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OVERVIEW

This report is a summary of work performed during 1981-82. The studies and experiments described are focused on the timely development of exhaust and impurity control systems for magnetically confined fusion. Areas investigated include divertor engineering (magnetic fields, thermal-hydraulics, shielding and target design); impurity and plasma transport analyses to assess effectiveness of the designs; and design and tests of high heat flux limiter concepts.

It was found that an optimized cascade T-shaped bundle divertor can be designed to be superconducting and meet the engineering constraints of a power reactor. For example, two-dimensional neutronic studies by General Atomic show that the 60 cm of space available in front of the divertor will provide adequate shielding. Monte Carlo modeling of such a divertor indicated that plasma thermal conductivity is increased only 40% over the neoclassical value for an axisymmetric plasma and that the divertor will enhance outward impurity transport compared with the net inward diffusion of an undiverted axisymmetric tokamak. A long pulse divertor was designed to test these results on TEXTOR, and the possible advantages of cryogenic cooling explored.

A key hole toroidal hybrid divertor concept has also been developed. This is a thin, poloidally elongated bundle divertor that is shaped to the plasma surface. As a result, $q_D$ is smaller and the diversion efficiency is higher. Such a divertor can be operated in a pulsed mode in a high-field compact device like Alcator. The divertor is small, inexpensive to build, and disposable.

The bundle divertor developed and built for ISX-B was successfully tested at MIT for electrical, magnetic, and structural characteristics. A heat load measurement on a forced flow, water cooled, heavy-wall molybdenum tube has been carried out by Westinghouse. About 1.2 kW/cm$^2$ was handled without any noticeable damage over many pulses. A 2 kW/cm$^2$ heat load was also tested, but with some local melting due to peak load higher than 2 kW/cm$^2$. A water cooled limiter was designed by McDonnell Douglas with a 1 mm molybdenum skin on 2 mm copper. From the testing results and computations, it is believed that the limiter can be operated safely at a 2 kW/cm$^2$ heat load in an Alcator plasma. The theoretical advantages of porous surfaces for enhancing redeposition of sputtered material were separately studied and found to reduce sputtering by factors of two.

A kinetic model of plasma/neutral gas transport in a bundle divertor is being developed to provide a better basis for the engineering design of divertor targets, as well as assessing impurity control features. Using present information, a scoping study of divertor solid targets confirmed the advantages of thermally connected, mechanically unbonded designs, and identified key thermal-hydraulic factors and data uncertainties for water cooling up to 2 kW/cm$^2$. A supersonic jet target
was proposed for recovering both energy and particles from a bundle divertor. The concept also has implication for future application in rocket nozzle cooling and thrust control with chemicals on fusion engines.

A novel self-protective blistering process in a composite material C-SiC alloy was proposed and tested by General Atomic. The blistering mechanism may limit the erosion of plasma interface components subject to disruptive energy load in tokamaks. Additionally, some C-SiC alloy coatings were tested and did not blister, surviving to the limits of the graphite substrate with only microcracking. Both coatings appear suitable for limiter application. A method and mechanism for testing and developing limiters on Doublet III was also designed by General Atomic.

Publications and Presentations

1.0 Magnetic Systems


2.0 Magnet Shielding


3.0 Divertor Physics


5.0 Divertor Target Design


- Investigation of Tokamak Solid Divertor Target Options, J. McMurray, N. Todreas, B. Mikic, P. Gierszewski, PFC/RR-81-23.


6.0 Limiter Design

1.0 BUNDLE DIVERTOR DESIGN

1.1 Optimization and Monte Carlo Modeling of Bundle Divertors

This paper describes a Monte Carlo study of the thermal conductivity and impurity transport in a tokamak with an optimized bundle divertor configuration. This configuration has been designed to obtain the best plasma performance. When collisions and electric fields are included, the thermal conductivity was found to be 40% larger than that of an axisymmetric neoclassical value. Without a divertor the oxygen impurity was found to diffuse towards the center of the plasma as predicted theoretically. However, this impurity diffused outwardly when the divertor was turned on. Few impurity particles diffused into the plasma when launched in the scrape-off layer. This optimized bundle divertor meets the engineering constraints of a power producing reactor.

1.1.1 Introduction

A bundle divertor [1,2] is a set of coils which creates a separatrix on the toroidal magnetic field of a tokamak and diverts a bundle of field lines from the outer layer of the plasma into a separate divertor chamber. In contrast to a poloidal divertor, the toroidal symmetry of the plasma is destroyed. This perturbation creates a localized magnetic field ripple and poses many problems in plasma confinement and tokamak engineering.

From the confinement point of view, the localized ripple may induce ergodicity of the magnetic surfaces, enhance the diffusive loss of thermal particles, and enhance the direct loss of energetic particles. From the engineering point of view, a very large localized current is needed to produce a separatrix with strong toroidal magnetic field. This results in very large magnetic forces. The space available for the conductor and neutron shielding is limited due to competing effects of reducing the current density in the conductor and the perturbation on the toroidal field.

