structural and breeder materials constraints, appears to represent a formidable task. The tritium inventory in the solid breeder is estimated to be as high as 10 kg, which would make the blanket the largest single hold-up point for tritium in the plant.

Introduction

The purpose of this paper is to describe the tritium containment and handling strategy developed during the early stages of the STARFIRE commercial tokamak reactor conceptual design study¹. Major objectives of the tritium-related studies for STARFIRE include (1) minimum total tritium inventory and, more importantly, minimum tritium holdup in those reactor systems and components wherein the tritium is vulnerable to release (e.g., the vacuum system, fuel recycle system, and blanket system), (2) a tritium release rate from the plant of less than 1 Ci/day/GWth during normal operation², and (3) a functional tritium facility design that is amenable to rapid maintenance and repair and has a high reliability factor.

Consistent with the above objectives, a limiter-vacuum concept³ has been conceived for STARFIRE which appears to permit adequate plasma impurity control and, simultaneously, high fractional burnup (i.e., >10%). Vacuum pumping considerations and several unique strategies designed to minimize tritium inventories in the evacuation and fuel recycle systems are discussed. In general, the proposed fuel purification, enrichment, and circulation methods for STARFIRE are along the lines currently under development in the Tritium Systems Test Assembly (TSTA) program⁴ at LASL and are not dwelled upon herein. Present plans for recovering tritium from the coolant circuits and from the breeder blanket are also summarized.

Plasma Impurity Control

Because STARFIRE is designed for continuous burn but has no magnetic divertor, impurities and, concomitantly, a substantial amount of unburned fuel must be

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Fig. 1. Effect of Reflection Coefficient on Fractional Burnup and Tritium Inventory

continuously removed from the torus by the limiter-vacuum system³. A series of parametric trade studies were performed to assess the impact of impurity control upon tritium inventory in the fuel cycle. Figure 1 is a typical example, showing the effect of the reflection coefficient (the probability that a particle escaping the plasma will return to the plasma before being trapped by the limiter or the first wall) upon fractional burnup and tritium inventory. The three cases represent values of 1 s, 3 s, and 5 s for particle confinement time (τ_p). (The total inventory includes an assumed 1 kg of tritium in the blanket.) As the reflection coefficient (R) increases, the fractional burnup increases. As the fractional burnup increases, fuel flow rates decrease and, therefore, tritium inventories in the fuel cycle decrease as well. A highly efficient impurity control scheme, on the other hand, is tantamount to having a very low value of R. It is evident that maximizing plasma purity will result in a low fractional burnup and a large fuel cycle inventory. In order to minimize tritium inventory in the fuel cycle, it is necessary to have the equivalent of a relatively inefficient divertor. Consistent with the objectives of minimum tritium inventory, the limiter-vacuum system³ was selected as the plasma impurity control method for the STARFIRE design.

Vacuum Pumping System

The primary vacuum boundary of the STARFIRE vacuum system is at the inner wall of the shield. A pair of limiters pass circumferentially around the outer

Fig. 2. Design of Limiter-Vacuum Concept

edge of the plasma region, and deflect ions from the scrape-off zone into the adjacent slots in the first wall. The slots are 60 meters long and 20 cm wide and penetrate the first wall and blanket. The location and configuration of the limiter is optimized to maximize the probability of a molecule entering the slot after striking the limiter. Each slot contains a step to reduce neutron streaming as shown in Fig. 2. In the molecular flow region this step has minimal effects on the conductance, the only consideration being the additional slot length required to provide the step. A large volume plenum exists between the outside of the first wall and the inside of the shield. Twenty-four 1.7 meter diameter, 1.5 meter long vacuum ducts penetrate the shield and provide access for liquid helium cryogenic pumps.

The liquid helium cryogenic pumps will be of the compound variety in which hydrogen and its isotopes are pumped by cryocondensation on a liquid helium cooled panel and helium is cryosorbed on a 4.2 K molecular sieve surface. The cryosorption surface on such a pump can become saturated with helium and will require regeneration after approximately eight hours of operation. Regeneration will be accomplished by stopping the liquid helium flow and allowing the sorption surface to warm, whereupon the helium is released and pumped away by an auxiliary pumping system.

If the impurity control system is to remove impurities at a rate sufficient to maintain a steady-state continuous burn plasma, 25% of the molecules which enter the slots at the first wall must be pumped. An

analysis of the vacuum system pumping probability was performed. The slots were treated as rectangular ducts and were assumed to be in series with the circular vacuum ducts at the inner wall of the shield. The number and size of circular vacuum ducts were assumed to be fixed for this analysis. A large volume plenum exists between the exit of the slots and the entrance to the circular ducts. This volume is assumed to have minimal effect on the system pumping probability. The final element in the vacuum pumping analysis is the cryogenic vacuum pump. A capture probability must be assigned to complete the calculations. A factor was also included to account for the directional nature of the molecules entering the slots, which enhances the system pumping probability.

The results of these calculations are presented in Fig. 3. The total system pumping probability is shown as a function of slot width for various cryogenic pump capture probabilities. It is seen that an overall pumping probability of 0.25 can be achieved for a 20-cm wide slot if a cryogenic pump capture probability of 0.5 is assumed. Such capture probabilities are beyond the present state of the art, but may become feasible as cryogenic pumping technology is advanced. Enlarging the slot widths beyond 20 cm, for a fixed vacuum duct geometry, will not improve the system pumping probability. However, widened slots will significantly increase the probability of a molecule entering the slots after striking the limiter surface and may thus reduce the required pumping probability. The calculated total helium pumping speed for this system is $1.25 \times 10^3 \text{ m}^3/\text{s}$ at the limiter, and $1.2 \times 10^4 \text{ m}^3/\text{s}$ at the pumps.

Fig. 3. Vacuum Pumping Requirements for Limiter-Vacuum System

1. Plasma Chamber
2. Limiter Plates
3. Debris Separator
4. D-T Cryocondensation Pump
5. Helium Pump
- 6,7. Regeneration Pumps
8. Metal Bellows Pumps
9. Breeder Blanket
10. Electrolysis Unit
11. Fuel Cleanup Unit
12. Tertiary Enclosures
13. Emergency Air Detritiation System
14. Secondary Enclosures, Purge Streams
15. Tritiated Waste Treatment
16. Water/Tritium Recovery Unit
17. Helium (tritium-free)
18. Tritiated Waste -- Liquids and Solids
19. Detritiated Gases: N_2 , O_2 , CO_2 , Ar
20. Isotopic Separation Unit
21. D_2 Supply
22. D_2 Storage
23. DT and T_2 Storage
24. T_2 Shipment/Receiving
25. Fuel Blender
26. Gas Fueling
27. Pellet Fueler

Figure 4. Fuel Cycle Scenario for STARFIRE

Fuel Cycle

The fuel cycle scenario shown in Fig. 4 has been developed for STARFIRE. Starting with the plasma chamber, the first item encountered is the limiter-vacuum system. The DT is pumped by cryocondensation pumping and the He is pumped by cryosorption pumping. Recent results⁵ indicate that an essentially clean separation between He and hydrogen isotopes can be achieved with a cryocondensation/cryosorption pump configuration. The helium is then passed to the Tritiated Waste Treatment (TWT) subsystem for removal of trace amounts of tritium. The DT is regenerated from the condensation pump (two sets are operated in tandem), pressurized to about 0.1 MPa (1 atm), and transferred to the Fuel Cleanup Unit (FCU), where condensibles are removed to levels of <1 wppm.⁴ The tritium in the breeder blanket will be recovered as T_2O . Upon reduction, the bred tritium is passed to the FCU. The purified fuel is then isotopically enriched and prepared for refueling. The fuel processing subsystems are contained within gloveboxes, which in turn are located in a separate tritium facility building.

The tritium and vacuum system parameters for STARFIRE are listed in Table 1. The fuel recycle time and reserve storage have been absolutely minimized. Redundancy and reliability of fuel processing and tritium recovery subsystems are implicitly required.

Table 1. Tritium Facility and Vacuum System Parameters for the STARFIRE

Torus Evacuation System Parameters

Fractional burnup	0.11
Evacuation Volume (m^3)	902.8
Gas Load-Limiter ($Pa \cdot m^3/s$)	9.89E 01
Total Pump Speed at Limiter (m^3/s)	2.6E 03
Total Rated Pump Speed-DT (m^3/s)	2.0E 04
Total Rated Pump Speed-HE (m^3/s)	1.2E 04
Cryopanel Surface-Lim. (m^2)	2.4E 02

Tritium and Fuel Processing Parameters

Tritium Burnup per Day (g)	495.40
Tritium Exhaust per Day (g)	3.99E 03
Tritium Fueling per Day (g)	4.48E 03
Tritium Input per Day (g)	4.48E 03
Helium Exhaust per Day (g)	659.
Tritium Bred per Day (g)	498.
Tritium Decay per Year (g)	143.
Doubling Inventory (g)	3.60E 03
Doubling Time (years)	5.
*Breeding Ratio	1.005
Tritium Inventories (g) *****	*****
Vacuum Pumps	330.
Storage	495.
Surge Tank	83.
Liquefaction Unit	45.
Cryogenic Distillation Cascade	105.
Fuel Preparation	240.
Tritium Inventory in Fuel Cycle	1300.

Emergency Air Detritiation System (E.D.S.)

Maximum Conceivable Release (g)	818.10
Initial Conc. ($\mu Ci/m^3$)	3.93E 07
*Cleanup Time (hours)	48.00
*Volume of Reactor Bldg. (m^3)	2.00E 05
*Decontamination Factor	1.00E 06
*Cleanup Efficiency	0.900
E.D.S. Flow Rate (m^3/s)	17.5
E.D.S. Flow Rate (% Bldg. vol/min)	0.52
*T Level Stacked ($\mu Ci/m^3$)	50.000
Tritium Release (Ci)	10.000
Vent Rate (m^3/s)	3.33E 00
Tritium Vented to Env. (Ci)	1.50E 00

Table 1. Tritium Facility and Vacuum System Parameters for the STARFIRE (Contd.)

Tritium and Vacuum Systems Costs (\$M)

Piping	2.0
Isotopic Separation Unit	2.0
Gloveboxes and Purifiers	6.1
Misc. T Facility Costs	2.4
Pellet Fueling	8.7
Total Fuel Processing	21.0
Cost - E.D.S.	19.5
Cost - Limiter-Vacuum System	17.0
Total Tritium + Vacuum Systems	57.5

The Concept of a "Lean-T" Burn

A method for reducing the tritium inventory by operating the plasma with a D/T ratio >1 is being considered. A similar method was proposed by Conn, et al⁶, in which the plasma has equal deuterium and tritium in the center but is rich in deuterium at the edge. The "Lean-T" method is complimentary to that of Conn. As shown in Table 2, as the D/T ratio increases, the tritium exhaust rate decreases. It is assumed that the fusion power is kept constant by maintaining a constant value of $N_p \times N_T$. As the D/T ratio increases, the tritium density decreases while the deuterium density and the total density N_{tot} increase. According to Alcator scaling, the particle confinement time increases linearly with increasing density. Thus, as the D/T ratio is increased, the gas load remains constant although N_{tot} increases. Also, tritium exhaust rates (proportional to N_T/N_{tot}) can be substantially reduced. The tradeoff of this scheme is that a higher density is required for a fixed power level and thus higher β or higher field is required. As shown in Table 2, a D/T ratio of 2 can reduce tritium exhaust rates (and inventories) by ~33%. In order to do this, N_{tot} is increased 6% and therefore β would have to be increased 6% (e.g., from 5% to 5.3%).

Tritium Recovery from the Blanket

The STARFIRE blanket design is currently focused on the use of solid oxide-based breeders and helium coolant. Methods of tritium recovery using a He purge stream are currently being investigated. Realistic breeder and materials constraints, together with diffusion and mass transfer considerations, appear to make it very difficult to keep the tritium inventory in Li₂O below 1 kg.⁷ For example, the equilibrium T₂O vapor pressure above LiOT in solution in Li₂O for a tritium inventory of 1 kg is calculated to be $\sim 10^{-8}$ torr. Recovery of tritium as T₂O by a purge stream requires a T₂O (or HTO) pressure of $\sim 10^{-3}$ torr for reasonable purge flow rates. The LiOT concentration must be increased nearly three orders of magnitude in order for in-situ extraction to be viable. Both in-situ and out-of-reactor methods of tritium recovery are being investigated. It appears likely that the blanket tritium inventory will have to be about 10 kg at steady-state recovery to achieve a credible integrated blanket design/operating scenario. Key tradeoffs to the blanket inventory problem lie in tailoring the breeder compound chemistry to achieve more favorable tritium release characteristics. This approach could result in the need to incorporate neutron multiplying zones in the blanket modules. In this same vein, enrichment of ⁶Li in the breeder compound would help to reduce the total mass of breeder material.

Tritium in the Blanket Coolant (Helium)

A calculation of permeation losses from the helium coolant through the steam generator was performed. The steam generator which is constructed of Croloy

(Fe-2%Cr-1Mo) has a tube heat transfer area of 5440 m² (58,500 ft²), a tube thickness of 2.0 mm (0.080 in), and an average tube temperature of about 350°C. Although the He coolant does not contact the breeder directly, as a worst case it is conservatively assumed that a leak develops and the T₂O pressure is 10⁻² Pa. Assuming equilibrium with 1 Pa O₂, the T₂ pressure is then 10⁻¹³ Pa. Given the tritium permeability⁸ of 0.4 mm³(STP)·mm/s·m²·√Pa (0.10 Ci/Day·m²·√Pa), the tritium permeation loss is 0.10 Ci/day. It is noted that no credit was taken for a permeation barrier, which probably could reduce the rate by a factor of about 150.⁸ It is concluded that, due to the fact that the tritium is predominantly in the oxidized form, and due to the relatively low temperature (350°C), tritium permeation losses will be very small and an intermediate loop is not required.

Table 2. Effect of "Lean-T" Burn (in relative units)

N_D/N_T ratio	Total Ion Density	Gas Load	Tritium Density	Tritium Exhaust Rates
1	1.00	1.00	1.00	1.00
2	1.06	1.00	0.71	0.67
4	1.25	1.00	0.50	0.40
10	1.74	1.00	0.32	0.18

Implications of the Choice of Water (or D₂O) as a First-Wall Coolant

The STARFIRE design currently employs water as the first-wall coolant. During the course of reactor operation, significant amounts of tritium will pass from the plasma chamber into the first-wall coolant water. This tritium is essentially all converted to HTO, in which form permeation rates are very small (<0.1 Ci/day) and tritium is lost only to the extent that the coolant water itself is physically lost. Thus, water is a sink for tritium. Permeation rates for tritium passing from the plasma chamber through the ferritic steel tube wall into the first-wall coolant were calculated (Table 3). First-wall temperatures of 300° and 500°C were assumed for the calculation. The driving pressure was assumed to be 0.13 Pa (10⁻³ torr) and the attainable barrier effectiveness was assumed to be 400-X. Comparison of these results with other materials of construction show that permeation rates for stainless steels will be an order of magnitude lower, while more permeable materials, e.g., Ti, V, and Nb alloys, will be about two orders of magnitude higher. Steady-state levels (Table 3) are achieved when β -decay equals about 20 times the annual inleakage rate, assuming that losses are insignificant and that tritium is not actively removed from the water.

For conditions anticipated in systems with a water-cooled first wall, tritium levels are estimated to be 2 to 100 Ci/l. As a point of reference, 20 Ci/l is typical of the level in the moderator of a heavy water reactor (HWR) after many years of operation, however, some control is implemented to maintain this level. This level gives H/T ratios on the order of 10⁵ in the water and requires an enrichment of at least six orders of magnitude before one can consider returning the tritium to the fuel cycle.

The use of D₂O as a coolant offers a significant advantage over H₂O because deuterium is a fuel, and therefore, isotopic enrichment requirements are somewhat relaxed. For example, the tritium could be recovered in a 99% deuterium mixture, requiring enrichment factors of $\sim 10^3$, which is two to three orders of magnitude

Table 3. Summary of Tritium Loss Calculations for the Tube Bank First Wall Concept

Material = Ferritic Steel

Area = 730 m²

Thickness = 3 mm (3 x 10⁻³ m)

Average Driving Pressure = 10⁻⁴ kPa (~10⁻³ torr)

Parameter	With Barrier ^c		Without Barrier	
	300°C	500°C	300°C	500°C
Permeability, Ci/day·m·kPa	10 ⁻³	10 ⁻²	0.4	4
Permeation Rate, Ci/day	2.5	25	1 x 10 ³	1 x 10 ⁴
Permeation Rate, Ci/year	10 ³	10 ⁴	4 x 10 ⁵	4 x 10 ⁶
Steady State Level, ^a Ci	2 x 10 ⁴	2 x 10 ⁵	8 x 10 ⁶	8 x 10 ⁷
Steady State Concentration, ^b Ci/l	0.2	2	80	800

^a Level at which decay rate equals migration rate.

^b Assuming 10⁵ liter of coolant. Comparable levels for heavy water moderated fission reactors are ~ 80 Ci/l in the moderator and ~ 4 Ci/l in the primary coolant.

^c Assuming a permeation barrier effectiveness of 400-X.

lower than required for light water. Separation factors will be somewhat lower for D₂O, but this is a comparatively small effect. Also, for STARFIRE, 2000 Ci/yr will be generated in the D₂O coolant from neutron activation of the deuterium. This amount is less than that for inleakage from the plasma. More importantly, although hydrogen permeation from the coolant through the first wall could affect the plasma performance, deuterium permeation should present no problem. All factors considered, it appears that D₂O is much preferred over H₂O as a coolant if water is to be used.

Conclusions

Although the STARFIRE study is still in the early stages of its evolution, it has been possible to draw several important insights and conclusions from the work on tritium-related aspects done to date. These are enumerated below:

1. Implementation of the limiter-vacuum concept appears to result in adequate plasma impurity control and, at the same time, a reasonably high fractional burnup (11%) which leads to reduced tritium throughput requirements.
2. Vacuum pumps having a He capture probability of 0.5 (total He pump speed of 1.2 x 10⁴ m³/s), although beyond present state of the art, are required to achieve an acceptable vacuum topology.
3. The high fractional burnup coupled with efforts to minimize tritium holdup times, leads to a low fuel cycle tritium inventory, ~ 1300 g.
4. Use of a "Lean-T" burn could (in principle) reduce the present fuel cycle inventory by from 30 to 50%.
5. It appears that the blanket (solid ceramic breeder) will have the largest tritium inventory, perhaps 10 kg because of thermodynamic and mass transfer constraints.
6. Tritium inventories in the blanket coolant (He) and first wall coolant (D₂O) are very small, only a few grams, and are considered to be recoverable.

7. D₂O is preferred to H₂O as a first-wall coolant because of considerations of plasma contamination (from hydrogen isotope permeation) and tritium recycle/enrichment.

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SUPERCONDUCTING POLOIDAL COILS FOR
"STARFIRE" COMMERCIAL REACTOR*

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Summary

STARFIRE is considered to be the tenth commercial tokamak power plant. A preliminary design study on its superconducting poloidal coil system is presented. Key features of the design studies are: the elimination of the ohmic heating coil; the trade-off studies of the equilibrium field coil locations; and the development of a conceptual design for the superconducting equilibrium field coils. Described are the 100 kA cryostable conductor design, the coil structure and evaluation of the coil forces.

Introduction

STARFIRE¹ is an assumed steady state operating commercial tokamak based on a continual plasma current mode. The plasma startup, heating and current drive are accomplished by a lower hybrid rf system. This system eliminates the need for ohmic heating coil systems.

STARFIRE Equilibrium Field (EF) coils provide several important functions.² Firstly, the EF coils will provide the static equilibrium of the plasma against both the radial and the vertical displacements; secondly, the EF coils will elongate the plasma; thirdly, the EF coils will control the plasma position; fourthly, the EF coils will provide disruptive instability control; and finally, the EF coils will drive the plasma through the current induction. If the EF coils were placed inside the Toroidal Field (TF) coils, there would be tremendous difficulties in coil assembly, disassembly, support, repair and maintenance; as well as in the assembly, disassembly and maintenance of the blanket and shield and the first wall. However, if EF coils were placed outside the TF coils, it is difficult to control plasma position, to generate a sufficiently D-shaped plasma and to control the plasma disruptive instability. Furthermore, EF coils outside the TF coils will require an order of magnitude larger ampere-turns, stored energy and will produce large tipping forces and superimposing fields onto the TF coils. Thus, the location of the EF coils is a key design consideration in the STARFIRE design.

The basic design approach is to locate almost all the EF coils outside of the TF coils; all such EF coils will be superconducting. A limited number of segmented copper coils would be located inside the TF coils but outside of the blanket and shield. These auxiliary coils will be used for control purposes. Although this configuration increases the ampere-turns and the stored energy of the magnet system, the reliability and the maintainability of the EF coil system should be far superior to an inside EF coil system. With this configuration, however, it is important to minimize the TF coil size. For example, equilibrium calculations with

EF coils exterior to a toroidal field coil with the outer leg at 16.8 m showed that a total of about 200 MA turns would be required in the EF coils to support plasma with 10 MA current, major radius of 7 m, elongation of 1.6, and beta poloidal β_p of 1. This large EF coil current implies very large local forces on the TF coil and very large stored energy, as well as great positional sensitivity of the plasma to the EF coil currents. When the coil outer leg is moved in to the current design value of ~ 13.5 m, the EF coil current is reduced to around 100 MA-turns, indicating the importance of minimizing the TF coil size.

In STARFIRE, the plasma current normal startup and shutdown period will be of the order of 10 to 60 s and the plasma disruptive shutdown will be of the order of 5 s. Rapid or sudden changes in the plasma position, due, for example, to modifications of the current profile by MHD activity, cannot be followed by the external EF coil system without oversizing of the power supplies and difficult constraints on coil manufacture due to the high voltage to be applied to the terminals. It is more practical to employ the internal EF coils for the purpose of plasma control. These coils would be actively controlled and would carry little average current and will thus consume little I²R losses.

The blanket and shield are segmented toroidally and insulated to facilitate the soak-in of the current fields. In the event of a plasma disruption, large voltages will be generated in the vicinity of the plasma. These voltages will break down the intersegment insulation of the blanket or the control coil insulation unless an alternative path for the current is provided. For STARFIRE a system of toroidal conductors with spark gaps will be designed for this purpose.

Superconducting EF Coils

The locations of the external superconducting equilibrium field coils and the currents in them are determined as follows. First, an MHD equilibrium of the plasma is determined based on the plasma physics requirements of the reactor. Among the equilibrium properties are the plasma current distribution and the poloidal magnetic field in the plasma. Part of the poloidal field is produced by the plasma currents. The remaining part, the external field, must be supplied by the EF coils.

For given locations of the EF coils, the required current in them is determined by making a least squares fit of the field produced by the coils to the desired external field of the MHD equilibrium. Experience has determined that an average error of $\sim 0.3\%$ in matching the desired external field is sufficient to reproduce the plasma physics parameters and the desired plasma boundary. Any additional flexibility in the coil system is used to minimize the stored energy in the EF coil system. This procedure is described in detail along with many examples in Reference 3.

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

The optimized design is found by systematically varying the locations of the coils, subject to engineering constraints such as the location of vacuum ducts. For STARFIRE it was found the configuration shown in Fig. 1 was a satisfactory design. Basically, the plasma D shape is formed by pulling the plasma outward with the two upper coil pairs (with current in the same direction as the plasma current) and by pushing in with the other two. The vacuum ducts come out between the two outermost coils, and the space between the two upper coils is left for engineering access. The stored energy of 10.1 GJ could be reduced slightly with more coils. The 98 MAT would, however, increase somewhat more rapidly. The location of the coils is such that moving any of them a short distance (but not inside the non-allowed space) would increase the stored energy.

The vertical field pattern generated by the EF coils is plotted in Fig. 2. The EF coils produce a vertical field of about 0.61 T at plasma center. The field lines concave to the right are required to generate a noncircular plasma. However, the field lines will not provide stability against vertical displacement. The total flux contour contributed by both the superconducting EF coils and the 10.8 MA plasma current is shown in Fig. 3. The plasma obtained has an elongation of 1.60 and a triangularity of 0.5.

Main characteristics of the EF coils are given in Table 1. The EF coils will have a maximum operational current of 200 kA. To reduce sponginess of the high current cable, it is proposed to connect in parallel all coils above the machine midplane with their counterparts below the midplane, thus the operating current of the superconducting cable in each coil will be 100 kA. The EF coils will have 490 turns in each parallel path. The normal charging or discharging voltage for the EF coils is about 9.6 kV to 1.6 kV depending upon the 10 second or one minute ramping time and the turn-to-turn voltage is about 18 to 3 V. The self-stored energy of the EF coils is 10.1 GJ.

The EF coils will be cooled by pool boiling at 4.2 K and 1 atmospheric pressure. Pool boiling is simple, inexpensive, reliable and easy to operate and control.

The superimposing field on the TF coils generated by both the EF coils and the plasma is also shown in Fig. 1. It is seen that the superimposing field will generate fairly large tipping forces on the TF coils as well as impose an order of 1 T additional field margin onto the TF coil winding.

Axial Pressure and Hoop Stress

Table 2 shows the peak field and the computed vertical forces per unit length of each EF coil. It is seen that all coils except Coil No. 2 will have attractive forces toward the midplane. The axial magnetic pressure, P_z , is a function of both axial and radial positions within a coil cross section. The maximum axial magnetic pressure for each coil is also given in Table 2.

Assuming a uniform coil structure, the average hoop stress is the product of the average current density, the mean coil radius and the axial field component averaged over the coil cross section. On the other hand, the average radial magnetic pressure is the product of the average current density, the coil radial thickness and the average axial field component. Both the hoop stress and the radial magnetic pressure for each coil are computed and

listed in Table 2. Since the coil mean radius is generally larger than the coil radial thickness, the average loop stress is generally larger than the average radial magnetic pressure. The maximum magnetic pressure is 823 Kg/cm² (about 12,000 psi) exerting on Coil No. 4.

Conductor Design and Coil Structure

As an illustration, a 100 kA cable design is shown in Fig. 4. The cable is designed with full cryo-stability and reasonably low ac losses for the 10 s to 60 s normal charging and discharging purposes. To achieve high reliability, the EF coils must be cryo-statically stable. To withstand the dB/dt of about 0.8 T/s during the current swing, individual superconducting strands in the basic cable and the basic cables in the full conductor must be fully transposed. The full conductor was achieved by fully transposing 24 basic cables around a G-10 strip so that the cable will have a height of 2.4 cm by a width of 13.8 cm. Spiral wrapping wetted-wound fiberglass of 3 mm in thickness is employed to band these basic cables together. The fiberglass band will also provide a liquid helium channel of 1 cm in width by 0.3 cm in depth by 13.8 cm in length.

Because of the sponginess of the 100 kA cable, it is important that the EF coil structure must be designed to isolate the electromagnetic forces in both the axial and the radial direction. Figure 5 illustrates such a coil structure using the 100 kA cable. The cable will be loosely fitted into a stainless steel channel with helium cooling and venting slots. The stainless steel channel will support both the axial and the radial electromagnetic forces. Since the cable will be loosely fitted into the channel, the coil body forces could not be accumulated to the cable. The axial coil forces will be bridged through the G-10 plates, which is the support plate for each pancake coil layer. These supporting plates will be machined to have radially outward cooling channels for helium gas venting.

Table 1
Characteristics of Superconducting EF Coil

Superconductor/Stabilizer	Nb-Ti/Cu
Stability	Cryostable
Operating Current	200 kA
Cooling	Pool Boiling
Operating Temperature	4.2 K
Average Current Density	About 1400 A/cm ²
Magnetic Field at Plasma Center	0.61 T
Total Ampere Turns	98×10^6
Maximum dB/dt (normal operation)	0.8 T/s (10s) to 0.13 T/s (60s)
Self-Stored Energy	10.1 GJ
Power Supply Voltage	9600 V (10s) to 1600 V (60s)
Volt-Second to Plasma*	100
Self Inductance	0.507 H
Mutual Inductance to Plasma	-5.23×10^{-4} H

* Plasma self inductance is 12×10^{-6} H and its stored energy is about 700 MJ

Table 2
Vertical Forces, Axial Pressure and Hoop Stress

Coil Number		1	2	3	4
Average Current Density (A/cm^2)		1382	1382	1186	1209
Peak Field (T)	without plasma	7.76	7.46	4.30	4.45
	with plasma	6.82	7.80	4.47	4.47
Vertical Force* (10^3 kg/cm)	without plasma	14.93	-4.88	.49	1.31
	with plasma	14.42	-3.95	1.35	.58
Max. Axial Pressure, P_z (kg/cm^2)	without plasma	309.25	134.32	105.05	115.44
	with plasma	299.50	122.11	109.37	111.61
B_z (T)	without plasma	-2.85	2.07	.24	-.46
	with plasma	-2.02	2.42	.40	-.48
Average Hoop Stress, $\bar{\sigma}_t$ (kg/cm^2)	without plasma	607	504	122	785
	with plasma	430	589	208	823
Radial Magnetic Pressure, $\bar{\sigma}_r$ (kg/cm^2)	without plasma	201	233	27	53
	with plasma	142	272	46	56

* Forces pointing toward the STARFIRE midplane are positive.
Forces pointing away from the midplane are negative.

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Fig. 1. EF Coils Configuration

Fig. 2. Equilibrium Field Pattern for Noncircular Plasma

Fig. 3. Total Flux Contour of STARFIRE

Fig. 4. 100 kA Cryostable Cable Conductor

TURN TO TURN STRUCTURE ($J_{avg} = 1400 \text{ A/cm}^2$)

TYPICAL CROSS-SECTION OF COIL STRUCTURE

Fig. 5. EF Coil Structure

TOROIDAL FIELD COIL SYSTEM FOR "STARFIRE", A PRELIMINARY ASSESSMENT*

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An important element of STARFIRE is the 12-coil superconducting toroidal field coil system which is required to generate 5.6 tesla at the 7.0 m plasma axis, with a peak-to-peak field ripple of 1% (maximum). Internal spatial requirements dictate a clear bore about 15-1/2 m high x 10 m wide, resulting in a total stored energy of 67 GJ.

Key Design Issues

One should bear in mind that this would be a truly immense magnet system, the size, mass, stored energy, refrigeration requirements and cost of which place it far beyond any existing superconducting device. Yet, every aspect of such a system is, or should be, a logical, straightforward extension of present technology.

Among the key issues to be addressed in the design are the following, not necessarily listed in their order of importance:

- Selection of superconductor material and configuration.
- Cryogenic and structural accommodation of the heat loads resulting from plasma neutron radiation and disruptions.
- Support of the coils against the out-of-plane (overturning) loads generated by the external poloidal field coil system.
- Ease, and location of manufacture.
- Operational reliability.
- Maintainability.
- Minimization of shutdown, and end-of-life radioactivity.

The first three of these items are discussed under Selection of Basic Design Features (below).

An important consideration is the feasibility of transporting completed TF-coils from a central manufacturing plant to a distant reactor site, rather than wind them "on location". This will be investigated by GA and Parsons personnel in the near future.

Of supreme importance for the STARFIRE TF-coils is assurance of operational reliability. This is achieved through a combination of sound, straightforward design; good manufacturing practice; and comprehensive component and system testing.

At best, the removal and replacement of a TF-coil following an in-service failure would be a costly, time consuming operation. The principal difficulties here would be removal of machine components surrounding the coil, and timely manufacture of a replacement. Nevertheless, the coils should be designed for relative ease of removal from the toroidal array. (Demountable

superconducting joints are not regarded as a feasible option. Their very complexity and bulk would likely mean that any failure would occur in the joints themselves.)

Selection of Basic Design Features

The goal of this study is not to produce a detailed "nuts-and-bolts" design for each major system, but rather, to identify and present viable solutions to the various fundamental design aspects. Basic design features of the TF-coil system include the conductor materials, configuration, and support; coil cooling method; coil design; cryostat materials, design and support.

Conductor Material

Various design studies at GA and elsewhere indicate that 10 tesla is about the upper practical limit for employment of NbTi cooled with liquid helium at 4.2 K and one atmosphere. Therefore, in the high field region (9-11 T) of an 11 tesla STARFIRE TF-coil, selection of Nb₃Sn appears to be warranted, based upon anticipated fabrication and metallurgical advances during the next two or three decades. Nevertheless, Nb₃Sn is an inherently brittle material, which can tolerate very little tensile strain.

Although high purity aluminum offers potential advantages of high conductivity, these presently appear to be offset by the difficulty of codrawing it with filamentary Nb and NbTi. Further, its much reduced end-of-life radioactivity compared to copper is mitigated by the intimate presence of highly activated superconductor materials. Copper has therefore been tentatively selected as the stabilizer material.

Conductor Configuration

A 24 kA, three level cabled conductor has been chosen for the following reasons:

- To minimize coil heat loads resulting from plasma disruptions and from time varying poloidal fields.
- To provide a high cooled surface-to-area ratio, an important cryostability consideration.
- As a practical, proven design for the fabrication of high current filamentary composite conductor.
- To minimize tensile strain experienced by the Nb₃Sn superconductor.

The conductor of each pancake (layer) is divided into four grades, the amount and type of superconductor in each being based upon the peak field experienced (3, 7, 9 and 11 tesla).

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

Nb_3Sn is employed only in the high field (9-11 tesla) region. A bronze diffusion geometry is employed, with a tantalum barrier to shield the surrounding stabilizer material (as shown in Fig. 1). The Nb is reacted with the Sn of the bronze matrix after the cabling process is complete but prior to winding.

The same number of composite strands, subcables and cables are retained throughout the layer, the strand size varying from grade to grade. The 7-9 tesla and 0-5 tesla NbTi conductor configurations are shown in Figs. 2 and 3 respectively.

For the Interim Design, cryostability of the conductor has been based simply on the "quasi-static" criterion that, in a resistive region, the "equivalent" heat transfer rate to the coolant is 0.35 W/cm^2 . This criterion is applied to the 250 amp subcable, where 50% of its enclosing cylindrical envelope is assumed to be effective for heat transfer.

Conductor Hoop and Bearing Load Support

Inherent in a cabled conductor design, particularly when employing Nb_3Sn , is its limited ability to support hoop and transverse bearing loads (the latter occurring in the centerpost region of a TF-coil). In the selected design, the conductor is sandwiched between two pre-tensioned stainless steel strips for hoop load support, and flanked by two bearing load supports. This configuration for the high and low field regions is depicted in Figs. 4 and 5. Note that the bearing support strips are much wider in the low field, high bearing load region.

Stainless steel is employed for these support elements due to its high elastic modulus (30 Mpsi), compared to aluminum (11 Mpsi). This is especially important with Nb_3Sn conductor in order to limit hoop strain. (Both support strip and conductor would then be discarded and buried after reactor disassembly.)

STARFIRE: TF-COILS 24 KA CONDUCTOR HIGH FIELD REGION (9-11 TESLA)

Fig. 1

STARFIRE: TF-COILS 24 KA CONDUCTOR 7-9 TESLA FIELD REGION

Fig. 2

STARFIRE: TF-COILS 24 KA CONDUCTOR LOW FIELD REGION (0-5 TESLA)

Fig. 3

**STARFIRE: TF-COILS
CONDUCTOR/SUPPORT STRIP MODULES
HIGH FIELD REGION (9-11 TESLA)**

AVG. CURRENT DENSITY IN
MODULE 957 A/CM²

ALL DIMS. IN CM.

Fig. 4

**STARFIRE: TF-COILS
CONDUCTOR/SUPPORT STRIP MODULES
LOW FIELD REGION (0-5 TESLA)**

CENTERPOST PEAK RADIAL BEARING LOAD
ACROSS BEARING SUPPORT STRIP = 32000 PSI

AVG. CURRENT DENSITY IN CONDUCTOR-
SUPPORT STRIP MODULE = 1181 A/CM²

ALL DIMS. IN CM.

Fig. 5

Coil Cooling Method

Bath cooling has been selected in lieu of forced flow, based upon considerations of design simplicity and operational reliability.

Forced flow cooling introduces complexities of conductor design, pumping, and parallel path manifolding which more than offset any potential benefits of improved heat transfer. In principle, forced flow cooling through simple well defined channels is well understood and is characterized by relatively high heat transfer rates (example: "hollow" monolithic conductor). However, the flow path geometries of cables or braided conductor presently under consideration for large TF-coils are torturous. Proper understanding of such geometries will require considerable analytical and experimental effort. Certainly a far greater body of practical operational experience exists for bath cooling of large superconducting coils.

The possibilities of coolant path blockage or pump failure are reliability concerns for a forced flow system. Bath cooling on the other hand is inherently reliable so long as the coils remain fully immersed in liquid.

Accommodation of Plasma Neutron Radiation and Disruption Heat Loads

For the STARFIRE design, sufficient high grade shielding will be interposed between the reacting plasma and TF-coil centerpost region to limit total heat flux to cryogenically acceptable limits (~30 kW overall into the centerpost).

Of far more concern is the inductive heat load deposited in the helium cooled TF-coil mass by a plasma disruption. (Based upon considerations of operational reliability and maintainability, the present STARFIRE design has no stabilizing/disruption absorbing coils inboard of the plasma.) Regardless of the TF-coil system configuration or component resistances, the total disruption energy

$$E_D = \frac{\Delta B^2}{2 \mu_0} V$$

where $\Delta B = 1$ tesla, and V is the coil/cryostat volume. For our TF-coil size, this amounts to about 500 MJ.

The question yet to be resolved is what fraction of this heat load can be absorbed by the cold (4 K) mass without driving the coil normal. The remaining fraction of the heat load can then hopefully be borne by the room and LN_2 temperature elements (vacuum tank and thermal radiation shield), material and configuration tailored as appropriate.

Coil Design

Conductor configuration, cooling method, cryostat structure, and coil design are of course intimately interrelated design considerations. The essential features of the coil design are shown in the coil/helium vessel centerpost region cross-section, Fig. 6. The pancake (layer wound) coils are wound directly onto the bobbin, formed by the inner radius wall and radial central web of the helium vessel. Primary coolant flow occurs between the coil layers, while the cabled conductor provides channels for face, and conductor internal cooling.

Fig. 6

Cryostat

Structural aluminum alloy (6061-T6) has been tentatively selected for both the helium vessels and vactanks of the toroidal field coils. The primary motivation is to minimize the operational and end-of-life radioactivity of these massive components. A secondary advantage is weight reduction of the coil/helium vessel assembly. A tertiary, but possibly important advantage is that both the helium vessel and vactank act as conductive shields for the TF-coils against plasma disruptions and time-varying poloidal fields.

The two disadvantages of aluminum as a structural material for this application are its low modulus of elasticity and its relatively high coefficient of thermal contraction, compared to stainless steel. Though further investigation is required, these limitations do not appear to outweigh the advantages of aluminum.

The TF-coil helium vessels share a common vacuum enclosure, in the centerpost region. The outer, curved portion of each TF-coil helium vessel is surrounded by an individual vacuum barrier. As shown in Fig. 6, the vertical intercoil vactank seams in the centerpost region are designed to minimize the difficulty of removing a coil/helium vessel unit.

The structure required to support the coil/helium vessel units against out-of-plane (overturning) loads has not yet been investigated, even conceptually. A tentative evaluation of the overturning loads to be borne is shown in the accompanying Fig. 7, based upon employment of external plasma equilibrium (EP-) coils.

Summary of Interim Design Parameters

The basic calculated design parameters are summarized in the accompanying Table 1.

TABLE 1
STARFIRE TF-COILS, CALCULATED PARAMETERS

- Stored energy: 67 GJ
- Total ampere-turns: 189×10^6
- Centerpost region. Coil/helium vessel:
 - Outer radius: 3.50 m
 - Inner radius: 2.07 m
 - Radial thickness: 1.43 m
 - Average current density: 770 A/cm^2
- Total heat load (12 coils)
- Neutron: 30 MW
- Plasma disruption: 500 MJ (unshielded)

Fig. 7

MAINTENANCE CONSIDERATIONS OF THE STARFIRE COMMERCIAL TOKAMAK

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Summary

An initial reactor design has been developed for the Starfire Tokamak reactor that incorporates maintenance features to permit reactor and plant operation with a 75% availability.

All components of the reactor can be replaced utilizing highly automated remote maintenance techniques. Provisions for contact maintenance are included but would only be utilized on a contingency basis since it is believed that allowable dose rates will be decreased in future years. A modular design approach is used for the reactor and auxiliary subsystems to permit efficient use of remote maintenance. The modular approach minimizes the number of different maintenance operations required and can result in use of simple tasks such as push, pull, turn, etc., maneuvers for module removal. Fault isolation is provided for each replaceable module. When a reactor component fails the fault isolation system identifies the module that contains the failed component. The module is subsequently removed and replaced with a pretested module. Detailed fault isolation and component replacement or repair is performed in the hot cell where more time and equipment is available.

The reactor design does not require any in-place manufacturing operations such as rewinding magnets in place and minimizes any in-reactor welding operations. No in-reactor welding is required for scheduled maintenance.

An initial allocation of replacement times and the resultant component reliability requirements has been made for the current utility maintenance scenario that utilizes scheduled annual shutdowns of 1 month and a scheduled 4 month shutdown every tenth year. These allocations establish requirements for maintenance equipment and can be used to identify the need for redundancy. The 75% availability goal is believed to be achievable if a four-year replacement cycle for the first wall, blanket, limiter and RF duct assembly can be utilized. Scheduled maintenance operations can not be permitted to shutdown the reactor more than 1 time per year and in-reactor components should be designed so that scheduled replacement is not required more than 1 time in two to four years. Auxiliary subsystem components outside of the reactor will be required to include redundancy that permits operation for at least a year or that permits replacement during reactor operation.

Introduction

The maintenance approach for the Starfire Commercial Tokamak is being developed as part of a DOE funded study to develop a conceptual design for a commercial tokamak reactor and balance of plant.

The primary consideration in maintenance approach selection is the plant availability which is the main driver in determining the cost of power produced. As such, major reactor design decisions are being made based on maintenance considerations. Design approaches permitting rapid replacement of life limited components have been emphasized. All components, including the life-of-plant items such as the TF and EF coils, are designed for replacement within time limits acceptable to the utilities.

Remote maintenance is used for all operations inside the reactor building because of the desire to limit radiation exposure to maintenance workers. Benefits of contact maintenance will be assessed at the end of the study.

The first wall and blanket assembly (including coatings and limiters) is expected to require the most frequent scheduled maintenance since little redundancy can be incorporated. Design studies resulted in selection of a design that permits fault isolation to a toroidal sector and subsequent removal and replacement. Subsequent repairs are made in a hot cell. The shield system is independent of the first wall and blanket and, except for an access door, requires no scheduled maintenance.

This paper presents the maintenance approach, the commercial tokamak design features that enhance maintenance and preliminary repair time and required mean-time-between-failures for major subsystems. Reactor hall building and maintenance equipment requirements including hot cells, coil rewinding and cranes are discussed.

Maintenance Approach

The remote maintenance approach was chosen for in-reactor operations because of the likelihood that current regulatory radiation limits will be significantly reduced and because the reactor hall will be exposed to tritium by permeation and releases during maintenance of the coolant, fueling, vacuum and tritium systems. Some activation may also result from neutron streaming, and particulate matter. Use of remote maintenance equipment will permit maintenance with a minimal cool-down or clean-up period, and may permit some maintenance operations during reactor operation. Other factors influencing the choice of remote maintenance include: 1) remote maintenance will be required for accident conditions, however it would not have to be as sophisticated, 2) remote technology will be available thru currently planned programs (HEF, etc.) and 3) the belief that for a properly designed machine, scheduled maintenance can be performed more rapidly than personnel suited for radiation protection.

The design philosophy being followed is to minimize the radiation levels within the reactor building; to design all components for complete remote maintenance, and to identify contact maintenance operations where personnel can safely be used with significant economic savings. Emphasis is being placed on designing to permit rapid replacement of scheduled maintenance items because Starfire is assumed to exist in a mature fusion economy where development work and operational experience have resulted in a predictable reactor with appropriate redundancy and reasonable component reliability.

All components within the reactor building are replaceable. Some are replaced on a scheduled maintenance basis while others are designed for the life of the plant and are replaced only in the event of failure. Items designed for the life of the plant include the overhead crane, TF coils, EF coils, coolant piping, reactor support structure and radiation shielding. The blanket assembly, impurity control compon-

ents, rf launchers, pumps, valves, fueling mechanism, power supplies, etc. are replaced on a scheduled basis. Spares are provided for all components with expected high failure rates, so that as one part is removed a pretested replacement is available so reactor operation can commence while repairs to the damaged components are being made. The spares for the superconducting EF coils trapped below the TF coils are stored in place so reactor disassembly is unnecessary in event of a failure. These coils are designed for life of plant but the consequence of their failure suggests in place spares are prudent.

The number of different maintenance operations planned in the reactor building are minimized by using a component "remove and replace" approach. This permits each maintenance action to be preplanned and designed for use with simple push, pull, etc., operations. This approach increases the confidence in the speed of maintenance operations and simplifies maintenance equipment design requirements. Once the damaged or end-of-life components are removed from the reactor they are transported to a hot cell where more time is available for checkout, repair or disposal. The hot cell will have extensive maintenance capability for testing, component replacement, cutting, welding, machining, pinpoint leak location, and low-Z coating repair.

Redundancy is planned for reactor auxiliary subsystems to permit continued operation of the plant until a scheduled maintenance period or until the component can be replaced in-service. The particular components where redundancy is planned will be defined as the design progresses; however, current plans include redundant power supplies, vacuum pumps, rf launchers, some valves, pumps and fueling mechanisms.

Availability goals have been established as 75% for the complete plant including the reactor. This results in a 91 day per calendar year downtime. Allocations of permissible time-to-repair and time-between-failures have been made for major subsystems to serve as a basis for design of the components and maintenance equipment. The maintenance scenario incorporates the current utility practice of scheduling shutdowns annually for one month of maintenance and a 4 to 5 month shutdown every 5 to 10 years for turbine repair.

The Starfire reactor is being designed to conform with this current utility practice, however, the major shutdown is assumed to occur only every 10 years and last for 120 days (3 months). The combination of the annual and ten year shutdowns requires that 39 days of downtime be allocated to scheduled maintenance. Current utility experience with the balance of plant indicates that failures result in approximately 21 days of unscheduled outage annually. This leaves 31 days of unscheduled downtime that can be allowed for reactor subsystems. These allocations assume that both the balance of plant and reactor scheduled maintenances are performed in parallel while no overlap in scheduled and unscheduled maintenance is planned. A summary of these allocations is given below in Table 1. It should be noted that a total of 52 days per calendar year is allocated to unscheduled maintenance while only 39 days is used for scheduled maintenance even though we have stated that most outages will be the result of scheduled shutdowns. This is in agreement with the fact that, in general, unscheduled failures will take longer to locate and repair. Convenient preventative maintenance and repair of redundant components is scheduled as part of the maintenance scenario during these unscheduled outages.

Table 1. Maintenance Scenario

Outage Type	Balance of Plant		Reactor*	
	Scheduled	Unscheduled	Scheduled	Unscheduled
Total Downtime Allocation (da/yr)	39	21	39	31

*Includes maintenance equipment and auxiliary reactor systems.

Reactor Maintenance Features

The reactor design configuration is shown in Figure 1. The design was developed so all components could be replaced in the event of failure and fault isolation is provided to the replacement level so that minimal time is required for identifying a failed component.

The design is being developed to keep the top and sides of the reactor clear for access by maintenance equipment. Components are also being combined where practical to minimize the number of assemblies and improve access. Examples of combined components include; the TF coil room-temperature dewar provides structural support for the EF coils and shield; and the shield provides the anti-torque frame and vacuum boundary.

The blanket is replaceable in 1/24 sectors of the torus. Each 1/24 sector incorporates an integral first wall, limiter, rf duct and air bearing pad that is removed with the blanket. The coolant is manifolded through separate loops for each 1/24th of the blanket to permit leak isolation to individual sectors. The helium coolant lines and the water coolant lines to the rf duct and limiter utilize mechanical disconnects. The life goal for limiter and rf duct is 4 years and the blanket life goal is 8 years. After four years the blanket sector is replaced. The used sector is refurbished in the hot cell by replacing the limiter, rf duct and low-Z coating. Periodic in-situ replacement of the low-Z coating may also be required.

Blanket module leak detection will be accomplished by sequentially reducing the pressure in individual blanket sectors and monitoring the change of the partial pressure of helium in the plasma chamber with a gas analyzer. This technique requires valves that isolate sectors from the primary coolant loop. Leak detection of the limiter first wall and rf launcher water system will also rely on reducing the system pressure of each sector sequentially and denoting changes in the detected leak rate. Leak detection of the shield system to the plasma chamber system will be accomplished by injecting helium into a cavity between redundant seals. The blanket leak detection system is dependent on the particular design that is developed and will be modified as necessary.

The shield is designed to last the life-of-plant and will be replaced only in event of unscheduled failures. A shield door is provided that permits access to the blanket. The door is sealed with redundant seals with intermediate pumping, that are shielded locally to reduce the radiation damage. The seals are replaced each time the door is opened. The basic shield consists of sectors welded together to form the vacuum boundary. Cutting and rewelding the poloidal seals is required if shield sector replacement is required. Access to the welds is from the shield interior and a permanent welder/cutter track is

Figure 1. Starfire Reactor Design

installed at each joint. The modular design of the shield is shown in Figure 2. Redundant seals are provided at each joint to permit leak testing.

The TF coils are also life-of-plant components. They utilize a common welded dewar in the center post region. Coil replacement time is not significantly impacted by the welded dewar. A permanent welder/cutter track is also installed at this joint. Annealing of the TF coil is planned every 10 years to reduce the effects of radiation damage in the stabilizer.

The EF coils are also life-of-plant. EF coils inside the TF coil are copper and are segmented to permit removal. An elevation system is provided to raise and lower the outer coils for access to the blanket. The external EF coils are superconducting. Those on the top and sides of the reactor can be removed for replacement. Spare EF coils are provided for those trapped below the TF coils. In event of failure the coil is cut out and the spare raised into position.

Figure 2. Shield Design

The vacuum system utilizes redundancy to improve the probability of continuous operation between scheduled maintenance periods. Twenty-four cryosorption pumps with isolation valves are provided of which approximately 20 are required for operation. External valves and pumps leading to the tritium processing system utilize redundancy.

The power supplies are assumed fully redundant and include a replacement capability during operation. The power supply systems are located outside the primary confinement building and can be repaired during plant operation.

Failed redundant heat transport system components, with exception of a few in-line valves, can be replaced during reactor operation using remote/hands-on maintenance equipment.

Reactor Building Maintenance Functions

A preliminary listing of scheduled maintenance functions is provided in Table 2. The number of scheduled actions at the reactor island have been held to a minimum. The scheduled maintenance functions of the auxiliary systems can, in part, be done during reactor operations.

The planned unscheduled maintenance functions include all component assemblies making up the reactor. The unscheduled items are shown in Table 3. Both tables 2 and 3 will be updated as the design progresses and the subsystems are better defined.

Table 2. Scheduled Maintenance Functions

Reactor Island

- o Shield door seal replacement
- o rf wave guide window replacement
- o Blanket sector replacement
- o Vacuum pump/isolation valve replacement
- o Fueling system replacement
- o TF coil annealing

Auxiliary Reactor Subsystems

- o Coolant loops
 - valve replacement
 - pump replacement
- o Power systems
 - electrical switch replacement
 - electrical energy storage component replacement
 - power conversion system replacement
- o Maintenance equipment replacement
- o Tritium processing

Table 3. Unscheduled Maintenance Functions
(excluding scheduled maintenance functions)

- o Power lead replacement
- o Coolant line replacement
- o Heat exchanger replacement
- o EF coil replacement
- o TF coil replacement
- o Shield replacement/repair

Hot Cell Operations

The functions planned in the hot cell are listed in Table 4.

Table 4. Hot Cell Functions

- o Component testing
 - vacuum integrity
 - electrical function
 - configuration
 - leakage
- o Repair
 - cutting
 - welding
 - machining
 - cleaning
- o Reactor retrofit
 - blanket
 - o limiter replacement
 - o rf launcher replacement
 - o low-Z coating replacement
 - o module replacement
 - tritium processing system
 - vacuum system
 - coolant systems
 - cryogenic systems
 - power system
- o Maintenance equipment
- o Decontamination/disposal

Maintenance Time Allocations

A preliminary listing of the scheduled maintenance actions is shown in Table 5. The assumed critical path is shown with the presumption that all other maintenance actions can be accomplished in parallel operations. This listing will be updated as the program progresses. An increase in the number of items in the critical path can have significant impact on achieving our scheduled maintenance goals and would require a decrease in the replacement time. If subsequent analysis of the life of blanket module components (i.e., limiters) indicate lives less than 4 years, other maintenance concepts which permit replacement without blanket removal will be required.

The unscheduled maintenance item listing is shown in Table 6. The values shown for Mean-Time-To-Replace (MTTR) are assumed values based on past studies. The average downtime per year is then assigned to each subsystem based on the perceived relative reliability and complexity of each subsystem. The result is a permissible failure rate per calendar year. These values can then be used by subsystem designers to define the redundancy and design margin requirements for the subsystems. An accurate assessment of the actual failure rates can only be derived after subsystems are defined as to the number of components, redundancy provisions, the types of failure modes and failure rate per failure mode have been established. Our program will utilize historical data where possible to assess feasibility of meeting the requirements of Table 6. A conclusion that can be drawn after review of the table is that the time required for shutdown and startup must be kept to a minimum (2 days or less) to prevent using a large percentage of the unscheduled downtime simply to restart the burn.

Maintenance Facility

The selection of the "remove and replace" and "fully remote" maintenance approaches results in a rather large maintenance facility. The first iteration in a facility layout to sustain the needs of normal maintenance has resulted in a facility that

Table 5. Scheduled Maintenance

Action	Frequency (yr)	Replaced/ Outage (%)	Time-to-Replace (days)	Critical Path
Reactor shutdown and startup	Annual	N/A	2	*
Shield door seal replacement	Annual	25	2	*
RF wave guide window replacement	4	25	6	*
Blanket replacement (2 sectors)	Annual	25	20	*
Vacuum pump replacement	Annual	16	6	*
Fueling system replacement	Annual	25	6	*
TF coil anneal	10	100	<120	(1)
Coolant loop component replacement	Annual	25	14	
Power system component replacement	Annual	50	14	
Maintenance equipment repair	Annual	N/A	N/A	

* These operations must be done sequentially, other operations are assumed to be performed during the critical path time frame.

(1) Compatible with 10-yr shutdown for 16 to 20 weeks for turbine repair.

covers some 2000 to 2500 m² of floor area. The major features of the facility are described as follows:

- 2 - Large component disassembly cells, 12 m tall x 12 m x 6 m
- 2 - small component repair cells, 4 m tall x 10 m x 6 m
- 1 - Storage bay (3 levels deep), 16 m wide x 18 m long x 20 m deep
- 2 - Reactor hall service entrances, 10 m tall x 6 m wide

These facilities are supplemented with interconnecting corridors, shielded walls and doors, equipment staging areas, equipment storage and repair, and hot cell operational areas. Additionally these facilities are equipped with cranes, hot cell windows and manipulators, lighting service/repair equipment and decontamination equipment. Scheduled functions covered with these facilities are:

1. Blanket Sector, including servicing of component connectors, seals, support structure and sensors. The major components are:
 - Limiters
 - First wall panels
 - Low Z coating
 - Blanket modules
 - RF grills and extensions

2. Vacuum pumps (scheduled replacement)

3. Shielding Sections (Unscheduled replacement)

Other components such as Klystrons (generator/launcher), coolant piping, vacuum, cryogenic and electrical connectors, structural members, etc. are of a lesser radiation problem and are handled in a second order maintenance category with separate equipment.

Major unscheduled maintenance operations have been considered for shield section, RF coil and TF coil replacement. Equipment will be provided even though they are designed for the life-of-plant.

A proposed maintenance facility layout is shown in Figure 3.

The layout shows the dual entrance to the reactor building through which the blanket sectors will travel. Each will have an ante-chamber for decontamination/isolation. Corridors connect the entrance with the two main disassembly cells, storage vault and outer equipment area. The smaller hot cells for first wall, blanket module, RF grille and vacuum pump maintenance are also attached to the corridor network to attain a smooth flow during multiple maintenance operations.

Figure 3. Maintenance Facility Layout

FUSION REACTOR TECHNOLOGY IMPACT OF ALTERNATE FUSION FUELS*

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Summary

The initial results of a study carried out to assess some of the technology implications of non-D-T fusion fuel cycles are presented. The primary emphasis in this paper is on D-D, catalyzed-D and D-³He fuel cycles. Tokamaks and field-reversed mirrors have been selected as sample confinement concepts. The technology areas considered include first wall design considerations, shielding requirements, fuel cycle requirements and some safety and environmental considerations. Conclusions resulting from the study are also presented.

Introduction

This paper describes the results of an initial study to assess some of the fusion reactor technology requirements of so-called "advanced" fusion fuels. In this paper, "advanced fuels" refer to any alternate fusion fuel cycle compared to the deuterium-tritium-lithium fuel cycle. The basic approach of the study was to develop representative parameters for the distribution of reactor power into neutrons, charged particles and electromagnetic radiation for two sample confinement concepts. These power split distributions were then used to examine some key reactor technology areas such as first wall design considerations, shielding requirements, environmental impact of activated materials, and fuel cycle design considerations.

Fuel Cycles and Confinement Concepts

A variety of elements other than deuterium and tritium can undergo fusion; prominent examples include D-D, D-³He, ³He-³He, p-⁶Li, p-⁷Li, D-⁶Li, and p-¹¹B. These fuels have, in varying degree, the generic features of reduced neutron production, increased fusion energy carried by charged particles, and the elimination of a need for tritium breeding. On the other hand, the combination of lower cross sections, higher plasma temperature, lack of availability of some fuels (³He), and increased radiation losses make efficient confinement (i.e., high energy multiplication) more difficult. A key consideration then is whether or not the advantages are indeed sufficient to justify the development of alternate fuel power plants. A quantitative evaluation of the technology involved must be undertaken.

Deuterium-based fuels have the advantage of operating at relatively low temperatures but involve more neutron and tritium production via "side" D-D reactions. Lower temperature deuterium based fuels, being easier to burn, are compatible with a wide range of confinement concepts; for example, tokamaks could

burn catalyzed deuterium and D-³He. (Catalyzed deuterium refers to burning deuterium such that the reaction products of tritium and ³He are also burned at a rate equal to its birth rate. The resulting energy release allows operation at lower temperatures.) But, unfortunately, the attribute of "cleanliness" is not as fully achieved in the deuterium-based fuels as the more ideal proton-based fuels. The proton fuels, on the other hand, due to a combination of the high temperature required and modest energy released per reaction, appear much harder to successfully burn, forcing the use of more advanced confinement approaches.

In order to study "representative" combinations that are illustrative of a wide variety of possible combinations, we have selected for study a matrix of alternate fuels along with a modest and high β confinement systems. Since catalyzed-D is the only "self sufficient" (only requires naturally available deuterium) low-temperature fuel, it is selected as the base fuel. However, due to its potential attractiveness, D-³He is also considered despite the complication of ³He breeding. Since tokamaks are presently the most highly developed confinement system, and since they (or at least modest β versions) can burn these low temperature fuels, they are selected as a representative modest β (10-20%) confinement system. (These β values are about a factor of two larger than predicted by most current theories, but are considered to be reasonable extrapolations for purpose of the study.) Concurrently, the Field-Reversed Mirror (FRM) is selected as a representative high- β (> 70%) device. This is acceptable for the low-temperature fuels, but may not be adequate for the higher temperature fuels where even more innovative confinement approaches such as Surmac may be necessary.

Plasma Engineering Considerations

This section summarizes some of the plasma engineering considerations of this study. Included is a brief review of the key features of example tokamak and FRM devices for alternate fuels, as well as their power splits.

Tokamaks

The tokamak sets a reference standard and provides a good basis for comparison of D-T and low-temperature alternate fuel technology. As a part of the present study, we have evaluated the characteristics of alternate fuel tokamaks in light of confinement scaling laws based on recent experiments, e.g., empirical energy scaling based on PLT experiments and on Alcator data. For example, with $\beta = 10\%$, ignited catalyzed deuterium and D-³He tokamaks are approximately one-fourth the volume for Alcator scaling as opposed to trapped ion scaling. While these new results are still uncertain due to uncertainties about the accuracy of η scaling at the high temperatures involved (30-40 keV), they demonstrate a plausible advanced-fuel tokamak approach. As an example, an

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ignited cat-D tokamak using Alcator scaling with $\beta = 10\%$ and $B_0 = 8.7$ T would have a major radius of 12 m and a minor radius of 4 m; the thermal power level is approximately 7900 MW.

An advanced fuel tokamak reactor will require toroidal magnetic fields and plasma currents larger than those contemplated in design studies for D-T tokamak reactors. The plasma current requires a large flux swing in the ohmic-heating (OH) coil system to initiate and sustain the plasma current. With NbTi superconducting OH coils, which will probably be limited to a field swing of ± 8 T, the required area within the OH solenoid is roughly proportioned to the desired plasma current. Similarly, a high toroidal field requires thick superconducting TF coils. As the central support cylinder and coil thickness increase, the radius of curvature of the coil increases, which demands even more support and an even larger coil.

A quantitative estimate of these considerations has been undertaken, using the magnet model¹ developed at ANL. Figure 1 shows the combinations of plasma current and maximum toroidal field at the inboard leg of the coil that can be obtained for different major radii of the tokamak. The reactor was assumed to have an aspect ratio of 3, a burn time of 3 minutes, and inboard and outboard blanket and shield thickness of 0.7 m. The TF coil was assumed to operate at 4.2 K and to have copper stabilizer and stainless steel support material. The OH solenoid was assumed to have a magnetic field limit of ± 8 T and a current density of 1000 A/cm². No particular superconductor was assumed. The range of peak fields at the conductor is 12 to 20 T. This entire range for the TF magnet is above the field producible with NbTi, and the upper half of it is above the field achievable with Nb₃Sn. The figure is intended to show the constraints on plasma currents and toroidal field, independent of the choice of superconductor. This analysis is intended to establish the engineering relationships between the allowed limits of the toroidal field, plasma current and reactor major radius. It is not a physics operating point (for example, physics constraints on the stability factor, q , have not been considered), but rather serves as a guide for establishing the possible range of high field

tokamak devices. Considering reasonable ranges of the maximum toroidal field and plasma current, major radii less than 12 meters are likely.

Field-Reversed Mirrors (FRM)

The FRM has been chosen as representative of high-beta devices for advanced fuels. High-beta devices are especially appropriate for advanced fuels because high β operation can partially compensate for the smaller reactivities of advanced fuels. High- β operation also decreases the cyclotron power emitted per unit volume, and it also leads to increased reabsorption of cyclotron radiation. Consequently, advanced fuel FRM's have for their fuel and temperature of operation relatively small power loss due to cyclotron radiation. For most energy loss mechanisms, the energy confinement time generally increases as size of a plasma increases. Thus, the plasma multiplication can be increased simply by increasing the size of the system. Unfortunately, stability considerations limit the radius of the FRM to a few ion gyroradii. Therefore, the advanced fuel FRM's are not ignited reactors. However, despite their small size, the Q values of the advanced fuel FRM's are substantial due to the assumed near-classical diffusion in the closed field region. Thus, advanced fuel FRM's can be categorized as small, efficient reactors with relatively large power densities and moderate Q .

Typical Power Splits

The power splits are affected by fuel cycle, plasma temperature, plasma density (β and magnetic field strength), plasma size and plasma confinement scaling. Table I presents the power splits for the reference semi-catalyzed D, fully catalyzed D, and D-³He tokamaks. Note that the fraction of power in neutrons is less by almost an order of magnitude for D-³He than for semi-catalyzed D. The fraction of power in neutrons is less by about 10% for fully-catalyzed D than for semi-catalyzed D. This is, of course, because the fusion product ³He is burned in the fully-catalyzed D reactor.

The relationships among the relative power fractions for the fuel cycles are not much different for the FRM than for the tokamak, as is also illustrated in Table I.

Table I. Power Splits for Tokamaks and FRMs

Power Splits (%)	Power Splits for Tokamaks		
	Semi-cat-D	Cat-D	D- ³ He
Leaking Particles	.20	.26	.33
Bremsstrahlung	.26	.25	.46
Cyclotron Radiation	.11	.11	.16
Neutrons	.43	.38	.05
Power Splits (%)	Power Splits for FRMs		
	Cat-D	D- ³ He	
Leaking Particles	.25	.24	
Charged Fusion Products	.19	.39	
Bremsstrahlung	.18	.26	
Cyclotron Radiation	.03	.08	
Neutrons	.35	.03

Fig. 1. Maximum plasma current and toroidal fields for alternate fuel tokamak reactors.

For purposes of technology considerations discussed in the next section, the most important parameter is the fraction of total power carried by neutrons. This is relatively constant for the various cases discussed above, thus it is possible to examine certain first wall/blanket/shield problems without specifying a particular type of device. This is the general approach taken in the next section when the fuel cycles are characterized in terms of neutron power fractions by the following representative values:

D-T ~ 80%
D-D ~ 40%
cat D-D ~ 40%
D-³He ~ 1%

Technology Considerations

This section describes some key technology considerations of alternate fusion fuel cycles including first wall design considerations, shielding requirements, fuel cycle considerations and environmental considerations of activation products and tritium handling.

Materials and First-Wall Considerations

An analysis of the impact of alternate fuel cycles on materials selection and design considerations for the first-wall system has been carried out. The investigation has focused on: (1) limitations of the energy deposition on the surface of the first wall, (2) sputtering and erosion of the first wall by plasma particles, and (3) the importance of first-wall surface morphology on the reflectivity of cyclotron radiation.

An important criterion in the design of the first wall for a D-T fueled reactor is the ability to withstand the high heat fluxes deposited on the surface of the first-wall system. In a D-T fueled reactor that operates without a divertor, transport and radiation heat loads equivalent to approximately 25% of the neutron wall loading are deposited in the surface regions of the first-wall system. Based on the estimated power splits given in Table II for D-T, D-D, and D-³He fuel cycles, the total allowable wall loading has been calculated for a maximum surface heat flux of 0.6 MW/m². This value was established as the design limit for a reference stainless steel first wall of a D-T fueled reactor. As indicated in the table, the total wall loadings for the D-D and D-³He fuel cycles are considerably less than that for the D-T fuel cycle if allowable surface heat flux is a limiting criterion.

Physical sputtering of the first-wall materials has been identified as a major impurity source for a D-T plasma. In general, the sputtering source term for D-T reactor analyses have been based on sputtering yields for a characteristic edge temperature, typically 50-1000 eV. If the characteristic edge temperature for an alternate fuel cycle is higher than that for a D-T cycle because of the higher plasma central temperature, the sputtering yields for most wall materials will increase. The importance of any increase is dependent on the effect of the impurity on the plasma performance. Since the sputtering yields are produced primarily by the fuel ions, the masses of the fuel ions are important. For the same ion flux to the wall, the D-D fuel should produce a slightly lower yield than D-T, whereas the D-³He should produce a significantly higher yield because of the higher mass of ³He. Other fuel cycles with higher mass ions would produce correspondingly higher sputtering yields for the same incident flux.

Table II. Comparison of Power Splits and Allowable Wall Loadings for Alternate Fuel Cycles

Power Split (%)	D-T	D-D	D- ³ He
Neutrons	80	38	5
Radiation	8	35	63
Transport	12	26	32
Total Wall Loading			
(P _w (max) = 0.6 MW/m ²)	3.0	0.97	0.63

The surface morphology of the first wall is important since the reflectivity of the cyclotron radiation from the wall will affect the plasma energy balance. Although data for specific conditions anticipated are limited and further work is warranted, some general observations can be made. Macroscopic surface roughness, for example that caused by blistering, should not significantly affect the reflectivity of the relatively short wave-length cyclotron radiation. In general, the electronic structure of metallic surfaces should provide superior reflectivity compared to that of insulators. This property may be important in the selection of viable first wall materials.

Neutronics Analysis

The nuclear analysis is oriented toward identifying the design impact of alternate fuel cycles in terms of (1) first wall/blanket nuclear performance, (2) reactor shielding performance, and (3) environmental effect.

Table III compares nuclear response rates in the first wall for the following four fuel systems: breeding DT(DT70/DT60), nonbreeding DT(DT00), cat-D and D-³He. It is found that the cat-D systems yield relatively high nuclear heating and atomic displacement rates when compared to the D-T systems that have a neutron wall load as much as twice that of the cat-D system. On the other hand, the radiation damage due to gas production is more severe in D-T systems because the associated damage cross sections have high threshold energies, so that the damage processes are very sensitive to the source neutron energies. There is a substantially smaller radiation damage burden on the first wall in the lean-D-³He systems. In particular, the gas production rates in the D-³He systems are reduced by more than two-fold relative to the D-T systems.

Figure 2 shows the variation of the nuclear dose in an epoxy-base magnet insulator as a function of the bulk shield thickness for the four candidate fuel systems. The dose variation is represented as a plant lifetime in terms of MW-yr/m² of the total integral wall load. The dose limit criterion assumed is 5 x 10⁹ rad in the insulator. As an example, for a total wall loading of 1 MW/m² and a 30 year lifetime, D-³He case would require about 70 cm of shielding compared to about 1 meter for the cat-D and nonbreeding D-T case. A D-T breeding blanket with shield would be about 1.5 meters thick.

Figure 3 represents another aspect of the design impact of the different fuel cycle systems. Shown in the figure is the biological hazard due to

Table III. Nuclear Radiation Response Rates at the First wall

			Fuel Cycle			
			DT70/60	DT00	Cat-D	D- ³ He
Total Wall Load (MW/m ²)			1.0	1.0	1.0	1.0
Neutron Wall Load (MW/m ²)			0.8	0.8	0.4	0.01
Nuclear Heating (MW/m ³)						
Type 316 Stainless Steel	Neutron		3.66	3.62	1.73	0.041
	Gamma		5.08	7.28	4.25	0.126
	Total		8.74	10.90	5.98	0.166
V-15Cr-5Ti Alloy	Neutron		1.58	1.80	1.00	0.028
	Gamma		2.68	4.30	2.76	0.088
	Total		4.26	6.10	3.76	0.117
Ti4381* Alloy	Neutron		1.57	1.66	0.87	0.023
	Gamma		2.34	3.49	1.89	0.052
	Total		3.91	5.15	2.76	0.075
Gas Production (appm/yr)						
Type 316 Stainless Steel	Hydrogen		451	427	190	4.0
	Helium		122	117	50	1.0
V-15Cr-5Ti Alloy	Hydrogen		214	205	87	1.7
	Helium		50	49	21	0.4
Ti4381* Alloy	Hydrogen		174	169	72	1.4
	Helium		107	105	45	0.9
Atomic Displacement (dpa/yr)						
Type 316 Stainless Steel			9.10	10.3	7.08	0.24
V-15Cr-5Ti Alloy			8.76	10.5	7.43	0.26
Ti4381* Alloy			**			

* Ti-4Al-2.5V-8Sn-0.5Si

** Currently not available.

inhalation (BHP_{air}) associated with the blanket/shield designs based on use of stainless steel structural material. One finds that beyond 30 yr, after reactor shutdown, the BHP_{air} in the cat-D systems, which have softer neutron spectra than the D-T system, exceed that of D-T. This is due to the fact that many of long-term radioactive isotopes, ⁶³Ni for instance in this case, are induced to an appreciable degree through (n,γ) reactions which are favored by low-energy neutrons. The results presented in Fig. 3 indicate how one has to reduce the d(d,³He)n[2.45 MeV] from the fuel cycles involving deuterium, in order to fully exploit "clean" advanced fuel reactor designs.

Fuel Cycle Considerations

Fuel cycle scenarios have been developed for cat-D-D and D-³He fusion reactors. A schematic of the fuel processing cycle for catalyzed-DD is illustrated in Fig. 4. Whether the reactor has a divertor or not, the exhaust from the plasma will eventually

be pumped by the vacuum system, most likely by compound cryopumps. Upon regeneration of the vacuum pumps, the fuel is then processed for chemical purification, isotopic enrichment, storage, and refueling. The chemical purification subsystem is designed to remove all condensable impurities (CD_x, D₂O, N₂, etc.). The helium is then separated from the D₂ fuel by a falling film condenser. The possibility exists that the helium, which is pumped separately, may be recovered from the vacuum system such that it is sufficiently free of tritium that it can be processed directly. The helium is then isotopically enriched and ³He is a product, which is a valuable fuel for a D-³He reactor. Isotopic separation of helium isotopes appears to be straightforward, using cryogenic distillation.^{2,3} Separation factors are reported higher than for cryogenic distillation of hydrogen isotopes.⁴ The hydrogen isotopes are thus isotopically distilled at ~ 20 K. It is necessary only to remove a small amount of the protium as HD from the top of the column, as the tritium is recycled as fuel. Qualitatively, the components of the processing cycle are very similar to a

Fig. 2. Blanket/shield thickness for fusion fuels.

Fig. 3. Biological hazard potential in air for fusion fuels for stainless steel structure.

D-D(CAT)

Fig. 4. Fuel cycle schematic for cat D-D system.

D-T fuel processing cycle⁵ with the additional feature of isotopic enrichment of helium.

The D-³He fuel processing cycle (Fig. 5) is quite similar to that of the D-D cycle, except that ³He is used as a fuel and tritium is not recycled. The hydrogen isotope separation unit then must separate D-T as well as HD. Although deuterium is plentiful, ³He is not. Potential sources of ³He will be discussed.

D-³He

Fig. 5. Fuel cycle schematic for D-³He system.

The fuel reprocessing parameters and costs for D-T, cat-DD and D-³He cases for a reference 2500 MWh reactor are shown in Table IV. The D-T and cat-DD cases have comparable vacuum pumping requirements, which is a major cost item in the fuel cycle system. The D-³He case has reduced hydrogen pumping requirements but significantly increased He pumping requirements resulting in higher vacuum system costs. The total fuel cycle system costs are very similar for all cases.

As noted previously, deuterium is abundant while ³He is not. Potentially available U.S. resources of ³He are estimated to be 200-500 kg.⁶ A 2500 MWh D-³He reactor would burn 330 g/day or 120 kg/yr. Since natural resources of ³He are not sufficient, the fuel must be supplied either from a D-D reactor or from decay of tritium.

A 2500 MWh reactor burning no ³He will produce 244 g of ³He per day. This is not sufficient to supply one comparable D-³He reactor. Further, since some ³He will be burned in the cat-DD reactor, the support ratio will be less than ~ 0.5.

Another potential source is tritium decay. To produce 120 kg/yr of ³He, a tritium inventory of 2150 kg is required. A storage facility for this amount of tritium is estimated to cost 200 million dollars. Such large inventories of tritium are clearly a major concern. The source of tritium production could be from D-T reactors. Assuming the excess tritium breeding ratio is 0.1, it would require about 170 D-T reactors to produce enough ³He by tritium decay for one D-³He reactor of the same power level. This obviously is too low a ratio of D-³He to D-T reactors to be of any interest. It is clear that utilization of D-³He requires the use of D-D reactors as a source of ³He, and even then one will probably require at least two D-D reactors for every D-³He reactor of the same power level.

Safety Considerations

The presence of tritium is one of the safety hazards under consideration. The total tritium inventory varies depending on the fuel combination involved. A typical 2500 MWh D-T system would require 8000 g of tritium inventory, cat-DD 100 g, and D-³He only 10 g. Of those totals the amount of tritium considered to be vulnerable is 2000 g for D-T, about 20 g for cat-DD and about 2 g for D-³He. The vulnerable positions are that part of the tritium which could conceivably be released in the case of an accident. Even with inventories as low as a few grams, some type of emergency tritium cleanup system will be required.

The induced radioactivities in the first wall and the structural materials in the blanket and shield are the other major sources of radioactivity presently being considered. Several candidate materials have been considered for all three fuel systems. The candidate materials considered are 316 stainless steel, Ti 4381 and V-15Cr-5Ti alloys. The normalized activities (Ci/MWh) of these alloys have been calculated for preliminary alternate fuels design conditions both during reactor operation (from 1 second to 2 years) and following reactor shutdown (from time zero to 1000 years).

The atmospheric (x/Q) dispersion of tritium and of structural material was examined for a hypothetical accident based on a short-term uniform release from a point source. A simple model is utilized in order to compare the relative external dose from the candidate alloys dispersed in a Gaussian plume. The impact of tritium leaking from the reactor building following a

Table IV. Fuel Processing Requirements for Alternate Fusion Fuel Cycles

Thermal Power - 2500 MW(th)

	Fractional Burnup		Fuel Burned Per day (g)			Required Pumping Speed (m ³ /s)		Cost (\$M)				
								Tritium Re-covery	Vacuum System	Fuel Processing	Emergency Air Detritiation	Total
	D	³ He	D	T	³ He	HDT	He					
Reference D-T	.05	-	230	345	-	1.8×10^4	9.5×10^2	2.0	13.3	14.8	7.1	37.2
Cat-DD (burn 1/2 of ³ He)	.05	.50	654	*	89.2	2.6×10^4	9.8×10^2	0.0	31.4	4.8	4.9	40.9
D- ³ He (lean D)	.10	.05	221	-	331	4.6×10^3	1.8×10^4	0.0	27.9	4.0	3.9	35.8

* Recycled.

hypothetical accident was also calculated using the same methodology.

The relative external doses due to a given structural material in different advanced fuel systems being volatilized by a hypothetical reactor accident and its leaking out of containment is shown for 316 stainless steel in Fig. 6. The same information could have been plotted for the other candidate materials, but they are generally similar, except the values are approximately an order of magnitude lower for the vanadium alloy.

Based on the results, the cat-DD system produces higher total external doses following a hypothetical accident than does a D-T system; while the D-³He system produces much lower doses. The relative doses for D-T compared to D-³He are about 10 times as great for 316 SS, 35 times for the vanadium alloy and about 60 times for the titanium alloy. The impact of the absolute value of these doses relates to the size of exclusion boundary that would be required around a fusion power plant.

Fig. 6. Relative external doses for alternate fuel system.

The relative effect of the vulnerable tritium inventory in the reactor building was also calculated for each of the advanced fuels systems. The normalized activities of tritium would be 7.7×10^3 Ci/MWth for D-T, 77 Ci/MWth for cat-DD, and 7.7 Ci/MWth for D-³He. The activity of tritium when compared to the activity of structural material is generally quite small. The external whole body tritium doses from the plume at 200 meters from the reactor and at one hour following an accident would be 1.9×10^{-9} rem/s-MWth for D-T, 1.9×10^{-11} for cat-DD, and 1.9×10^{-12} for D-³He. The whole body doses due to tritium compared to the doses due to structural materials are small (on the order of 0.10 percent).

Conclusions

Based on this initial study, the following preliminary conclusions are developed:

1. Radiation damage parameters (atomic displacements and gas production rates) are reduced by about two orders of magnitude for D-³He compared to D-T for the same total wall loading. D-D offers about a factor of two reduction compared to D-T.
2. For cyclic fusion concepts, surface heat loading limitations reduce the total wall loading by a factor of three for D-D and a factor of five for D-³He compared to D-T. (Alternate fuels are probably better suited to steady-state fusion concepts.)
3. Surface roughing will probably not significantly reduce reflection of cyclotron radiation; there is very little experimental data.
4. The use of low-Z, non-conducting surfaces (e.g., oxide coatings) may not be acceptable, thus, perhaps, making impurity control more difficult for alternate fuels.
5. Shielding requirements (e.g., thicknesses) are reduced by about a factor of two for D-³He compared to D-T. There is little advantage for D-D compared to D-T for the same total wall loading.
6. Maintenance for any deuterium-based fuel cycle will probably have to be by remote methods.
7. Long-term BHP for activated material for D-D is similar to D-T. After about 100 years, the difference between D-³He and D-T is small for stainless steel structural materials.

8. The tritium throughput for D-D is about a factor of 15 less than for D-T for the same total power. For cat-DD, the reduction is 100 over D-T. For D-³He, the reduction is about 1000.

9. There is little difference in the total cost of the fuel handling/vacuum system between D-T, D-D and D-³He.

10. The only feasible source of ³He seems to be a D-D reactor; support ratios of D-³He to D-D will be at least 1:2.

11. D-³He reactors will result in lower doses (10-100X lower) under possible reactor accident conditions compared to D-T, but D-D reactors may produce higher total external doses compared to D-T.

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SHIELDING AND MAINTAINABILITY IN AN EXPERIMENTAL TOKAMAK

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1. Introduction

It has long been recognized by the fusion community that provisions must be made for remote maintenance for all tokamak reactor components inside the toroidal field (TF) coils (e.g. first wall, blanket, bulk shield, etc.). This does not imply, however, that all maintenance-related operations have to be carried out by remote means. For example, there is much equipment located in the reactor building outside the TF coils that has to be maintained. Furthermore, there are many operations such as planning, viewing, connects/disconnects, and adjustment of maintenance equipment positions that can be made inside the reactor containment building exterior to the TF coils.

Therefore, a critical issue that has to be resolved in developing a complete maintenance plan for the next generation of experimental tokamak reactors such as ETF and INTOR is whether there is a net benefit from designing the reactor for human access into the reactor floor within a short time (a few hours to one day) after shutdown so that some maintenance operations can be carried out in contact and/or semi-remote mode. From maintenance plan standpoint, the benefit from personnel access is primarily a reduction in the total downtime and hence an improvement in the reactor availability. The principal penalty is an increment in the capital cost for the additional shield that is required above and beyond that necessary for radiation protection of the reactor components.

This paper presents the results of an attempt to develop an understanding of the various factors involved. This work was performed as a part of the task assigned to one of the expert groups on the International Tokamak Reactor (INTOR). However, the results of this investigation are believed to be generally applicable to the broad class of the next generation of experimental tokamak facilities such as ETF.

The shielding penalties for requiring personnel access are quantified in Sec. 2. This is followed by a quantitative estimate of the benefits associated with personnel access in Sec. 3. Section 4 compares the penalties to the benefits and develops conclusions and recommendations on resolving the issue.

2. Penalties of Personnel Access

In the absence of penetrations, a properly designed magnet (bulk) shield is capable of reducing the biological dose in the reactor hall to <1 mrem/hr within 24 hr after shutdown. This dose level permits a person to work continuously (40 hr/wk) without exceeding the ICRP

guides for occupational exposure. The presence of major penetrations such as those for neutral beams, divertors, and vacuum pumping results, however, in substantial radiation streaming into the reactor building.¹ This will result in strong activation of the equipment located at the outer end of the penetration (e.g. beam injector components and walls), reactor building liner, reactor support frame, overhead cranes, poloidal coil, located outside the bores of the TF coils, and all equipment located in the reactor building. For the preliminary INTOR design concept in the presence of 0.8-m diameter beam ducts, detailed calculations resulted in the following estimates of the biological dose in the reactor building.

Time After Shutdown	Dose (mrem/hr) Outside the Injector Walls
0	6×10^6
12 hr	4×10^4
1 day	2×10^4
1 wk	1×10^4
1 mo	1×10^4
6 mo	3×10^3

Such a biological dose is too high to permit personnel access for any reasonable period of time.

For personnel access to be feasible, additional shielding must be provided to prevent radiation streaming into components located in the reactor building. Our objective here is to make an approximate estimate of the cost of this additional shield. To do this one must define an acceptable biological dose and a method of shielding.

The ICRP rules limit the maximum individual occupational exposure to 1.25 rem/quarter. This translates into a dose limit of 2.5 mrem/hr to a person working the entire quarter on 40 hr/wk basis. Another quasi-legal principle is to reduce the dose as low as reasonably achievable (ALARA). A measure of environmental impact that is presently receiving much attention is the total cumulative dose to the entire work force. Prudent practices require keeping the total cumulative dose (person-rem) as low as possible. Our evaluation suggests that for the purpose of gaining human access the design target should be to reduce the biological dose in the reactor building to <1 mrem/hr within 24 hr after shutdown.

One theoretical method of reducing the biological dose in the reactor building is to employ materials (for constructing all components outside the bulk shield) that do not transmute under neutron irradiation to radioisotopes with half-lives longer than a few minutes or hours. In practice, there is no suitable material to serve this purpose.

Radiation shielding is the only effective means of

* Author's work performed during stay at Georgia Institute of Technology and supported by the Georgia Power Company.

limiting the biological dose rate in the reactor building. There are two methods of shielding against radiation streaming from penetrations. The first is to employ a shield plug that completely closes each penetration at all times the neutron production is not zero. The principal difficulty here is that the functional requirements of some penetrations require that these penetrations be open during part of or all of the plasma startup and burn times. For example, neutral beam injection during the startup is accompanied with a substantial neutron yield from the heated plasma. Furthermore, if INTOR has to be driven, beam injection will also be necessary during the peak power portion of the plasma burn. It is not clear at present whether vacuum pumping (for the plasma chamber) during the plasma burn will be necessary. Holes for divertors, if a divertor is used, will of course have to remain open during the plasma burn.

The only shielding option whose viability is not subject to uncertainty is a local exterior shield that surrounds all penetrations and functional end equipment (i.e. equipment located at the outer end of the penetration such as a vacuum pump, beam injector, or a detector). This shield is always required for radiation protection of reactor components, but the magnitude of radiation attenuation it has to provide is larger if personnel access into the reactor building after shutdown is to be permitted. Multidimensional transport calculations and nuclear analysis effort carried out for INTOR shows that the largest and most costly requirement for personnel access is the additional shield around the functional end equipment. The largest portion of this shield is that required for the beam injectors because of their large size and the correspondingly large exterior surface area that has to be covered. A reactor system in which the opening of the neutral beam duct at the first wall is 0.8 m in diameter was analyzed. The beam duct was assumed to have a uniform cross section and connected to the beam injector at a location that is 3 m away from the first wall. The size of the beam injector was taken to be 10 m long, 6 m wide, and 6 m high. Based on previous studies for experimental power reactors, this system is roughly within the range of what can be expected for INTOR. For this system, it was found that ~50-cm thick shield providing radiation attenuation and induced activation characteristics similar to that of a mixture of stainless steel and boron carbide plus lead need to surround the exterior surfaces of the beam injectors in order to reduce the biological dose rate in the reactor building below 1 mrem/hr within 24 hr after shutdown. This biological dose rate varies with time after shutdown as follows (for a neutron wall load of 1.6 Mw/m²):

<u>Time After Shutdown</u>	<u>Dose (mrem/hr)</u>
0	350
12 hr	14
1 day	0.8
1 wk	0.3
1 mo	0.25
6 mo	0.09

These calculations were made for an operating period of 3 mo, but it should be noticed that the level of radioactivity reaches ~50% of the maximum after only a few days of operation.

An important conclusion obtained in the course of this assessment is that the use of the so-called "low-activation" materials such as aluminum are not useful for shielding components in the reactor building for several reasons: (1) the activity of ²⁷Na produced in aluminum (half-life ~15 hr) requires several days of decay to reach an insignificant level; (2) aluminum is

a poor neutron attenuator; therefore, for the same thickness of material a higher neutron leakage will obtain, leading to higher activation of building liner, structural torque frame, etc.; and (3) aluminum is a poor attenuator for gamma rays; therefore, more decay gammas from deeper regions of the shield will reach its surface and escape into the reactor building.

Penetrations with cross-section area smaller than those of the neutral beam will require less shield (smaller thickness) around their functional end equipment. The ducts for vacuum pumping (plasma chamber evacuation) will have roughly the same cross-section area as the ducts for neutral beams except that the ability to bend the vacuum pumping ducts significantly reduces radiation streaming.

Table I shows a summary of the cost of shield required around the functional end equipment of the various types of penetrations. Approximately 80% of this cost is attributed to the requirement of personnel access. These cost estimates are based on a unit cost of \$70,000/m³ of the shield (~12\$/kg for physical density of 5800 kg/m³). This cost includes material, fabrication, and cooling expenses. Other relevant assumptions are indicated in the table.

The largest cost item is that of the shield around the beam injectors because of the large surface area around each injector. The shield for the vacuum pumps is included in Table I and it is assumed that no divertor is incorporated. The total cost of the additional shield strictly required for permitting personnel access is ~89 M\$. This is the cost penalty of personnel access.

It must be noticed that this shield cost penalty is very sensitive to certain technology choices as indicated in Table II. For example, if a divertor is incorporated the divertor shield increment attributed to personnel access is estimated to be ~38 M\$. The use of rf heating instead of neutral beam reduces the cost of shielding significantly for several reasons: (1) the feasibility of making bends in rf ducts; (2) larger power density per unit area at the first wall makes rf ducts smaller than those for neutral beams; and (3) the flexibility in geometrically locating the rf generator. Notice that the use of negative ion deuterium sources (all calculations above are based on D⁺) will also result in a significant reduction in the cost of the shield. If rf heating is used and no divertor is employed the total cost penalty of shield for personnel access is only 48 M\$ compared to 120 M\$ for the neutral beam/divertor case.

3. Incentives for Personnel Access

In Sec. 2 the penalties of the additional shielding to permit personnel access were examined. In this section the benefits that can be obtained from enabling maintenance personnel to enter the reactor building within 24 hr after shutdown are assessed. The two general maintenance scenarios considered here are: all-remote and contact/partial-remote. The maintenance scenario has been developed in enough detail to provide estimated failure frequency and maintenance plan data for major reactor components.

The areas of the reactor building that have been considered for contact or partial-remote (this can also be described as contact assisted remote) maintenance include all accessible components or systems that are external to the reactor main (bulk) shield. This generally includes instrumentation and control diagnostics, cooling systems, power leads, and the tritium recovery system as well as a portion of the maintenance operations on many components inside the shield. Both the all-remote and the contact/partial-remote maintenance

TABLE I. Cost of Shield for Personnel Access

Component with Penetration	Cost of Shield Around End Equipment (Based on 70 K\$/m ³)	Cost of Portion of Shield for Personnel Access	Remarks
Neutral beams	80 M\$	65 M\$	7 injectors (each is 10 × 6 × 6 m ³)
Diagnostic	12 M\$	a	50 detectors (each is 1 × 1 × 1 m ³)
Vacuum pumps	21 M\$	16 M\$	30 vacuum ducts (2 pumps per duct)
Others	10 M\$	8 M\$	
TOTAL	123 M\$	89 M\$	

^aThe shield around the instrumentation and control "end equipment" appears to be always required for radiation protection of this equipment from background radiation.

TABLE II. Effect of Technology Choices on Cost of Shield for Personnel Access

Option	Total Cost of Shield for Personnel Access
Neutral beams, divertor ^a	120 M\$
Neutral beams, no divertor	89 M\$
rf (or D ⁻), no divertor	48 M\$

^aThe cost of the shield for the divertor can be much larger than what is assumed here, depending on the particular divertor concept considered.

external to the reactor main (bulk) shield. This generally includes instrumentation and control diagnostics, cooling systems, power leads, and the tritium recovery system as well as a portion of the maintenance operations on many components inside the shield. Both the all-remote and the contact/partial-remote maintenance schemes are strongly design-sensitive and will require continuing attention during the machine conceptual design phase for proper implementation.

3.1 Contact and Partial-Remote Maintenance

The three major maintenance operation methods are contact, all-remote, and partial-remote. A wide range of partial-remote operations can exist between the contact and all-remote extremes and this is the area in which many of the INTOR maintenance operations may fall if adequate shielding is provided.

Contact maintenance is defined as the use of direct hands-on or conventional techniques including hand-held and guided tools to repair or maintain components or systems. Maintenance operations may include: inspection, damage assessment, and repair for forced outages as well as scheduled operations in connection with preventive maintenance.

All-remote maintenance operations are those which must be accomplished without the benefit of any human assistance within the immediate area of operation. The actual tasks are performed by manipulators (some with force feedback) or by special tools and fixtures which may be guided or assisted by manipulators or cranes. These operations generally require 8 to 500 times longer to perform the same task than with contact work. Table III, taken from a recent remote systems seminar,² compares the time to perform various remote tasks with contact work.

In the case where some personnel access is available (e.g., during the period when all shielding is in

TABLE III. Comparison of Time Required to Perform Typical Tasks

	LASL ^a	MIT ^b	NASA ^c	MBA ^d	CEA ^e
Two-armed man					
Unsuited	1	1	1	1	1
Suited	-	-	-	8	-
Mechanical M/S					
Two-arm	8	8-10	8	8	2-8 ^f
One-arm	16	-	16	-	-
One-arm EMM					
Position control ^g	80	40-50	64	55	10-30
Switch control ^g	480	80-100	640	-	50-100
Crane -					
Impact wrench ^g	>500	>100	>600	>500	>100

^aBased on installing bolt and lifting sling.

^bBased on eight definitive tasks.

^cBased on analysis and literature survey.

^dBased on typical valve changeout.

^eBased on variety of extensive tasks using different manipulators.

^fMultiply by 1.4 for untrained operators.

^gNo force feedback.

place, but preparations are in progress for unshielded remote work) partial-remote maintenance may be carried in order to minimize the time lost with all-remote operations. Varying amounts of contact maintenance operations can be performed on different components depending on the design of the equipment and the task involved. Those operations sometimes called "semi-remote" (including long-handled tools and temporary or shadow shields) are included in the partial-remote category for purposes of this analysis.

3.2 Utilization of Partial-Remote Maintenance on INTOR

Operations at fission reactors provide many examples of partial-remote maintenance. The use of long-handled tools in water-shielded pools allows significant savings in time and optical equipment. Temporary or shadow shields and special tools are frequently utilized for maintenance tasks such as changing filters.

An example of a partial-remote maintenance task on a fusion reactor with adequate biological shielding to provide personnel access soon (say 24 hr) after shutdown is as follows: An ion source on a neutral beam injector has failed. The neutral beam assembly is shielded but the ion source is separately shielded and detachable

along with its shield. Electrical and cooling services are supplied through the shield and have conventional external disconnects. As soon as allowable after shutdown, maintenance personnel enter the reactor building and disconnect the electrical and cooling supplies as well as the ion source shield structural fasteners. Tooling and fixtures are set up locally for the actual source removal operation. The personnel temporarily move to a safe distance or behind a shadow shield while the remote or semi-remote equipment removes the source and shield assembly and reinserts a new source and shield assembly. A temporary shielding cover is placed over any exposed radioactive portions of the removed ion source and shield assembly. After shielding is in place, maintenance personnel can reapproach the neutral beam and complete the ion source hookup. Alternatively, the entire change-out could be accomplished by personnel utilizing shadow shielding and long-handled tools and/or cranes. About 25% of the original remote work remains.

Significant savings in time, complexity of reactor components, disconnects, and remote equipment can be realized when compared to an all-remote operation. Many other opportunities exist for more efficient operations by use of partial remote operations on shielded reactor components such as cryopumps, control and instrumentation diagnostics, divertors, etc.

3.3 Utilization of Contact Maintenance on INTOR

Obviously, the greatest maintenance time savings occur when an all-remote operation can be replaced by a contact maintenance operation. If areas exterior to the reactor shield can be made available by personnel access about 24 hr after reactor shutdown, a number of very difficult remote operations are eliminated.

Control and instrumentation diagnostic components external to the shield represent a class of maintenance tasks which can be rather trivial under a contact maintenance scenario but which will create challenging design and operational problems if remote manipulations are required. Replacement or repair of degraded optics and vacuum isolation valves are typical tasks. Diagnostic modification and realignment are tasks which generally require excellent viewing capabilities and an extremely delicate touch. Examples are laser optics and photo-diode replacements. Considerable equipment and time savings are possible if these maintenance jobs are contact. Many reactor control or instrumentation components require shielding for self-protection greatly complicating the inspection, repair, and replacement tasks with all-remote operations.

Breakdown of remote maintenance equipment can also create serious operational problems and add to reactor downtime. These breakdowns will usually occur during remote operations and therefore require special recovery equipment and design provisions. Each remote operation must be examined to identify equipment failure modes. Generally, a shielded area must be provided to which failed remote equipment can be moved for hands-on maintenance. Failing this, additional remote equipment must be provided. Reducing the number of remote operations required obviously will reduce these tasks.

3.4 Inspection, Damage Assessment, and Repair

An important issue, which is difficult to quantify, relates to the amount of time required to identify reactor equipment problems. Closely related is the inspection of components to head off failure. When these tasks must be accomplished remotely, they will be forced to rely on remote viewing devices and considerably augmented instrumentation.

One source³ states that localization and isolation

functions related to corrective maintenance of electronic equipment generally account for 60% of the total expended maintenance time. The proportion of downtime required to troubleshoot mechanical equipment is also a relatively large percentage of the total repair time.

Accomplishing the above tasks remotely will add significantly to the time involved. Television viewing is required in most cases since reactor equipment will be located in obscured positions and at relatively large distances from any shielding viewing windows that could be provided in the reactor building. TV systems should certainly provide depth perception in order to compensate the lack of all other human senses in a remote environment. With only remote viewing and instrumentation as an aide to fault identification and troubleshooting, considerable additional maintenance time should be expected.

Remote repair operations can actually create additional maintenance work due to unexpected incidents such as twist-off of bolts, cross-threading of fasteners, and the dropping of delicate parts. Many remote handling design techniques exist to help offset these problems, but the design solutions are often costly.

3.5 Impact of Use of Contact/Partial-Remote Maintenance

In order to quantify some of the expected benefits of utilizing contact/partial-remote maintenance where possible on the proposed INTOR reactor, an analysis of the data for an all-remote maintenance scenario developed earlier for INTOR was undertaken. Components, downtimes, manpower estimates, and personnel exposure times were examined in as much depth as possible at this early stage of the concept.

Maintenance plan data are categorized into scheduled and unscheduled maintenance periods depending on expected failure frequency, importance of the equipment to continued reactor operation, and redundancy provided. A preventive maintenance scenario of scheduled quarterly 28-day downtimes and scheduled 2-day weekly downtime (for 32 wk) is then pursued.

The critical path quarterly maintenance items are shown in Table IV. All-remote downtimes are compared with contact/partial-remote downtimes expected. Contact/partial-remote times are calculated from all-remote time estimates using divisors derived from remote handling experience, including data shown in Table III. The divisors chosen include considerable conservatism to ensure that time savings are not overestimated. The basic factor chosen is that a 5 to 1 reduction exists between an all-remote job and an all-contact one.

The blanket recoating operation all-remote downtime, estimated as 18 days, is divided by 1.43 to reflect the fact that only a portion of this operation can be done in the contact mode. This implies that 438% of the remote work can be replaced by contact work. The downtime for the new contact/partial-remote operation is therefore 12.6 days and the time saved is obviously 5.4 days. Of the 12.6 days now required for this task, 1.35 days (11%) are accomplished by contact work. Thus, a major portion of the downtime on this job is still accomplished in the remote mode. These proportions were chosen from a preliminary analysis of the operations for this maintenance task.

A similar approach is used for the second critical path item shown, replacing limiter plates. A portion of these first two tasks shown in Table IV is assumed to be accomplished in parallel and the entire experiment exchange task is assumed to be completed while the

TABLE IV. INTOR Quarterly Preventative Maintenance Downtime (Critical Path)

	All Remote		Partial-Remote
	Total Downtime (days)	Net Downtime (days)	Net Downtime (days)
Blanket - withdrawn four sectors and recoat walls	18	18	12.6
Limiters - replace 8 plates from 2 sectors	8	6	3.6
Experiment exchange - replace 3 first-wall/blanket modules	8	0	0
Maintenance equipment downtime (average)		0.4	0.1
Shutdown + startup		1.0	1.6
TOTALS		25.4	17.9
Quarterly downtime assumed		28.0	18.0

other tasks were in progress. A total of 19 other quarterly or annually scheduled maintenance items are identified, but all are of shorter duration than critical path items and can be performed in parallel. Thus, the net downtimes shown are used to calculate contact/partial-remote times for these pacing items. Finally, additional downtime is allowed for maintenance equipment failures and shutdown and startup of the reactor. A total quarterly downtime of 28 days for the all-remote mode and 18 days for the contact/partial-remote method is assumed from these estimates.

The weekly maintenance plan includes two items: neutral beam ion sources and divertor bombardment plates. Both systems can be serviced in parallel during the one-day downtime available under the all-remote maintenance plan. With the contact/partial-remote mode, these weekly items are completed in 9.6 hr using a divisor of 2.5. As discussed in the example previously given, ~25% of the original remote work remains. The total weekly downtime is 48 hr for either maintenance mode.

Since no downtime reduction is predicted for weekly scheduled maintenance under the contact/partial-remote scheme, the savings per quarter is 10 days for a total of 40 days per year.

In a similar manner, the estimates of manpower for the all-remote maintenance plan were used to calculate manpower saved when using the contact/partial-remote maintenance scheme. Since contact work in the reactor building will add to personnel radiation dose, the number of contact man-days is also calculated.

The savings in the unscheduled downtime per operating year is estimated as 80 days. Manpower is saved at the rate of 416 man-days per operating year (24-hr days). Contact work accomplished will require 104 man-days per operating year.

4. Summary, Conclusions, and Recommendations

Two maintenance plans were analyzed for INTOR. The first employs all-remote maintenance operations and requires no access to the reactor building at any time

during the useful life of the facility. The other is based on a partially-remote plan that combines remote, semi-remote, and contact operations in specific maintenance tasks. The semi-remote and contact operations are limited to components that are located outside the magnet bulk shield. Examples are instrumentation and control equipment, coolant lines, and power leads. Maintenance tasks on components inside the bulk shield envelope were assumed to be carried out remotely with the benefit of semi-remote or contact operations for some of the preparatory work (e.g. coolant lines and power leads disconnects/reconnect) when all shield is in place. A reasonably conservative, but preliminary, analysis shows that for most of the critical path maintenance tasks ~63% of the operations in the all-remote scenario remain as fully remote while the other 37% can be performed by contact or semi-remote means.

A major requirement of the partially-remote plan is for maintenance personnel to gain access into the reactor building a short time after shutdown to work in the reactor hall regions located outside the toroidal-field magnets. Multidimensional radiation transport calculations and nuclear analysis shows that additional shield is required around all functional equipment located at the outer end of all penetrations such as those for beam injection, vacuum pumping, divertors, and diagnostics. The amount, characteristics, and cost of this additional shield to reduce the biological dose rate in the reactor building to <1 mrem/hr at 24 hr after shutdown were determined.

It was assumed that the radiation level is the only factor that determines the feasibility of human access into the reactor building. Therefore, it was implicitly assumed that: (1) the environment of the reactor building is not contaminated with tritium, or more specifically maintenance personnel are not required to use breathing apparatus that could severely reduce their productivity; and (2) the environment of the reactor building during maintenance periods is normal air which implies that either the reactor building is not evacuated under normal operation or, alternatively, it can be repressurized within the 24-hr period allowed for radioactive decay.

Table V presents a summary of the significant differences between no special shield/no personnel access plan and the partially-remote/additional shield/personnel access plan. The conclusions from this comparative assessment are:

(1) The partially-remote plan significantly reduces the maintenance downtime (scheduled plus unscheduled) by ~120 days per operating year relative to the all-remote plan. Therefore, the estimated reactor availability of 0.285 for the all-maintenance plan increases to 0.38 for the partially-remote plan. This 33% improvement in availability is the most significant potential benefit of the partially-remote/additional shield/personnel access scenario.

(2) The economic penalty of the partially-remote plan is the capital cost of the additional shield around the beam injectors, vacuum pumps, and other functional equipment located at the outer end of penetrations. Assuming neutral beams are used for plasma supplementary heating and no divertor is incorporated the cost of material, fabrication, and cooling of this additional shield is estimated to be ~89 M\$ when the most efficient shielding materials are used. The merits and disadvantages of employing cheaper but less efficient shielding materials such as borated water and concrete have not been evaluated but could possibly reduce the shield cost penalty. If the capital cost of INTOR is in the range of 800 M\$ to 1 B\$, then the 89 M\$ for the additional shield represents an in-

TABLE V. Summary of Significant Differences Between All-Remote and Partially-Remote (with Personnel Access) Maintenance Plans

	All Remote	Partial Remote (with Personnel Access)
Savings in downtime, days per operating year	0.0	120
Reactor availability, %	28.5	38
Manpower requirements:		
Total person-hr per calendar year	57,288	49,443
Radiation person-hr per calendar year	0.0	1,963
Cost items, M\$:		
Capital cost of remote maintenance equipment	50	45
Maintenance labor cost per calendar year	19	16.5
Cost of additional shield for personnel, access with neutral beams and no divertor	0.0	89
Increase in radiation exposure due to personnel access (person-rem per calendar year)	0.0	2.0

crement in the capital investment of the facility in the range of 9% to 11%.

(3) With the lack of an established cost-benefit analysis methodology, there are sufficiently convincing arguments that suggest the use of the ratio of the capital cost to availability as a comparative figure for experimental facilities such as INTOR. The cost per unit benefit for INTOR with the partially-remote/ additional shield/personnel access scenario is 17% lower than that with the all-remote/no-special shield/ no-access maintenance plan.

(4) Another way to quantify the benefits of the partially-remote plan is to examine the impact on the allowable failure frequency and the required redundancy for the individual reactor components. Since for a given failure the partially-remote scenario requires less downtime for repair or replacement than the all-remote plan the failure frequency can be allowed to be higher for the former for a fixed target availability. Alternatively, the required level of redundancy with the all-remote can be substantially reduced with the partially-remote plan. Examining the allowable failure frequencies for individual reactor components in the all-remote plan shows that they can be increased by 40% for the partially-remote plan.

(5) The cost of the additional shield for personnel access is sensitive to particular technology choices. If a divertor is incorporated on INTOR the cost of the additional shield increases from 89 M\$ to 120 M\$. Replacing neutral beams with rf heating reduces the cost of this shield from 89 M\$ to 48 M\$.

(6) Since it is impossible to reduce the biological dose in the reactor building to zero, maintenance, personnel working in the reactor building will receive a radiation dose. The additional shield considered for the partially-remote plan is sufficient to reduce the total cumulative dose to all maintenance personnel to 2 person-rem/calendar year. This is more than two orders of magnitude lower than the radiation exposure to maintenance personnel in current fission facilities.

(7) The partially-remote maintenance plan has favorable impact on reactor facility design requirements, capital cost of maintenance equipment, and labor cost.

(8) Research and development requirements for maintenance equipment are extensive for both the all-remote and partially-remote maintenance scenarios. However, the risks in the maintenance equipment development appear to be somewhat less for the partially-remote plan.

Important recommendations derived from the above conclusions are:

(1) The most attractive maintenance scenario appears to be the one that combines contact, semi-remote, and remote operations so as to achieve an optimum benefit-to-cost ratio for the experimental facility. Finding this optimum should be a key area that must be addressed, in all its aspects, in the INTOR development effort.

(2) The space in the reactor hall inside the reactor containment building but outside the main bulk magnet shield should be designed for personnel access within one day after shutdown. Material choices and design options that achieve this goal at a minimum cost should be comprehensively investigated. The feasibility of shield plugs for the major penetrations should be closely examined.

(3) Alternatives to technology choices requiring very large penetrations such as neutral beams and divertors should be seriously examined.

(4) The tradeoffs between the location of the vacuum boundary and maintenance plan should be investigated.

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SAFETY OF SUPERCONDUCTING FUSION MAGNETS: TWELVE PROBLEM AREAS*

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Summary

Twelve problem areas of superconducting magnets for fusion reaction are described. These are: Quench Detection and Energy Dump, Stationary Normal Region of Conductor, Current Leads, Electrical Arcing, Electrical Shorts, Conductor Joints, Forces from Unequal Currents, Eddy Current Effects, Cryostat Rupture, Vacuum Failure, Fringing Field and Instrumentation for Safety. Priorities among these areas are suggested.

Introduction

This report calls attention to twelve areas requiring further study for the safety of fusion magnets. It is based largely on the experience of the ANL Superconducting Magnet Group. The discussion deals directly with tokamak toroidal field (TF) coils, cryostable, cooled by pool boiling helium, with NbTi superconductor and copper stabilizer. Each of the twelve areas is discussed under the five subheadings: Identification and Definition, Possible Effects on Safety, Current Practice, Adequacy of Current Practice for Fusion Magnets, and Areas Requiring Further Analytical or Experimental Study. Under "Possible Effects of Safety," consequences have been followed as far as the breaking or melting of the current path, excessive thermal stresses, or overpressurization of the cryostat. "Current Practice" refers to current practice as the author is aware of it at Argonne and elsewhere; there has been no extensive literature search or questioning of colleagues at other institutions. Finally, priorities for the recommended experimental and analytical studies are discussed. All of these topics are described in more detail, and with references to related work, in Reference 1.

Quench Detection and Energy Dump

Identification and Definition

A superconducting magnet is said to quench when a section of conductor goes from the superconducting state to the normal state, the normal region generates heat, the heat drives more conductor normal, and the normal region propagates until a large part of the conductor is normal and the current has decayed. Quench analysis is particularly difficult for a large cryostable magnet. On the one hand, such a magnet is designed so that it cannot quench; cryostability requires that there be sufficient cooling and stabilizer available that a normal region will shrink and disappear rather than grow. On the other hand, there are fault conditions that could quench a magnet, and large cryostable magnets have been known to quench.

A quench must be detected early, and the energy of the magnet dumped into an external dump resistor. The discharge voltage is the product of the magnet current and dump resistance; if the discharge voltage is too

high, there is danger of electrical arcing. If it is too low, the dump will last too long; and the conductor can rise to too high a temperature.

Possible Safety Effects

A high discharge voltage can lead to electrical arcing; a slow dump can cause excessive heating of parts of the coil, leading to unacceptable thermal stresses, or cause extensive helium boiling, leading to cryostat overpressurization. A very slow dump or an undetected quench may allow the conductor to heat until it melts or until it decomposes the electrical insulation and arcs over.

Current Practice

A quench analysis, for quenches initiated by various fault conditions, will determine what discharge voltage is required for an acceptable temperature. The coil is designed for that temperature rise and that voltage. To detect a quench, the voltages across two halves of a magnet, which should be equal by the symmetry of the halves, are compared. If they differ by an amount corresponding to the voltage drop across a specified length of normal conductor, the magnet is discharged. As the normal region grows, more and more liquid helium is vaporized. The pressure in the cryostat increases until the pressure relief valve opens, emptying helium from the cryostat. Thereafter the quench propagates more rapidly.

The dump resistor should be permanently connected across the magnet terminals. In case of a quench, the power supply is disconnected and the magnet discharges into the dump resistor; switches between the magnet and dump resistor cannot fail to close. The dump resistor should be of such a size that discharging the magnet will not heat it excessively.

Adequacy of Current Practice

The methods currently used should be adequate, but the means of implementing those methods must be thought out. For example, the protection scheme must assure that the energy of all the TF coils is not dumped in the one coil which quenches. Moreover, the choice of discharge voltage continues to be a compromise between high voltages and high temperatures; but it may be that a safe compromise exists only for high currents, 50,000 A or higher. A quench analysis must be done to choose that compromise, but existing computer programs for quench analysis are decidedly inadequate. Similarly, the quench analysis should determine how quickly a quench must be detected and the discharge started. This information in turn would set the requirements for the quench detection system.

Areas Requiring Further Study

A general computer program for quench analysis should be developed for cryostable magnets; no such program exists today. The program should be sufficiently general that the properties of any particular magnet design could be readily inserted into it. Quench detection, switching, and dump resistor design depend on the actual coil design, and must await the choice of a design.

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Stationary Normal Region of Conductor

Identification and Definition

The previous section explored the consequences of a propagating normal region which does not propagate. In a cryostable magnet, a stationary normal region can arise only from a failure of cooling of that region, either from a massive blockage of the cooling passages to the normal region or from the liquid level falling below the conductor region. In each case the region is characterized by good boiling at each end of the region, preventing its further growth, but poor cooling within the region.

Possible Safety Effects

The consequences of the stationary normal region depend on the temperature difference which develops. If the temperature difference is hundreds of kelvin, the electrical insulation can be damaged, permitting turn-to-turn and turn-to-ground electrical shorts, and possibly an overpressurization of the helium vessel. For intermediate temperature differences, thermal stresses can result leading to possible mechanical damage, structure weakening, or insulation destruction.

Current Practice

No stable normal regions have been observed in operating cryostable magnets. Magnet designs prevent such regions by preventing uncooled regions, i.e. by preventing the liquid level falling below the top of the coils and by providing a network of cooling channels to prevent massive blockage.

Adequacy of Current Practice

The large size of the TF magnets and their orientation in a vertical plane makes the potential problems from stationary normal regions much more serious than those with existing magnets.

Areas Requiring Further Study

The credible length for a normal region is not known; it should be established both for force-cooled conductors and for those cooled by pool boiling. Meanwhile, an analysis should be done to determine the consequences of an uncooled length, for lengths up to a half turn.

Current Leads

Identification and Definition

Probably current leads have been responsible for more superconducting magnet failures than any other cause. Each lead generally consists of two parts: a normal lead from room temperature to liquid helium temperature, and a superconducting lead from the normal lead to the magnet itself. The leads experience the magnetic field of the magnet and so undergo a force, but their location makes them difficult to support. Because of their location outside the coil, they are also subject to excessive heat input and to arcing.

Possible Safety Effects

Failure of a current lead means a discontinuity of current, with all the safety consequences that entails. There will be high voltages and possible arcing, both at the lead that failed and throughout the magnet system. Large transient forces and eddy current heating are likely. High thermal stresses, overpressurization, and magnet destruction may occur.

Current Practice

The normal, gas-cooled leads are typically procured commercially. These are available with current capacities up to 20 kA or so. The principal safety practice is to monitor the temperature of the leads, the voltage drop across them, and the gas flow through them, and to dump the magnet if these exceed prescribed limits. The leads must have sufficient excess capacity that if the gas flow stops, they can safely carry the magnet current throughout the magnet dump. Safety practice for the superconducting lead is to secure it wherever possible, thus preventing motion, and to monitor the lead temperature via thermocouples or potential taps.

Adequacy of Current Practice

There are three areas in which the adequacy of current practice might be questioned. First, the support of both the gas-cooled and the superconducting leads may be more difficult because of the higher fields and more complex geometry of TF coils. Second, the much greater stored energy of a TF coil system may require a longer dump time; and the leads must survive this longer dump. Third, currents of 50 kA and higher have been proposed for TF coils; but gas-cooled leads of this capacity are not well developed.

Areas Requiring Further Study

An experimental program should be undertaken on high-current (50,000 A or more) leads with sufficient excess capacity to survive a long discharge. Fortunately, such leads are largely independent of the magnet design.

Electrical Arcing

Identification and Definition

Electrical arcing is another frequent cause of superconducting magnet failure.^{2,3} Arcing may occur between the current leads or between a lead and a grounded component, especially where the intermediate space is filled with helium vapor, which has a very low dielectric strength. It may also occur between turns, where the intervening insulation exhibits imperfections or has been damaged by overheating, overstressing, or radiation.

Possible Safety Effects

The safety effects of arcing are severe; it can lead to the destruction of the coil. Even if it does not, it may lead to shorted turns. If arcing shorts out an entire coil, it will lead to a force imbalance.

Current Practice

Arcing is avoided by using insulating material with appropriate low temperature properties, and by ensuring that nothing will break through the insulation. Where there is a potential difference across a helium-filled space, the gap must be wide enough; and the conductor must be mounted firmly to avoid deflections which narrow the gap. After the magnet is assembled, it is checked against arcs to ground with a high voltage insulation breakdown tester.

Adequacy of Current Practice

Insulating materials, including helium, are not as well understood as they should be at liquid helium temperature; radiation effects on insulation are even less well understood.

Areas Requiring Further Study

Insulating material, including helium, must be studied in a simulated fusion reactor environment. A number of insulating materials with potentially favorable properties should be included in the tests.

Electrical Shorts

Identification and Definition

Electrical shorts can be caused by arcing, or by contact between conductors. Both turn-to-turn and turn-to-ground shorts can occur. Turn-to-ground shorts are less likely to happen, and a single turn-to-ground short is not serious if the coil is otherwise ungrounded or grounded only through a fuse. The remainder of this section deals only with turn-to-turn shorts.

Possible Effect on Safety

Contact shorts have little effect on a superconducting magnet during steady state operation. However, during the charging or discharging of the magnet, the shorted turn can have a large current induced on it by transformer action from the changing flux through it, which can drive the turn to the normal state and cause overheating of the conductor.

Current Practice

During winding, a magnet coil can be checked for shorts by both eddy-current and resistance methods, but some shorts do not show up until the magnet is assembled.

Adequacy of Current Practice

The consequences of a short can only be determined by analysis and experiment.

Areas Requiring Further Study

An analytical study must be performed for a particular magnet design to determine what the consequences of a contact short would be. Experiments can check the analytical predictions.

Conductor Joints

Identification and Definition

Conductor joints are always potentially a safety concern in a superconducting magnet. They can be in danger of mechanical failure, as well as serving as a resistive heat input; at least they are mechanically an irregularity in the coil structure.

Intra-coil joints, performed during coil winding, include joints between different grades of conductor. Inter-coil joints, performed during coil assembly, include joints between coils or between pre-assembled portions of coils, e.g. pancakes. Intra-coil joints can be made under almost ideal conditions in the winding line, with whatever space is needed for forming and inspecting the joints; while inter-coil joints must be made and inspected in the process of assembling the coils.

Possible Effects on Safety

A poorly bonded joint or a joint in which one side protrudes out of the coil and can be sheared off when the coil is energized can lead to a current discontinuity. A joint with good mechanical strength but high electrical resistance causes heating and may produce a magnet quench.

Current Practice

Techniques by which safe joints can be made include: soldering, cold welding, resistance welding, ultrasonic welding, explosive welding, and electron-beam welding. Any of these could be incorporated in the winding operation for intra-coil joints, but space limitations may preclude some of them for the inter-coil joints. Inspection methods include ultrasonic and X-ray, plus resistance measurements of completed joints. Acceptable joints must meet the criteria of electrical resistance and mechanical strength.

Adequacy of Current Practice

It is by no means clear which technique of making joints is most likely to produce safe, reliable joints. An extensive development program is required.

Areas Requiring Further Study

Once a conductor concept, or a small number of different conductor concepts, is chosen, development work should begin on determining the best way to make safe intra-coil joints. Subsequently, work should be done to adapt that method to inter-coil joints, or to find a method that can make safe inter-coil joints.

Forces from Unequal Currents

Identification and Definitions

If the TF coils of a tokamak are perfectly aligned and all carrying the same current, they exert only a centering force on each other, withstood by the central support cylinder. The misalignment of the coils expected in assembly introduces out-of-plane forces on each coil, but these forces⁴ are smaller than the out-of-plane forces due to the pulsed poloidal field. However, a large current imbalance among the TF coils would lead to very large out-of-plane forces which tend to pull the coils together like an accordion.

Possible Effects on Safety

It is hard to imagine a worse accident in a tokamak reactor than the TF coils collapsing together. Cryostat rupture, vacuum failure, electrical discontinuity; all would result, with all their subsequent dangers.

Current Practice

As no superconducting TF coils now exist, it is more accurate to speak of current planning rather than current practice. Two approaches may be taken to this problem. One is to provide an outer structural shell connecting the TF coils and sufficiently strong to withstand the forces produced by any combination of currents in the coils. The other is to operate the coils in series; operating in series, the coils must carry equal currents, and there is no force problem.

Adequacy of Current Practice

The series current approach is advantageous, provided the current cannot arc across an entire coil or many turns of a coil, bypassing the coil and providing field imbalance among the coils.

Areas Requiring Further Study

A careful analytical study should be carried out to see if it is possible to design and build a coil which is not subject to shorting out. Such a study might then define some key experimental questions which must be answered before the choice can finally

be made between the two approaches to forces from unequal currents.

Eddy Current Effects

Identification and Definition

Eddy currents are produced by the pulsed poloidal field coils. More severe eddy current problems will be produced by any rapid unscheduled interruption of the toroidal field, poloidal field or field from the plasma. Eddy current effects occur not only in the magnets themselves, but also in the support structure, cryostats, and other systems of the reactor such as the blanket and shield.

Possible Safety Effects

Eddy currents can cause heating, forces and torques both in the magnet system and in other systems of the reactor. Within the cryostat, the heating is a significant problem as it could vaporize helium and overpressurize the cryostat or could quench the magnet. The forces and torques are a problem both inside the cryostat and out.

Current Practice

Where changing fluxes occur, poorly-conducting materials prevent eddy current effects. If a conducting material is required, it can be segmented to limit eddy current paths. For example, the nitrogen shield can be made from multiple segments.

Adequacy of Current Practice

The field from the fusion magnets engulfs the entire reactor. The eddy current effects can occur throughout the reactor. The design of all systems must take eddy current effects into account.

Areas Requiring Further Study

Eddy currents occur in many parts of the reactor, and differ from system to system and from one reactor design to another. To analyze them, one of the general purpose eddy current programs, such as those under development at the Rutherford Laboratory^{5,6} and Argonne^{7,8} should be adapted to fusion magnets.

Cryostat Rupture and Vacuum Failure

Identification and Definition

Obviously a cryostat should not rupture; but a cryostat rupture is conceivable through overpressurization, excessive mechanical stress, penetration by an object or the shearing off of tubing to the cryostat. Cryostat rupture and vacuum failure are interrelated; either can cause the other.

Possible Safety Effects

A rupture which allows helium or air into the vacuum can result in rapid heat transfer from the outer cryostat wall to the inner or helium temperature wall. The pressure in the helium space and vacuum space will rise, and the magnet may quench, which in turn will result in more heating and pressurization. A dangerous failure of the magnet system is possible.

Current Practice

Both the helium vessel and the vacuum space must be equipped with pressure relief valves or rupture disks to prevent overpressurization. These must lead

to vent piping sized so that a low pressure gradient can empty the vessel quickly.

Adequacy of Present Practice

The helium vessel is larger and more complex for the fusion magnets than for any existing magnets. Only a thorough analysis can determine what must be done to prevent serious consequences. It may be necessary to provide separate helium vessels for each coil, although this would greatly complicate the support of the coils and would certainly increase the heat load of the system.

Areas Requiring Further Study

Analysis of heat transfer and pressure buildup and relief should be carried out for the helium vessel and vacuum space for a typical fusion reactor design. This should determine whether a common helium system and common vacuum for all coils is acceptable. All potential causes of cryostat rupture and vacuum failure should be studied as well.

Fringing Field

Identification and Definition

The TF coils will produce a weak fringing field, but one extending far from the magnets. The pulsed field from the PF coils will be even higher.

Possible Effects on Safety

There are three possible effects on safety: disturbance of instrumentation with consequent malfunction, attraction of iron objects to the magnets with possible penetration of the cryostat, and effects on personnel. The first two of these are readily dealt with in a straightforward manner, but the effects on personnel are hard to specify, because there is so little evidence of biological effects from low fields. S. J. St. Loran⁹ in his extensive review on biomagnetic effects concludes that magnetic effects are fully reversible and that there are no data which indicate a cumulative effect.

Present Practice

An advisory committee to DOE has recommended¹⁰ Interim Standards for Occupational Exposure to Constant Magnetic Fields; these appear in Table I.

Table I. Interim Safety Standards for Constant Magnetic Fields

	For an 8 hr. Work-Day	For Exposures of 1 hr. or less	For Exposures of 10 min. or less
Whole Body or Head Exposure	0.01 Tesla	0.1 Tesla	0.5 Tesla
Exposure of Extremities	0.1 Tesla	1 Tesla	2 Tesla

To prevent accidents from iron objects, the checklist for magnet charge should include a thorough check for iron objects each time the magnets are charged.

Adequacy of Present Practice

In the absence of new data, the present practice provisionally can be considered adequate. Any fusion reactor design should include a map of the fringe field from the TF and PF coils.

Areas Requiring Further Study

If there is a question about the adequacy of the Interim Standards, or if observing them would place a hardship on fusion reactor operation, then an experimental program should be commissioned to find the needed data that do not yet exist.

Instrumentation for Safety

Identification and Definition

Proper instrumentation is required to monitor and respond to the safety concerns discussed in the previous sections. Temperature sensors, potential leads, strain gauges, liquid level indicators, and instrumentation for the leads must all be chosen so that any fault condition can be detected quickly, analyzed, and informed action taken.

Possible Safety Effects

Problems such as abnormal forces between coils, a propagating or stationary normal length, an uncooled length, an overheated or uncooled lead, an electrical short, high pressure in the cryostat, or a poor vacuum cannot be corrected without sufficient reliable instrumentation to inform the operator what is happening. The choice of instrumentation is an important safety consideration.

Current Practice

Existing large cryostable magnets are extensively instrumented for safety. Temperature sensors and potential leads are located in the coil region. Strain gauges are located in the coil and in the structure where high or anomalous stresses may occur. Liquid level indicators tell when the level is in danger of falling below the top of the coil. The leads are checked for temperature, voltage drop, and gas flow. On the basis of this information, appropriate action is taken. In case of a severe problem, the magnet is discharged. In case of some external problems such as power supply problems, the magnet is switched to a "free-wheeling" state in which the magnet terminals are connected together, the power supply is disconnected, and the magnet continues to operate but with a very slowly decreasing current. For less severe problems, a visible and audible warning is given so that the problem will be noticed and corrected.

Adequacy of Current Practice

The current philosophy of instrumentation for safety seems adequate, but the choice of sensors will be unique to the fusion reactor. The magnets are so closely located to the rest of the reactor that the magnet must be made to respond to problems in the other systems and vice versa. In addition to other instrumentation, it may be desired to monitor radiation dosage at the coil.

Areas Requiring Further Study

An analysis of the instrumentation requirements for fusion magnets should be made. The object of such a study would be to find areas in which existing sensors are inadequate. Suitable ones must then be developed and evaluated.

Conclusions

The above list of safety concerns is by no means complete, but it does include those that have been of concern to the Superconducting Magnet Group at Argonne. The highest priority should be attached to those areas which are very important, need to be determined early, and are virtually design independent.

1. The development of a computer program for the quench analysis of cryostable fusion magnets.

2. Development of a general purpose program for eddy current calculations applied to fusion magnets.

3. Analysis of series-connected TF coils, to determine if a credible accident could cause them to carry unequal currents.

4. An experimental program to develop current leads of capacity above 50 kA.

5. An experimental study of the behavior of electrical insulation under radiation at cryogenic temperatures.

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Overcurrent Protection for the TFTR Neutral Beam Sources During Spark Down*

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Summary

The accelerating grid of a neutral beam source (NBS) of the Tokamak Fusion Test Reactor (TFTR) operates at 120 kV and 65 A. The capacitance to ground between the switch tube (ST) and the NBS is $C_1 \sim 5$ nF (~ 36 J). The arc and filament power supplies for the NBS float at 120 kV and have a capacitance to ground of $C_2 \sim 2$ nF (~ 14 J). When the NBS sparks to ground, C_2 begins to discharge immediately. The ST impedance limits the fault current from the high voltage (HV) power supply to ~ 100 A until it disconnects the power source < 2 μ s after spark down. In case the ST fails, a crowbar removes the power source ~ 4 μ s after spark down and C_1 begins to discharge. During spark down, fault currents are limited with a saturated time-delay transformer (STDT) connected between the ST and the NBS and with a snubber, which is in the arc and filament power leads, in connection with a spark gap. Alternatively, STDT's can be used for the HV and for the arc and filament power leads. This paper presents design details and experimental results of the overcurrent protection circuits.

Introduction

The accelerating and the gradient grid of an NBS operate at very high voltages and must be protected from damage by occasional sparks to ground. In the summer of 1975 I proposed an STDT to limit the fault current during spark down.^{1,2} The STDT is connected in series with the HV source. W. R. Baker of the Lawrence Berkeley Laboratory³ reported in 1976 that toroidal 50-50 NiFe tape cores can be used as one-turn "snubbers" to limit fault currents. In 1977 I proposed that spark gaps and resistors in conjunction with an STDT and reactors be used to shunt most of the discharge energy away from the NBS.⁴ On the basis of this proposal two prototype circuits are being developed to protect the NBS's of the TFTR.

In the circuit of Fig. 1 an STDT limits the current to the acceleration (accel.) and gradient grids of the NBS. During normal operation the ST will open the power circuit in < 2 μ s, thereafter capacitor C_1 discharges. In case the ST fails, a crowbar short circuits the power source ~ 4 μ s after the NBS sparked to ground. The energy stored in C_2 is dissipated in the Deltamax cores and in resistor R_2 which is connected in parallel to the Deltamax cores when spark gap G is triggered by the STDT.

In the circuit of Fig. 2 the one-turn Deltamax core has been replaced by multi-turn Sillectron cores. Sillectron cores are less efficient but also very much less expensive than Deltamax cores. With Sillectron cores and multi-turn windings, the time delay and circuit impedance can be increased economically, thereby eliminating the need for the spark gap circuit.

Fault-Current Limits. The 12 NBS's for the TFTR are each rated 120 kV 65 A. From the specifications⁵ for the NBS a few of the many requirements are stated as follows:

Turn-off time after fault < 10 μ s
Interrupt duration after fault 5 to 50 ms,

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Fig. 1. NBS Protected by STDT, Snubber and Spark Gap.

Fig. 2. NBS Protected by STDT's.

Maximum $1/2 CV^2$ energy allowed on NBS side of protection circuit	3 J,
Maximum $1/2 CV^2$ energy allowed on power supply side of protection circuit	15 J
Maximum fault current permitted by protection circuit over and above operating current	200 A

The above limits the discharge to $\int i dt \leq 265$ A $\times$ 10 μ s $= 2.65$ m As and the capacitance of 2.1 nF. The specifications do not mention $\int i^2 dt$ values. For the NBS of the TFTR we have $C_1 = 5$ nF (36 J) and $C_2 = 2$ nF (14.4 J) which exceeds the desired value of 15 J by a factor of 3.36. However, there should be no limit set to the $1/2 CV^2$ allowed on the power supply side of the protection circuit as long as the protection circuit holds the fault current to safe values.

Failure Modes. The failure mode of the arc and filament circuit is always the same. When the NBS, sparks, the capacitance to ground, C_2 , of the arc and filament transformer assembly begins to discharge. The grid circuits can have three failure modes. First, during normal operation of the protective circuits, a spark down causes the ST to disconnect the HV supply within 2 μ s, thereafter capacitor C_1 discharges. Second, the ST does not open the circuit but keeps limiting the current. In this case the crowbar is activated within 4 μ s. This removes the HV source and capacitor C_1 begins to discharge. The worst fault condition exists if the ST sparks over when the NBS sparks to ground. The ST no longer limits current and causes capacitor C_1 to be charged to 150 kV for 4 μ s until the crowbar shorts the HV source.

$\int i dt$ versus $i^2 dt$. In the past only the peak values of charge (As), energy (J) and current (A) have been specified during fault conditions. For a given spark down resistance of a NBS, the energy dissipated is proportional to $\int i^2 dt$. Therefore, discharging a given amount of coulombs through as large an impedance, connected in series with the NBS, as practicable will cause the least amount of energy to be dissipated in the NBS. For example discharging 2 nF, charged to 120 kV into a 200 Ω resistor results, with $RC = \tau = 0.4 \mu$ s, in a discharge current of

$$i = I_p \exp(-t/\tau) = 600 \text{ A} \exp(-t/0.4 \mu\text{s}) \quad (1)$$

The total discharge is

$$\int_0^\infty i dt = I_p \tau = 600 \text{ A} \times 0.4 \mu\text{s} = 240 \times 10^{-6} \text{ As}, \quad (2)$$

and for $i^2 dt$, we have

$$\begin{aligned} \int_0^\infty i^2 dt &= I_p^2 \tau / 2 = 600^2 \text{ A}^2 \times 0.4 \mu\text{s} / 2 \\ &= 72 \text{ mA}^2 \cdot \text{s}. \end{aligned} \quad (3)$$

If we now increase the resistance by a factor 10, $\int i dt$ does not change, but the discharge will have a 4 μ s time constant and a peak current of only 60 A resulting in a 10 times smaller $\int i^2 dt$ of

$$\int_0^\infty i^2 dt = 60^2 \times 4 \mu\text{s} / 2 = 7.2 \text{ mA}^2 \cdot \text{s}.$$

Of course the energy dissipated is 14.4 J in either case. However, the heat dissipation is 10 times smaller. It is recommended that the specifications for fault currents include values for $\int i^2 dt$.

The Scubber Design

The Initial Current Step in Deltamax

Without spark gap G and resistor R_2 the discharge circuit for capacitor C_2 is the scubber circuit first proposed by W. Baker.³ It uses eddy-current losses in Deltamax cores to limit the current. The equivalent circuit is included in Fig. 4. The circuit is based on a paper by Winter et al.⁶ Part of that paper reports on initial current step measurements on toroidal cores of 50-50 NiFe tape with the result that

$$H = 5.65 \xi^{1/2}, \quad (4)$$

where H = initial step in magnetic field at the surface of a wrap of tape, in Oe,
 ξ = gradient of the initial step voltage around the same wrap of tape, in V/cm.

The authors found that most of the good cores tested were within 50% of the value given by Eq. (4). Converting Oe into A/cm we can write

$$H = \frac{5.65}{0.4\pi} \xi^{1/2} = 4.5 \xi^{1/2} = \frac{i_r n}{l_{Fe}} \quad (5)$$

where i_r = initial step in excitation current, in A.
 n = number of turns on the excitation coil.

and

$$l_{Fe} = \text{length of magnetic flux path, in cm.}$$

From Eq. (5) we obtain the initial step current

$$i_r = 4.5 \frac{l_{Fe}}{n} \xi^{1/2}. \quad (6)$$

The average initial voltage gradient on the laminations of a tape wound core is

$$\xi = \frac{E}{2wnN}. \quad (7)$$

where E = step voltage applied to the excitation coil, in V,
 w = width of the core laminations, in cm,

and

N = total number of laminations

Combining Eqs. (6 and 7) results in

$$i_r = 3.18 \frac{l_{Fe}}{wnN} \left(\frac{E}{2wnN} \right)^{1/2}. \quad (8)$$

The resistance value of the core, equivalent to the initial step response, is

$$R_r = \frac{E}{i_r} = \frac{0.314}{i_r} (EwnN)^{1/2}. \quad (9)$$

The resistance, equivalent to eddy-current losses, changes with time.

The Initial Current Step in Silectron

Al Schofield of Los Alamos Scientific Laboratory took experimental data on the step response of 1-mil Silectron. From these measurements a value of

$$H = 14.8 \xi^{1/2} \quad (10)$$

in A/cm was determined by Vincent Cheng of Gulton Industries, Inc. This agrees with measurements taken at Argonne National Laboratory (ANL) with 2 and 4-mil cores. Therefore, for Silectron, the equations for i_r and R_r become

$$i_r = 14.8 \frac{l_{Fe}}{n} \xi^{1/2} = 10.5 \frac{l_{Fe}}{n} \left(\frac{E}{2wnN} \right)^{1/2} \quad (11)$$

and

$$R_r = \frac{0.095}{i_r} (EwnN)^{1/2}. \quad (12)$$

Discharge Equations

Experiments at ANL have shown that the time responses of the initial current can be grouped as follows:

- The current decays approximately in a straight line (Fig. 9a)
- At higher energies the current decays with a 1-cos ωt shape (Fig. 8a)
- The current rises much above its initial value (Fig. 9c).

The last of these is not in agreement to limit the current to close to its initial step and, therefore, not considered any further.

The Current Decays Linearly. At relatively low energies, the current decay can be approximated with a straight line

$$i(t) = i_r (1 - \frac{t}{T_1}), \quad (13)$$

where T_1 = time when current is zero.

The capacitor voltage is given by

$$e_C(t) = E_0 - \frac{1}{C} \int_0^t i dt = E_0 - \frac{i_r}{C} t (1 - \frac{t}{2T_1}), \quad (14)$$

where E_0 = voltage on capacitor C at $t = 0$. From Eq. (14) the discharge time, $t = T_1$, is, for $e_C = 0$,

$$T_1 = \frac{2 C E_0}{i_r}. \quad (15)$$

The voltseconds (Vs) the snubber must support are

$$\phi(t) = \int_0^t e_C dt = E_0 t - \frac{i_r t^2}{2C} (1 - \frac{t}{T_1}), \quad (16)$$

which, at time $t = T_1$ becomes

$$\phi_{T_1} = \int_0^{T_1} e_C dt = E_0 T_1 - \frac{i_r T_1^2}{2C}. \quad (17)$$

The heat generated is proportional to

$$\int_0^{T_1} i^2 dt = i_r^2 \int_0^{T_1} (1 - \frac{t}{T_1})^2 dt = i_r^2 t (1 - \frac{t}{T_1} + \frac{t^2}{3T_1^2}), \quad (18)$$

and at the end of the pulse we have

$$\int_0^{T_1} i^2 dt = i_r^2 \frac{T_1}{3}. \quad (19)$$

The above equations are plotted on a per unit basis in Fig. 3.

Fig. 3. Linear Current Decay.

The Current Decay Follows $(1 + \cos \omega t)$. For a current shape described by

$$i(t) = 0.5 i_r (1 + \cos \omega t), \quad (20)$$

the capacitor voltage becomes

$$e_C(t) = E_0 - \frac{1}{C} \int_0^t i dt = E_0 - \frac{i_r}{2C} (t + \frac{1}{\omega} \sin \omega t), \quad (21)$$

and with $e_C = 0$ for $t = T_1 = \frac{\pi}{\omega}$, we find

$$T_1 = \frac{2CE_0}{i_r}$$

which is the same as Eq. (15).

For the flux we have

$$\phi(t) = \int_0^t e_C dt = E_0 t - \frac{i_r}{2C} \left[\frac{t^2}{2} + \frac{1}{\omega^2} (1 - \cos \omega t) \right] \quad (22)$$

which, at $t = T_1 = \frac{\pi}{\omega}$, is

$$\phi_{T_1} = E_0 T_1 - \frac{i_r}{2C} \left(\frac{T_1^2}{2} + \frac{2T_1^2}{\pi^2} \right) = E_0 T_1 - \frac{0.35 i_r T_1^2}{C} \quad (23)$$

Finally, we find

$$\int_0^{T_1} i^2 dt = \frac{i_r^2}{4} \left[\frac{3t}{2} + \frac{1}{\omega} (2 \sin \omega t + \frac{1}{4} \sin 2 \omega t) \right] \quad (24)$$

which at $t = T_1 = \frac{\pi}{\omega}$ becomes

$$\int_0^{T_1} i^2 dt = \frac{3}{8} i_r^2 T_1 \quad (25)$$

The above equations are plotted in Fig. 4.

Fig. 4. Current Decay Follows $(1 + \cos \omega t)$.

The TFR Deltamax Snubber and Spark Gap Circuit

The Deltamax Circuit. As shown in Fig. 1, the heavy buses of the arc and filament circuits, together with the cables of the monitoring circuits, form a one-turn excitation coil for the Deltamax snubber. The snubber is biased to -0.2 Oe with a one-turn winding. When the NBS sparks to the snubber cores, the 120 kV on capacitor $C_2 = 2$ nF excites the snubber cores. Snubber details are given in Table I.

Table I: For 120-kV Snubber Core Assembly

No. Deltamax cores	36
OD	25.0 cm
ID	14.5 cm
Buildup	b = 5.24 cm
Tape width	w = 1.27 cm
Tape thickness	d = 2.54×10^{-3} cm
Stacking factor	s = 0.75
No. wraps in assembly	N = $36 \text{ bs/d} = 5.57 \times 10^4$
Average voltage gradient on wraps	$\epsilon = 0.848 \text{ V/cm}$
Voltage between wraps	2.15 V
Median length of flux path	$l_{Fe} = 61.8 \text{ cm}$
Cross area of assembly	A = 180 cm^2
Cost of 36 cores @ \$310/core	\$11,160

The tapes are deburred and double-coated. Each core is in a nylon case and cushioned with dielectric gel KF-15-523 of the Dow Corning Corp. From Eq. (8) the initial current step is $i_p = 256$ A. From Eq. (15), the discharge time is, $T_1 = 1.88$ μ s. From Eq. (23) the core assembly must support a total flux of $\Phi_{T_1} = 0.067$ Vs. The average voltage during the discharge is $V = \Phi_{T_1}/T_1 = 0.367$ Vs/ 1.88 μ s = 35.8 kV. With a flux density change of $\Delta B = 28 \times 10^3$ G the cores will support 35.8 kV for

$$\Delta t = \frac{nA(\Delta B)}{V \times 10^8} \quad (26)$$

$$= \frac{180 \times 28 \times 10^3}{35.8 \times 10^3 \times 10^8} = 1.40 \text{ } \mu\text{s}.$$

This falls 25% short of $T_1 = 1.88$ μ s. However, as is shown later, the spark gap will shunt most of the discharge away from the NBS and into R_2 in less than 100 ns after the NBS has sparked down.

We see from Fig. 4 that after 100 ns ($0.05 T_1$) not much energy has passed through the NBS. Because of the quick response of the spark-gap circuit, we can reduce the amount of core material. For example, by cutting the tape width in half, we would keep the same volts/wrap. The gradient ξ would double, causing i_p to increase by 41.4% to 362 A. With half the area, the volt-second rating of the core assembly would be cut in half to 25 ms, which still provides a large safety margin.

The Bias Circuit. During normal operation a dc bias current of 12 A generates 0.2 Oe on the outer wrap of the snubber cores. This current also causes a dc flux of 5 kG in the cores of bias choke L_{B2} . A short time after the NBS sparks to ground, this bias circuit is, by transformer action, connected in parallel to the snubber impedance. Choke L_{B2} is dimensioned to keep this additional fault current small and to absorb the voltage across C_2 . The voltage divider made by L_{B2} , R_{B2} , and the parallel connection of a diode and a metal oxide varistor protects the bias supply from transient voltages.

Table II: Details for 120-kV Bias Choke

Eight Sillectron cores AL-1463 with	$d = 5.08 \times 10^{-3}$ cm
$N = 8$ bs/d	$N = 4.45 \times 10^3$
Width of Lamination	$w = 3.81$ cm
No. of turns	$n = 13$
Volts/turn = 120-kV/13	$= 9230$ V
Volts/lamination = 9230/N	$= 2.07$ V
Voltage gradient on lamination	$\xi = 0.271$ V/cm
Length of flux path in iron	$l_{Fe} = 55.6$ cm
Length of flux path in air gap	$l_g = 0.02$ cm
DC induction	$B_{dc} = 5000$ G

Under worst conditions (perfect coupling between the primary and secondary turns) we would have a current step in the bias circuit when the NBS sparks down. From Eq. (11) the current step is $i_p = 33.2$ A. This current adds to the 12 A bias current. However, the pulse permeability of Sillectron⁷ is very much smaller than the dc permeability. This causes the flux density to increase slow enough to stay well below saturation during the discharge pulse. The cores were selected on the basis that the volts/lamination be ≈ 2 V. This voltage requires double coating of the laminations (a carlote coat followed by a magnesium methacrylate coat). The turns were wound with RG-17 cable with the braid removed, and the coil assembly was vacuum impregnated with epoxy resin.

Test Results

The Deltamax snubber was tested in the circuit of Fig. 5. The arrangement of the circuit components is shown in Fig. 6.

Fig. 5. Test Circuit.

Fig. 6 Test Circuit Components.

Tests Without Spark Gap G2. With the STDT disconnected, the 2 nF capacitor C_2 was discharged through the snubber when spark gap G_1 was triggered via thyatron TH_1 . The trigger pulse to thyatron TH_1 was also used to trigger the time sweep of the oscilloscope used to monitor the output of the current transformers (CT's). The spark gap, especially at low voltages, does not always break over at the same time after TH_1 is triggered. This time jitter causes the time delay seen in some of the pictures at the start of the discharge pulses. By means of switches S_1 , S_2 , and S_3 , the effect of bias circuit parameters was investigated.

A discharge from 70 kV (4.9 J) with all switches open, after the snubber cores had been reset, is shown in Figs. 7a. The "NBS" fault current is measured by CT_1 . The capacitor current is measured by CT_2 . Closing switch S_3 after the core had been reset resulted in current pulses as shown in Fig. 7b; the LC of the bias circuit rings at 3.3 MHz. Future operating conditions are simulated by closing switch S_2 after core reset and connecting the bias winding to the primary winding. These conditions are shown in Fig. 7c. With 84 kV on C_2 we expect a peak current of 214 A. As shown in

Fig. 8a the current measured with CT_2 is in reasonable agreement: this picture was taken with all switches open after the core had been reset. Figure 8b was taken with S_1 and S_2 closed. The current through the grounded bias circuit, oscillating at 5.6 Mhz, is superimposed to the current shown in Fig. 8a. The current through the bias circuit is shown in Fig. 8c. The changes in current shape and magnitude for various levels of energy are illustrated in Fig. 9, which shows the discharge of a 7 nF capacitor charged to 30, 60, and 35 kV.

(a) 100 A/div (b) 0.5 μs/div (c)

Fig. 7. Discharges of 2 nF at 70 kV.

Fig. 8. Discharges of 2 nF at 84 kV.

Fig. 9. Discharges of 7 nF.

Tests With Snubber and Spark Gap. For reference, Fig. 10a shows the snubber current with G_2 disconnected. With the circuit of Fig. 5, resistor R_2 is parallel with the snubber when G_2 is triggered by the STDT. As illustrated by Fig. 10b, this happened approximately 170 ns after G_2 began to discharge. The capacitor current is shown in Fig. 10c; it shows the current step when G_2 turns on. The pictures in Fig. 11 illustrate how the time to trigger G_2 from the STDT decreases as the operating voltage increases. This is also shown in Table III.

Table III: Voltage and Trigger Delay of G_2 with 40 psl of N.

E_0	60	70	75	84	kV
Δt	360	170	120	100	$\times 10^{-9}$ s

Fig. 10. Discharges of 2 nF at 70 kV.

The STDT Design

In contrast to a snubber, an STDT limits fault currents with its magnetizing inductance. Eddy-current losses, utilized by a snubber, are undesirable in an STDT. An STDT can be designed to provide time delays of tens of microseconds or longer, which is impractical for a snubber. An equivalent circuit for the STDT portion of the circuit of Fig. 1 is shown in Fig. 12. In this circuit, it is assumed that the ST has arced over and that 4 μs will elapse before the crowbar removes the power source. For simplicity, only the primary winding to the accel. grid (n_1) and the secondary winding of the bias current (n_2) of the circuit in Fig. 1 are being shown. All primed components represent circuit values of the secondary of the STDT transformed to the primary.

Fig. 12. Equivalent circuits for an STDT.

During normal operation, the bias current i_2' is larger than the load current i_1 ; this saturates the core ($\mu_d \sim 200$). The power-supply voltage appears across the load. When the NBS sparks to ground, the STDT comes out of saturation and magnetizing impedance L_M and the eddy current resistance R_e appear in the circuit; R_e appears in the bias choke circuit. Voltage E_0 is now absorbed by the STDT. The copper losses and the leakage inductances of the STDT are small when compared with R_e and L_M and can, therefore, be neglected. This results in the simplified circuit of

Fig. 12b. The NBS is exposed to the sum of the currents shown in Fig. 12a, however, we will neglect the capacitive currents i_{CB}' and i_{CS}' . The total fault current can now be written

$$\begin{aligned} \sum i(t) &= \sum i_{STDT}'(t) + \sum i_{S'}'(t) = i_F(t) + i_M(t) \\ &+ i_2' + i_F'(t) + i_M'(t), \end{aligned} \quad (27)$$

Because of the spark gap circuit, there is one more step-current for the STDT shown in Fig. 1. This current i_{FG} must be added to the above currents. Under worst-fault conditions, 150 kV will be applied to the circuit. We will now calculate the fault current for this case.

Fault Current $\sum i(t) = \sum i_{STDT}'(t) + \sum i_{FG}'(t)$. Table IV gives details of the STDT built for the circuit shown in Fig. 1.

Table IV: Details for 150-kV STDT

Four Silatron cores AL-1268 with $d = 5.08 \times 10^{-3}$ cm	
No. of lamination $N = 4$ bs/d	$N = 3560$
Width of laminations	$w = 8.89$ cm
No. of primary turns	$n_1 = 28$
No. of secondary turns	$n_2 = n_3 = 28, n_4 = 14$
Volts/turn	5357 V
Volts/lamination	1.5 V
Voltage gradient on laminations	$\xi = 0.085$ V/cm
Length of flux path in iron	$l_{Fe} = 98.6$ cm
Length of flux path in air gap	$l_g = 5 \times 10^{-3}$ cm
Core area	$A = 160$ cm ²
NBS operating current	$i_1 = 65$ A
Peak bias current for 5 Oe	$i_B = 163$ A
RMS bias current for 5 Oe,	
$i_B / \sqrt{0.133}$	$= 59.4$ A

From Eq. (11) and data of Table IV, the initial current step is $i_F = 15.2$ A when the NBS sparks down. A short time later, current step i_{FG} occurs when spark gap G triggers. With $n_1 = n_3$, this current follows the expression

$$i_{FG}'(t) = \frac{E_0}{R_3} \exp(-2t/R_3 C_3) = 60 \text{ A} \exp(-t/0.2 \mu\text{s}).$$

The magnetizing current i_M is calculated from the pulse magnetization curves of the core material.⁷ In our case we have, after $\Delta t = 4 \mu\text{s}$, a flux change of

$$\Delta B = \frac{\Delta t \times V \cdot 10^8}{n_1 A} = \frac{4 \mu\text{s} \times 150 \text{ kV} \times 10^8}{28 \times 160 \text{ cm}^2} = 13.4 \text{ kG}$$

which is less than half of the 30 kG available from the cores. Extrapolating from manufacturer's curves, we find for $\Delta B = 13.4$ kG a $\Delta H = 7.8$ A/cm. The magnetization current required by the STDT can be calculated from

$$i_M = \left(\Delta H_{Fe} \times l_{Fe} + \frac{\Delta B}{0.4 \pi} l_g \right) / n_1 \quad (28)$$

as

$$i_M = (7.8 \times 98.6 + \frac{13.4 \times 10^3}{0.4 \pi} \times 0.005) / 28 = 29.4 \text{ A}.$$

Fault Current $\sum i_{STDT}'(t)$. For the STDT to come out of saturation, the fault current must exceed the bias current i_2 . Transformed to the primary, the bias current is

$$i_2' = i_2 n_4 / n_1 = 163 \text{ A} \cdot 14 / 28 = 81.5 \text{ A}.$$

Currents $i_F'(t)$ and $i_M'(t)$ are determined by the step voltage across the bias choke, which is $V_B = E_0 n_4 / n_1 = 150 \text{ kV} \cdot 14 / 28 = 75 \text{ kV}$, and by the choke parameters given in Table V.

Table V: Details for the 75-kV Bias Choke L_{B1}

Four Silatron cores with the dimensions of the AL-1268 cores but wound with tape of thickness	$d = 10.16 \times 10^{-3}$ cm
No. of laminations	$N = 1800$
Core Area	$A = 162$ cm ²
No. of turns	$n = 78$
Volts/turn	961 V
Volts/lamination	0.53 V
Voltage gradient on laminations	$\xi = 0.030$ V/cm
Length of air gap	$l_g = 1.7$ cm
DC magnetization	$B_{dc} = 8.8$ kG

The initial current step from Eq. (11) is $i_F = 3.25$ A, which, transformed to the primary amounts to $i_F' = 0.5 i_F = 1.63$ A. During the fault duration of $\Delta t = 4 \mu\text{s}$, the change in induction in the choke is

$$\Delta B = \frac{4 \mu\text{s} \times 75 \text{ kV} \times 10^8}{78 \times 162 \text{ cm}^2} = 2.37 \text{ kG}$$

This corresponds to $\Delta H = 1.43$ A/cm. The magnetization current for $l_{Fe} = 98.6$ cm and $l_g = 1.7$ cm is, from Eq. (28), $i_M = 42.9$ A. This value transformed to the primary becomes $i_M' = 0.5 i_M = 21.4$ A.

Maximum Values of Fault Current. The step currents will decay to insignificant values within 1 or 2 μs while the magnetizing currents, starting from zero, will be at their peak after 4 μs . The current shape, therefore, has two peaks. One occurs at the time the NBS sparks to ground. Its value of

$$i_{t=0+} = i_2' + i_F' + i_F' + i_{FG}' \quad (29)$$

is in our case

$$i_{t=0+} = 81.5 + 15.2 + 1.6 + 60 = 158 \text{ A}.$$

The second peak occurs at the time when the HV source is crowbarred. Its value of

$$i_{T=4 \mu s} = i_2 + i_M + i_{M1} \quad (20)$$

is for our application

$$i_{T=4 \mu s} = 31.5 + 29.4 + 21.4 = 132.3 \text{ A.}$$

With the HV source removed, the magnetizing currents i_M and i_{M1} will go to zero. Their decay is determined by the L/R time constant of the network.

The above worst conditions of 150 kV, when the ST sparks over, should not occur often. Normally, when the NBS sparks down, only 120 kV is applied for 2 μ s. After that time, the ST disconnects the HV source from C_1 . The oscillatory discharge current of C_1 is limited by the magnetizing inductance of the STDT as described above. When C_1 has discharged, the circuit energy is stored in the STDT and in the bias choke L_{B1} . Diode D_1 prevents C_1 from taking a negative charge and cuts off the fault current through the NBS. The inductively stored energies are dissipated in R_1 and R_{B1} .

Test Results. In the circuit of Fig. 5, capacitor C_1 was discharged by triggering spark gap G_1 . During those tests we had $R_1 = 20 \Omega$, $R_2 = 1 \text{ k}\Omega$, and $C_3 = 350 \text{ pF}$. As shown in Fig. 13a, a current of about 11.5 A flows for 26 μ s; this amounts to 300 μ As which agrees with the charge on C_1 of 60 kV x 5 nF = 300 μ As. The same agreement is found in Fig. 14, where C_1 is discharged from 84 kV; 84 kV x 5 nF = 15.6 A x 27 μ s = 420 μ As. The currents of 11.5 and 15.6 A are proportional to the voltages of 60 and 84 kV. [$i = E (C/L)^{1/2}$]. Figures 13c and d show the circulating current through diode D_1 , which discharges the STDT in 800 μ s. At 60 kV, the initial current spike of $i = i_p + i_{rg}$ was expected to be $i \approx 15.2 \text{ A} (60/150)^{1/2} + 60 \text{ kV}/1 \text{ k}\Omega = 69.6 \text{ A}$. Figure 13b shows an oscillatory peak of 190 A. These large initial oscillations last for about 500 ns and are attributed to the capacitance between windings n_1 and n_2 . With n_2 grounded, the interwinding capacitance is charged to 60 kV and discharges with little series inductance when G_1 is triggered. In the TFTR circuit (Fig. 1), both n_1 and n_2 are at the same potential when the NBS sparks down; this should eliminate the initial large oscillations.

Fig. 13.

Discharge of 5 nF at 60 kV.

Fig. 14.

Discharge of 5 nF at 84 kV.

STDT's For the Arc and Filament Circuits. The filaments of the NBS operate at 6 kA. With a duty factor of 0.133, the rms current is 2.2 kA. For the arc supply, the peak current is 3 kA, the duty factor 0.033, and the rms current 545 A. It appears to be

more economical and more reliable to replace the Deltamax snubber and the spark-gap circuit of Fig. 1 with two STDT's, as shown in Fig. 2. We are presently investigating this possibility. Data for a preliminary design for STDT2 are given in Table VI.

Table VI: Details for the 120-kV STDT2

10 Sillectron cores AL-1145 with	$d = 5.08 \times 10^{-3} \text{ cm}$
No. of laminations	$N = 15,000$
Width of laminations	$w = 6.99 \text{ cm}$
No. of bifilar turns	$n = 4$
Volts per turn	30 kV
Volts per lamination	2.0 V
Length of flux path in iron	$l_{Fe} = 121.4 \text{ cm}$
Length of flux path in air	$l_e = 5 \times 10^{-3} \text{ cm}$
Core area $10 \times 9.28 \times 6.45 \times 0.89$	$A = 573 \text{ cm}^2$
$\phi = nA \Delta B \times 10^{-8}$	$= 0.69 \text{ Vs}$
Initial current step	$i_p = 170 \text{ A}$
Coil resistance	$\approx 102 \mu\Omega$
Heat dissipation	$\approx 500 \text{ W}$
Cost of core material	\$5760

For the arc leads and the coaxial monitoring cables, another smaller core assembly is visualized for STDT 3.

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Resistivity, Hysteresis, and Magnetization of 9% Cr Stainless Steel as a Function of Temperature and Its Electromagnetic Shielding Effects in Cylindrical Structures^a

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Summary

Ferromagnetic stainless steels may offer significantly greater wall life-times for first wall/blanket and vacuum vessel structures than commonly used non-magnetic stainless steels. One steel under consideration has the following composition, in weight percent,

Fe	Cr	Mn	Mn	Si	Nb	V	C	P	S
86.24	9.0	2.0	1.0	0.75	0.50	0.30	0.15	0.3	0.30

There appears to be no literature on the electromagnetic properties of this material. Therefore, the resistivity, the hysteresis loops, and magnetization were measured as a function of temperature up to the Curie point.

For Tokamak applications it is of specific interest to know the resistivity and saturation flux density in the range from 400 to 600°C. By applying a toroidal field that saturates the ferromagnetic steel, the first wall/blanket and vacuum vessel structures should respond to a superimposed alternating field like non-magnetic stainless steel. The field of the equilibrium coils, which gives the fine control of the position and shape of the plasma, must penetrate the first wall/blanket and/or vacuum vessel. Normalized graphs are given for the logarithmic ratio of external to internal alternating fields (shielding) as a function of the ratio of wall thickness to skin depth, with vessel radius, wall thickness, and permeability as parameters. These graphs should be useful for the design of feedback loops for equilibrium-field-coil power supplies and in the designing of electromagnetic shields with this material, either do saturated or not.

Resistivity

A 300 cm long wire sample was drawn to a diameter of 0.159 cm and annealed at 1100°C for half an hour. The resistance of the sample was measured with a Kelvin bridge at temperatures ranging from -195.8°C (liquid nitrogen) to 800°C. Figure 1 shows the resistivity calculated from these measurements. When the resistivity was calculated, no corrections were made to account for the effects of the coefficient of thermal expansion of the material. For comparison, the commonly known resistivity of Types 304 and 316 stainless steel is also shown.

Permeameter Measurements

A sample of material was machined to the dimensions 1.232 x 1.232 x 2.540 cm, and the B-H curves shown in Figs. 2 and 3 were measured with a permeameter at the Arnold Engineering Co. in Marenco, IL. This permeameter uses a large electromagnet to generate magnetic fields of up to 16×10^3 Oe in the tips of large pole pieces shaped like the frustum of a circular cone. The distance between the pole pieces is adjustable to accommodate samples of different lengths. A very thin wire is taped around the middle of the sample to serve as a one turn δ coil. The magnetic field H is measured with a Hall plate. The δ -coil signal is amplified and integrated and used for the vertical deflection of a

Figure 1 Electrical Resistivity in Stainless Steels

Figure 2 DC Hysteresis Loops of 9% Cr Stainless Steel Sample for $H \leq 130$ Oe

Figure 3 DC Saturation of 9% Cr Stainless Steel

^aWork supported by the U.S. Department of Energy

recorder. The amplified H signal is used for the horizontal deflection of the recorder. From Fig. 2 the steel appears to be isotropic having a maximum permeability of $\mu_m = 400$ at 10 kO. The two solid curves were measured when the sample was energized in the direction of its 2.540 cm axis. The two dashed curves were measured when the sample was energized in the direction of a 1.232 cm axis. In the permeameter the ratio of the length of the air gap l_{ag} to the length of the iron sample l_s is larger when the sample is magnetized along a 1.232 cm axis as compared to magnetizing along the 2.54 cm axis; this larger ratio of l_{ag}/l_s causes the slope of the dashed curve to be smaller as compared to the slope of the solid curve. From Figs. 2 and 3 we see that the steel saturates at room temperature at ≤ 20 kO.

Measurements on a Toroidal Sample

Sample Preparation

A toroidal sample was machined to an outside diameter of 10.16 cm and a cross section of 1.11×1.11 cm and its dimensions measured with an accuracy of ± 0.0025 cm. A 0.01 cm thick coating of aluminum oxide was plasma-sprayed on the toroid for insulation. Next a δ coil was wound on the core by uniformly spacing 103 turns of No. 26 AWG Nichrome wire. The δ coil was covered with high-temperature ceramic cement (SAUERHEIMEN No. 6). An excitation coil of 354 turns of bare No. 18 AWG copper wire was wound over the δ coil in three layers. Ceramic cement was applied after each layer and the assembly was baked in an oven. Turn-to-turn insulation was provided by placing a glass-quartz string of 0.1 cm diameter between turns.

Measuring Circuits

Temperature. A nickel-chromium versus nickel-aluminum (Chromel-Alumel) thermocouple was imbedded in a small hole drilled 2.5 cm deep into a piece of steel $1.11 \times 1.11 \times 5$ cm. The steel was then covered with the same thickness of cement as the toroid and placed in the oven next to the toroid. Two more thermocouples were placed in the center of the toroid. Inside the oven, the wires of the three thermocouples were insulated with ceramic tubing. Outside the oven, each thermocouple was connected to a similar thermocouple in series opposition. In this way the thermocouple effects of the interconnections at room temperature between the millivolt meter and the thermocouples cancel. The number of millivolts produced is the difference in millivolts between the temperature of the thermocouple in the oven and the temperature of the thermocouple on the outside. The three outside thermocouples were placed in an agitated ice bath; in this way the oven temperatures were measured with reference to 0°C. The thermocouple voltages were monitored with a Fluke digital voltmeter Model No. 8600 A. The thermocouple temperature was read with an accuracy of ± 0.25 .

Magnetizing Force H and Induction B. The toroid was excited with a 0.5 Hz sinusoidal current ($\sin \omega t = \sin \pi t$). The current was monitored by measuring the voltage drop on a noninductive resistor of 0.340 Ω . By means of a stable amplifier, the voltage on the resistor was amplified to produce on the screen of an oscilloscope a horizontal peak-to-peak deflection of ten divisions. The oscilloscope was a Tektronix Model 706 with a Model 7A13 time base. The voltage induced in the δ coil was fed to an electronic integrator to obtain $\int H dt = B$. The B signal was used for the vertical deflection of the scope. The integrator has a time constant of 50 s. The flux-meter installation was calibrated with a square-wave signal to an accuracy of $\pm 2\%$.

Measurements

Hysteresis Loops. A magnetization curve (solid) and a hysteresis loop (dotted) together with some important magnetic quantities are shown for definition in Fig. 4. Figure 5 shows a few of the hysteresis loops measured. In these pictures, the excitation is held constant at $H = H_0 \sin \omega t = 55 \sin \pi t$ Oe and the temperature T is changed. The oven has a regulator that holds temperature within a few degrees of a set point. It took approximately two hours at a set point to reach a stable temperature in the toroid. The sensitivity of the B scale was increased to take measurements near the Curie point; this is illustrated by Fig. 6. The results of the measurements are summarized in Figs. 7 and 8. Referring to Fig. 7, the Curie point of the sample is at $705^\circ\text{C} \pm 4^\circ\text{C}$. Its maximum permeability $\mu_m = 673$ is reached around 400°C . At 20°C we have $\mu_m = 515$; this is larger than the $\mu_m = 400$ shown in Fig. 2. The difference is due to the unavoidable air gaps when measuring with the permeameter. The air gaps cause a smaller slope (shearing) of the B-H curve.

The coercive force, H_c , declines as the temperature increases. At room temperature the value measured with the permeameter is $H_c = 11$ Oe. The value measured with 0.5 Hz applied to the toroid is $H_c = 12.4$ Oe. The difference is due to an increase in eddy-current losses at 0.5 Hz as compared to the slower changes of dH/dt with the permeameter measurements. At 20°C the 0.5 Hz skin depth

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

of the toroid material is

$$\delta = \left(\frac{77.3 \mu\Omega \text{ cm}}{0.4\pi \times 10^{-8} \text{ H s/cm} \times 515 \pi \times 0.5 \text{ s}^{-1}} \right)^{1/2} = 2.37 \text{ cm},$$

which is 4.3 times the half-thickness of the toroid. Figure 9 shows the effect of frequency on the hysteresis loop of the toroid. At 20°C ($\delta = 2.37$ cm) the loop widens noticeably between 0.05 Hz (~dc case) and 0.5 Hz. From Fig. 9 the dc permeability can be calculated as $\mu_m = 870$, which is 6% larger than the 0.5 Hz permeability of $\mu_m = 515$ shown in Fig. 7. Above 600°C with $\delta \geq 3.48$ cm, the effect of frequency is smaller and disappears as we get closer to the Curie point. Also shown in Fig. 7 is the permeability at 55 Oe, $\mu_{55\text{Oe}}$, as a function of temperature. Figure 8 shows the induction at 55 Oe, $B_{55\text{Oe}}$, and the residual induction, B_r , versus temperature. The induction at maximum permeability, μ_m , has nearly the same values as B_r .

Figure 4 Magnetization Curve (solid) and Hysteresis Loop (dotted)

Vertical: 4.2 kO/div Horizontal: 11 Oe/div

Figure 5 Hysteresis Loops at Various Temperatures for $H = 55 \sin \omega t$ Oe

Figure 6 Hysteresis Loop at 696°C

Figure 7 Variation of Permeability and Coercive Force With Temperature for $H = 55 \sin \omega t$ Oe

Figure 8 Variation of B_{ssOe} and B_r With Temperature for $H = 55 \sin \omega t$ Oe

Figure 9 Hysteresis Loops of Toroidal Sample for Various Frequencies at 20°C

Magnetization. Magnetization curves, measured at room temperature and at 600°C are shown in Fig. 10. To plot these curves, pictures were taken with magnetizing forces of $H \geq 1$ Oe after the toroid had been demagnetized by heating it above the Curie point. Figures 11 and 12 show composites of different hysteresis loops taken at 20 and 600°C, respectively. The tips of these loops lie on the respective magnetization curves.

Figure 10 Magnetization Curves at 20°C and 600°C

Figure 11 Hysteresis Loops at 20°C for Various Magnitudes of Field Strength

Figure 12 Hysteresis Loops at 600°C for Various Magnitudes of Field Strength

Electromagnetic Shielding

In a Tokamak the field of the equilibrium coils, which gives the fine control of the position and shape of the plasma, must penetrate the first wall and/or the vacuum vessel.

With the resistivity, ρ , and the permeability, μ , known, we can calculate the electromagnetic shielding effects of cylindrical structures built from 9% Cr steel. Two conditions are considered. First, the toroidal field is assumed to saturate the cylinder along its axis. At room temperature, toroidal fields of ≥ 20 kG and at 500°C fields of ≥ 16 kG completely saturate the steel (see Figs. 3 and 8). The equilibrium field, which is transverse to the axis of the cylinder, causes only rotations of the saturated domains in the steel.² The incremental permeability is $\mu_A = B_A/B_H = 1$ G/Oe. This is the same as a nonmagnetic cylinder. Second, for completeness sake we also consider applications in which the 9% Cr steel is used unsaturated as an electromagnetic cylindrical shield.

As shown in Fig. 13, a homogeneous alternating field H_0 (the field of the equilibrium coils) is applied transverse to the axis of the cylinder; the resulting electric field E is in the direction of the cylinder axis Z . The field inside the cylinder H_i can be written

$$\begin{aligned} H_r &= H_i \sin \gamma = Q H_0 \sin \gamma \\ H_z &= H_i \cos \gamma = Q H_0 \cos \gamma \end{aligned} \quad 0 \leq r \leq r_0 \quad (2)$$

The factor of electromagnetic shielding Q can be written¹

$$Q = \frac{H_i}{H_0} = \frac{1}{\cosh kd + \frac{1}{2}(K + \frac{1}{K}) \sinh kd} \quad (3)$$

where

$$k = \text{eddy current constant} = \frac{1 + j}{\delta},$$

$$j = (-1)^{1/2},$$

$$\delta = \text{skin depth} = \left(\frac{\rho}{\omega \mu} \right)^{1/2},$$

$$K = \frac{\mu_0}{\mu} k r_0,$$

$$\mu_0 = 0.4\pi \times 10^{-8} \frac{\text{H}}{\text{cm}} \text{ permeability of space,}$$

and

$$\mu = \text{permeability.}$$

Figure 13 Cylinder in an Alternating Field H_0 Transverse to its Axis Z

Nonmagnetic Cylinder. With the cylinder saturated ($\mu_d = \mu_0$), $|K| = |k r_0| \gg 1$, the term $1/K$ in Eq. (3) can be neglected, and we have for the shielding factor

$$Q = \frac{1}{\cosh kd + \frac{1}{2} K \sinh kd} \quad (3')$$

The attenuation a_s of the cylinder is defined by

$$a_s = \ln \frac{1}{|Q|} = \ln \left| \frac{H_0}{H_1} \right| \quad (4)$$

From Eqs. (3') and (4), we obtain

$$a_s = \ln \frac{1}{|Q|} = \frac{1}{2} \ln \left[\left(\frac{r_0}{2\delta} \right)^2 \left(\cosh \frac{2d}{\delta} - \cos \frac{2d}{\delta} \right) + \frac{r_0}{2\delta} \left(\sinh \frac{2d}{\delta} - \sin \frac{2d}{\delta} \right) + \frac{1}{2} \left(\cosh \frac{2d}{\delta} + \cos \frac{2d}{\delta} \right) \right] \quad (5)$$

Figure 14 shows the attenuation, calculated from Eq. (5), as a function of d/δ , which is proportional to the square root of the frequency, for different parameters $p = r_0/2\delta$. For low frequencies ($d < \delta$) and for high frequencies ($d > \delta$), Eq. (5) can be simplified to

$$a_s = \ln \frac{1}{|Q|} = \frac{1}{2} \ln \left[1 + \left(\frac{r_0 d}{\delta^2} \right)^2 \right] \quad \text{for } d < \delta, \quad (6)$$

$$a_s = \ln \frac{1}{|Q|} = \frac{d}{\delta} + \ln \frac{r_0}{2\delta} \quad \text{for } d > \delta. \quad (7)$$

Figure 14 Attenuation a_s of a Nonmagnetic Cylinder

Examples of Shielding Effects. The vacuum vessel of Argonne's ATR Tokamak is made from type 304 stainless steel ($\rho = 74 \mu\Omega/\text{cm}$ at 20°C). It has a radius of 15.2 cm and a wall 0.30 cm thick. For frequencies of 1 and 10 kHz, we have $\delta = 1.38$ and $\delta = 0.43$ respectively, resulting in ratios of $d/\delta = 0.22$ and 0.70 . From Eqs. (5-7), or from Fig. 14, we find an attenuation of $a_s = \ln |H_0/H_1| = 0.99$ at 1 kHz and $a_s = 3.23$ at 10 kHz. This corresponds to field ratios $|H_0/H_1|$ of 2.59 and 25.3 respectively.

Argonne's EPR design has a 7 cm thick vacuum vessel made from type 316 stainless steel. With an average radius of $r_0 \approx 220$, we have $p = r_0/2\delta = 15.7$. Operating at 400°C we find from Fig. 1 a resistivity $\rho = 98 \mu\Omega/\text{cm}$. For frequencies of 10 and 100 Hz, we have $d/\delta = 0.444$ and 1.41 , respectively. From Eq. (5) or the curves of Fig. 14 we find for 10 Hz an attenuation $a_s = \ln |H_0/H_1| = 1.84$ and for 100 Hz, $a_s = 4.16$. This corresponds to ratios of $|H_0/H_1| = 6.3$ and 64 , respectively. A total of 20 cm of stainless steel first wall/blanket material is inside the vacuum vessel to minimize radiation damage to the vessel. If this 20 cm thick material were also in the form of a vessel the combined wall thickness would be $d \approx 27$ cm. For frequencies of 10 and 100 Hz we would have $d/\delta = 1.71$ and 5.42 , respectively. From Eq. (7) the attenuation at 10 Hz would be $a_s = 3.31$ and at 100 Hz, $a_s = 8.17$. This corresponds to ratios of $|H_0/H_1| = 27.5$ and 3533 , respectively. In order to reduce eddy-current shielding the first wall/blanket is made from 128 blocks which are insulated from each other. Each block is made from a 20 cm thick type 316 stainless steel flat plate.

Magnetic Cylinders. With $\mu \gg \mu_0$, the value of $1/K$ in Eq. (3) is larger than 1 and can no longer be neglected. From Eqs. (3 and 4), the general equation for the attenuation is given by

$$a_s = \ln \frac{1}{|Q|} = \frac{1}{2} \ln \left[\left(\frac{\mu_0 r_0}{2\mu d} \right)^2 + \left(\frac{\mu d}{4\mu_0 r_0} \right)^2 \right] \times \left(\cosh \frac{2d}{\delta} - \cos \frac{2d}{\delta} \right) + \frac{\mu_0 r_0}{2\mu d} \left(\sinh \frac{2d}{\delta} - \sin \frac{2d}{\delta} \right) + \frac{\mu d}{4\mu_0 r_0} \left(\sinh \frac{2d}{\delta} + \sin \frac{2d}{\delta} \right) + \frac{1}{2} \left(\cosh \frac{2d}{\delta} + \cos \frac{2d}{\delta} \right) \quad (8)$$

Figure 15 shows the attenuation as a function of d/δ for various values of the parameter $p = \mu_0 r_0/\mu d$. The difference of these curves as compared to the curves of Fig. 14 is that, at low frequencies, all curves have constant values. These values represent the magneto-static shielding of the cylinders. At the limit with $\delta \rightarrow 0$ (or $f \rightarrow 0$), Eq. (8) reduces to

$$\lim_{\delta \rightarrow 0} a_s = \ln \left(1 + \frac{1}{2} \frac{\mu d}{\mu_0 r_0} \right) \quad (9)$$

At very high frequencies, where $\mu_0 r_0/\mu d \gg 1$, the field does not penetrate the cylinder appreciably and magneto-static effects disappear. In this case Eq. (8) reduces to Eq. (5).

Figure 15 Attenuation a_s of a Magnetic Cylinder

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DEVELOPMENT OF A 50 kA CRYOSTABLE AC SUPERCONDUCTING CABLE*

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Summary

A design of a 50 kA cable at 8 T for superconducting ohmic heating coils is presented. The cable, which will be cryostable with relatively low ac losses, is composed of 24 basic cables and stainless steel backbone. A non-metallic cryostat for the test of pancake coils of the cable is developed.

Introduction

The conceptual design studies of tokamak reactors undertaken at Argonne National Laboratory (ANL) and elsewhere over the past several years have identified the requirement of superconducting ohmic heating coils. The ohmic heating coils will have stored energies of the order of 1 GJ and a peak operating current of between 50 and 100 kA with dB/dt of approximately 9 T/s. Because of the large stored energies, the coils should be cryogenically stable and still have tolerable low ac losses during the high ramping rate of the magnetic field. Recently, ANL has developed a 11 kA cable

and demonstrated the cryostability of the coil made from the cable.^{1,2,3} The purpose of this paper is to describe a 50 kA cable. The design of the cable is based on the design and test results of the 11 kA cable.

Design of the 50 kA Cable

The structure of the 50 kA cable is shown in Fig. 1. The cable is assembled by twisting 24 basic cables around an insulated stainless steel or C-10 strip with a twisting pitch of 50.8 cm. The cabling device will provide sufficient stranding tension and pressure to assure a tightly packed cable assembly having an effective cable cross section of 8.26 cm by 1.65 cm.

The basic cable, shown in the upper part of Fig. 1, is composed of six 19-wire cables by twisting together around an insulated multi-strands stainless steel cable with a twisting pitch of 2.5 cm. The stainless steel cable is introduced to reinforce the tensile strength of the basic cable.

The 19-wire cable consists of three layers of wires with a copper wire at the center, six superconducting wires in the second layer and 12 copper wires in the outer layer. The lay of wires is as tight as the geometry allows with a twisting pitch of 0.8 cm for the second layer and 1.25 cm for the outer layer. Then the 19-wire cable is soldered with Staybrite and the surface of the cable is weakly insulated by coating a thin organic film. The soldering and the weak insulation are intended to achieve both the cryostability and relatively low ac losses of the cable in pulsed field.

Cable Test

The performance of the 50 kA cable will be tested initially in a form of pancake coils. For two pancake coils, ac losses at a central field rate of 9 T/s are estimated to be approximately 5 kW. In Table 1, parameters are listed for a 65 MJ coil with 22 pancakes.

Fig. 1. 50 kA AC Superconducting Cable and its Basic Cable

Fig. 2. Inner (Left) and Outer (Right) Tanks of the Non-Metallic Cryostat

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Fig. 3. Set-Up of the Non-Metallic Cryostat for the Test of the Pancake Coils

A non-metallic cryostat for the test of the pancake coils is developed. The cryostat consists of two fiberglass reinforced polyester tanks (Fig. 2) and 100 layers of superinsulation between them. Overall dimensions and support structures of the cryostat are shown in Fig. 3. The cryostat support structure is incorporated into the outer tank so that the cryostat could be supported in an upright position. To reduce the mechanical load to the top cover of the cryostat, an external support structure is introduced to sustain the pancake coils. Heat leak of the cryostat, estimated from the cryostat, developed in the past for the 1.5 MJ coil, is approximately 8 W (11 LHe/hr).

Table 1. Pancake Coil Parameters

Stored energy (MJ)	65
Central field (T)	7.9
Critical current (kA)	46
Inductance (mH)	60
Coil ID (cm)	100
Coil OD (cm)	165
Coil axial length (cm)	200
Number of pancakes	22
Total number of turns	316
Coil current density (A/cm^2)	2300
Cu/SC	7.9
Heat flux when normal (W/cm^2)	0.35
Hysteresis loss (kJ/cycle)	3.2
Eddy current loss at 9 T/s (kW)	135
Heat flux due to ac losses at 9 T/s (W/cm^2)	0.11
AC losses/stored energy (%)	0.1

GENERAL PHASE-FREQUENCY SHIFTING IN THE THREE-PHASE INDUCTOR-CONVERTER BRIDGE*

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Abstract

A fundamental method of shifting phase frequency in the inductor-converter bridge (ICB) for the purpose of controlling the power in real time is presented. Transient switching sequences needed to implement phase-frequency shifting can be developed by the use of this method and the other five system constraints. Two of the constraints that have been expressed in equation form so far are presented. Finally, an alternative algorithm for computing the frequency shifting transient sequences in real time is suggested.

Introduction

The ICB is a solid state dc-ac-dc converter system for reversible energy transfer between two high Q inductors, Fig. 1. The converter SCR's are naturally commutated by a set of y-connected capacitors on the ac lines of the circuit. The circuit is designed so that in every converter cycle, a very small fraction of the magnetic energy is stored in the capacitors and then transferred. The inherent efficiency and real time control of power in either direction make these circuits a specially suitable power supply for pulsed superconductive magnets. Two such applications are the superconductive equilibrium field coils of the projected Tokamak fusion power reactors and superconductive magnets needed in the future particle accelerators. In such systems, the load constitutes one of the ICB coils, the other coil being a storage superconductive coil. Stored energy is supplied to and retrieved from the load.

The details of operation of the ICB may be found in references 1 and 2. It has been shown analytically^{3,4} that the transferred power in the ICB circuit is controlled by two parameters of the storage and load converters: their relative operating phase and their operating frequency. Equation (1) shows the instantaneous average power delivered to the load.

$$\langle p \rangle = \sum_n \frac{12 \langle i_s \rangle \langle i_L \rangle}{n^3 \pi^2 \omega C} [1 - (-1)^n] \sin^2 \frac{n\pi}{3} \sin n\phi$$

$$\Delta k \langle i_s \rangle \langle i_L \rangle \quad (1)$$

where n = Fourier harmonic number of the phase currents

C = capacitance per phase

and the other variables are defined in the appendix. Varying the controlling parameters ϕ and ω in real time requires inducing careful transient switchings on the converters as described in this paper.

Transient Switching in the ICB

A bridge transient switching is defined as a temporary increase or decrease of the time intervals between SCR firings. Switching transients may be induced

to control the operating phase, frequency, or both. A successful transient switching should satisfy six constraints imposed by the ICB circuit and the controlling system:

1. meet the operating phase demand,
2. meet the frequency demand,
3. should not cause phase overvoltages,
4. should not cause SCR commutation failure,
5. should not cause phase voltage unbalance,
6. should be as fast as possible.

Phase Shifting Transients

The relative phase of the two converters is altered by delaying or advancing the relative switching events of the two converters. The simplest way to induce a phase shifting transient and meet constraint 5 above is a three-step transient, Fig. 2. The three steps are a characteristic of the three-phase ICB. Constraints 3 and 4 can necessitate the phase shift to be induced by more than the minimum three steps. In these cases, a multistep transient is produced by superposition of several three-step fundamental transients in tandem.

In general, an n -step transient switching sequence can be constructed from the sum of n switching sequence components.⁵ The sequence may be defined as follows:

$$\begin{bmatrix} s^{(n)} \end{bmatrix}^T \triangleq \begin{bmatrix} \Delta t_1^{(n)} & \Delta t_2^{(n)} & \Delta t_3^{(n)} & \Delta t_4^{(n)} & \dots & \Delta t_n^{(n)} \end{bmatrix}$$

$$\begin{bmatrix} s_1^{(n)} \end{bmatrix}^T \triangleq \Delta t_0 \begin{bmatrix} 1 & 1 & 1 & 1 & \dots & 1 \end{bmatrix}$$

$$\begin{bmatrix} s_2^{(n)} \end{bmatrix}^T \triangleq \Delta t_2 \begin{bmatrix} 1 & 0 & 1 & 1 & \dots & 1 \end{bmatrix}$$

$$\begin{bmatrix} s_3^{(n)} \end{bmatrix}^T \triangleq \Delta t_3 \begin{bmatrix} 1 & -1 & 1 & 0 & \dots & 0 \end{bmatrix}$$

$$\begin{bmatrix} s_4^{(n)} \end{bmatrix}^T \triangleq \Delta t_4 \begin{bmatrix} 0 & 1 & -1 & 1 & \dots & 0 \end{bmatrix}$$

$$\vdots$$

$$\begin{bmatrix} s_n^{(n)} \end{bmatrix}^T \triangleq \Delta t_n \begin{bmatrix} 0 & 0 & \dots & 1 & -1 & 1 \end{bmatrix} \quad (2)$$

where the variables are defined in the appendix. Based on Eq. (2), a general n -step transient switching sequence will be

$$s^{(n)} = s_1^{(n)} + s_2^{(n)} + \dots + s_n^{(n)} \quad (3)$$

or

$$\begin{bmatrix} \Delta t_1^{(n)} \\ \Delta t_2^{(n)} \\ \Delta t_3^{(n)} \\ \Delta t_4^{(n)} \\ \vdots \\ \Delta t_n^{(n)} \end{bmatrix} = \begin{bmatrix} \Delta t_0 + \Delta t_2 + \Delta t_3 + 0 + 0 + \dots + 0 \\ \Delta t_0 + 0 - \Delta t_3 + \Delta t_4 + 0 + \dots + 0 \\ \Delta t_0 + \Delta t_2 + \Delta t_3 - \Delta t_4 + \Delta t_5 + 0 + \dots \\ \Delta t_0 + \Delta t_2 + \Delta t_3 + \Delta t_4 - \Delta t_5 + \Delta t_6 + 0 \dots + 0 \\ \vdots \\ \Delta t_0 + 0 + 0 + \dots + \Delta t_{n-2} - \Delta t_{n-1} + \Delta t_n \end{bmatrix}$$

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[†]Also at the University of Wisconsin, Madison, WI.

[‡]Also at Oakland University, Rochester, MI.

$$\begin{bmatrix}
 1 & 1 & 1 & 0 & \dots & 0 \\
 1 & 0 & -1 & 1 & 0 & \dots & 0 \\
 1 & 1 & 1 & -1 & 1 & 0 & \dots & 0 \\
 1 & 1 & 0 & 1 & -1 & 1 & 0 & \dots & 0 \\
 \vdots & \vdots & \vdots & \vdots & \vdots & \vdots & \vdots & \vdots & \vdots \\
 1 & 1 & 0 & \dots & 0 & 1 & -1 & 1
 \end{bmatrix}
 \begin{bmatrix}
 \Delta t_0 \\
 \Delta t_1 \\
 \Delta t_2 \\
 \Delta t_3 \\
 \Delta t_4 \\
 \vdots \\
 \Delta t_7
 \end{bmatrix}
 =
 \begin{bmatrix}
 A_3 \\
 \Delta t
 \end{bmatrix}
 \quad (4)$$

The matrix $\begin{bmatrix} A_3 \\ \Delta t \end{bmatrix}$ is called the characteristic transient matrix of the three-phase ICB. Other multiphase ICB circuits will have their own unique characteristic transient matrix.

Frequency Shifting Transients

The frequency of operation of the ICB circuit is altered by increasing or decreasing the time intervals between the SCR firings on both converters. The simplest way of changing frequency, while observing constraint 5, is a three-step transient, Fig. 3. Note that the fundamental transient for frequency shifting, $S_2^{(n)}$, is different from that for phase shifting, $S_3^{(n)}$. It can be seen, from inspection of Fig. 3, that a pure frequency shift will alter the ICB operating phase. To correct for this, a three-step compensating phase shift may be superimposed on the frequency shifting transient. The resulting transient sequence, from Eq. (3) will be

$$S^{(3)} = S_1^{(3)} + S_2^{(3)} + S_3^{(3)} \quad (5)$$

If more transient steps are necessary to meet the constraints, the general Eqs. (2), (3) and (4) are used.

It is possible to control both phase and frequency in one transient sequence. A phase-frequency shifting transient sequence is produced exactly as a frequency shifting sequence. However, the phase compensating sequence is adjusted to produce a new phase rather than maintain the original phase.

The Constraint Equations

The fastest way of computing the intervals of a phase-frequency shifting transient, in real time, is to solve the circuit constraint equations simultaneously. For example, constraints 1 and 2 may be formulated, respectively, as follows:

$$\begin{aligned}
 \sum_{i=1}^n [\Delta t_{i5}^{(n)} - \Delta t_{iL}^{(n)}] &= \Delta[\Delta\phi] = \frac{\phi_1 - \phi_0}{60^\circ} (\Delta t_1) \\
 &= \Delta\phi_1 - \frac{\Delta t_1}{\Delta t_0} (\Delta\phi_0) \quad (6)
 \end{aligned}$$

$$\Delta t_2 = \Delta t_1 - \Delta t_0 = \frac{1}{6} (T_1 - T_0) = \frac{\pi}{3} \left(\frac{1}{\omega_1} - \frac{1}{\omega_0} \right) \quad (7)$$

Constraint 5 is represented by Eq. (4). Constraints 3, 4, and 6 may similarly be translated to equation form.

A Frequency Shifting Algorithm

The next best way to the analytical calculation of the transient intervals is choosing the intervals in a reasonable way and then testing them against the circuit constraints. The following is an example algorithm for computing the intervals of a frequency shifting transient.

$$\Delta t_1^{(n)} = \frac{\Delta\phi_0}{\Delta t_0} [\Delta t_{i-1}^{(n-1)} - \Delta t_1^{(n-1)}] + \Delta t_1^{(n-1)} \quad (8)$$

This is an iterative formula that computes the intervals of an n-step transient from those of an (n-1)-step transient. The iteration starts from the fundamental or three-step transient.

$$\Delta t_1^{(3)} = \frac{\Delta\phi_0}{\Delta t_0} (\Delta t_0 - \Delta t_1) + \Delta t_1 \quad (9)$$

This algorithm produces smoother transition from one frequency to the next as the number of transient steps are increased. Thus, the chances of meeting constraints 3 and 4 are improved. The commutation threshold curves (constraint 4) of the three-step transient have been computed by a digital computer and shown in Figs. 4 and 5.

Conclusion

The instantaneous equation of the load average power in the ICB circuit shows that this system variable can be controlled by the frequency and relative phase of the converters. Real time control of phase frequency requires inducing transient switching sequences on the converters. These sequences must comply with the six constraints for successful and optimal operation of the system.

This paper presented a fundamental technique for shifting phase frequency while maintaining phase voltage balance. The technique produces (n-1) unknown time increments where n is the number of transient intervals. These unknowns are to be calculated from the other constraint equations, two of which are presented.

As an alternative, a method of "picking" reasonable transient intervals and then checking them against the constraints is suggested.

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Appendix

Definition of Symbols

- $\langle i_s \rangle$ = average storage coil current.
 $\langle i_L \rangle$ = average load coil current.
 ω = operating bridge frequency in radians.
 ω_0 = operating bridge frequency before transients.
 ω_1 = operating bridge frequency after transients.
 ϕ = lead angle of the load bridge over the storage bridge in degrees. One bridge cycle is 360° .
 ϕ_0 = lead angle of the load bridge before transients.
 ϕ_1 = lead angle of the load bridge after transients.
 $\Delta\phi$ = increment of lead angle of the load bridge.
 Δt_0 = firing interval before transients.
 Δt_1 = firing interval after transients.
 Δt_2 = increment of time interval to change the operating frequency.
 Δt_ϕ = lead time of the load bridge over the storage bridge in seconds.
 $\Delta[\Delta t_\phi]$ = increment of lead time of the load bridge.

- Δt_{ϕ_0} = lead time of the load bridge before transients.
 Δt_{ϕ_1} = lead time of the load bridge after transients.
 $\Delta t_{iS}^{(n)}$ = i th interval of an n -step transient on the storage bridge.
 $\Delta t_{iL}^{(n)}$ = i th interval of an n -step transient on the load bridge.
 Δt_i = increment of time interval corresponding to the i th sequence component.
 $[\Delta t]$ = vector of time increments.
 $S^{(n)}$ = sequence vector of the intervals of an n -step transient switching.
 $S_i^{(n)}$ = i th sequence component of an n -step transient.
 $S_S^{(n)}$ = sequence vector of the transient intervals on the storage bridge.
 $S_L^{(n)}$ = sequence vector of the transient intervals on the load bridge.
 $[A_{j\phi}]$ = characteristic transient matrix of a j -phase bridge.
 T = the period of one bridge cycle in seconds.
 T_0 = bridge period before transients.
 T_1 = bridge period after transients.

SL1 SL5, SL1 SL6, SL2 SL6, SL2 SL4,
 SL3 SL4, SL3 SL5, SL1 SL5, ...

Fig. 1. Circuit Diagram for the 3-Capacitor Model IC Bridge.

Fig. 2. Phase voltage waveforms of a three-step phase shift.

Fig. 4.

Fig. 3. Phase voltage waveforms of a three-step frequency shift.

Fig. 5.

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