To study the confinement the computation methods are as follows: MHD equilibrium flux surfaces are obtained for a typical reactor plasma such as INTOR [7]. The flux surfaces are retraced by superimposing the axisymmetric equilibrium surfaces with the divertor coils. Prominent magnetic island structures were observed for the cascade divertor. The thermal particles and impurities are launched on the flux surfaces. Their orbits are tracked using the guiding center equation to the second order of the magnetic moment $\mu$. Collisions and electric fields are included. Many trapping and detrapping occurs along the particle orbit, thereby justifying the diffusive model of transport. In general the thermal conductivity is enhanced by about 40% over axisymmetric neoclassical value. The impurity will diffuse towards the center of the plasma without the divertor...
when they are launched at a surface halfway between the center and the edge of the plasma. They will diffuse outward when the divertor is turned on.

1.1.2 Magnetics

Many coil configurations have been proposed to reduce the ripple or to reduce the current density in the conductor. Nine different configurations have been studied and are illustrated in Fig. 1. The ripple for the T shaped coils are even negative at the radii less than the major radius. The ripple for the multiple T coils for various sizes all fall in the shaded region. The ripple on axis is reduced by a factor of 4. There is little further reduction by varying the configurations.

For all the configurations other than T shaped coils, the current requirements are very large and the current densities are not practical for engineering consideration. To reduce the current density the coil radius or width has to be increased. This makes the coil maintenance difficult. The double T shaped coils give the best expansion, lowest ripple, and smallest current density. Therefore, the X and reversed T coil configurations can be eliminated from a practical standpoint. A typical toroidal magnetic flux pattern for configuration (h) is shown in Fig. 3. Aside from the reduction in current requirement and ripple, the stagnation axis is concaving towards the plasma instead of concaving towards the divertor like DITE [1] and the mirror ratio is also reduced. These two features will improve the efficiency. The ergodicity of the magnetic surfaces for solenoidal coils with constant radius, expanding radius, and T shaped coils were studied and are now discussed.

To compute the flux surfaces the axisymmetric MHD equilibrium surfaces are superimposed with the perturbed toroidal field. The poloidal flux surfaces are computed by using the PEST code [15] and are shown in Fig. 4. The plasma parameters are that of INTOR $R_o = 5.4$ m, $a = 1.6$ m, $\beta = 6\%$, $q = 2.8$, $I_p = 7.8$ MA and $B_o = 5.3$ T, which are typical of a power reactor. This was chosen for the purpose of illustrating the engineering feasibility discussed in Section 5. Some care is needed in selecting the poloidal flux configuration for a high $\beta$ noncircular plasma. For a noncircular high $\beta$ plasma the flux surfaces are closer together in the area beyond the magnetic axis. The poloidal separatrix is very close to the plasma surface even if it is defined by a limiter or toroidal separatrix. As shown in Fig. 4, the poloidal separatrix is more than 20 cm away, larger than the scrape-off layer, otherwise the poloidal divertor would be competing with the limiter or bundle divertor. On the inner side of the plasma the flux surfaces are less dense and the wall has to be at least 30 cm away; otherwise, the inner wall will become the effective limiter. These are common problems for both bundle divertor and mechanical limiter methods.

The two representative flux surfaces computed at $\phi = 0^\circ$ for configurations e and h are presented in Fig. 5. The fluxes surfaces for single T coils are ergodic. The flux surfaces for solenoids with expanding radius, double T and triple T are nonergodic.
1.1.3 Confinement Characteristics

In the previous section we have determined that the cascade T bundle divertor is the optimum configuration. It is not detrimental to the gross confinement since the flux surfaces are nonergodic. In this section we would like to test the microscopic confinement characteristics by following the guiding center orbit of the test particles and study the effect of the ripple on particle diffusion.

The thermal conductivity is computed numerically using a Monte Carlo guiding center particle orbit code [16] which was modified to include the real coil configuration of the T-shaped divertors and electric field. The guiding center equations, which are accurate to the second order [17], are

\[ V = \frac{V_{\parallel}}{B} \left[ B + \nabla \times (\rho_{\parallel} B) \right] + \epsilon(0), \]  

and

\[ \frac{dV_{\parallel}}{dt} = -\frac{\mu}{m} \frac{B_0}{B} \nabla B + \frac{q}{m} E_{\parallel} + 0(\epsilon), \]

and

\[ E_{\text{total}} = \frac{m}{2} V_{\parallel}^2 + \mu B + z e \phi + \frac{m}{2} V_{\phi}^2 + E_{\phi} B + \int_{0}^{t} z e E_{\parallel} V_{\parallel} dt, \]

where \( \rho_{\parallel} = mV_{\parallel}/eB \).

In computing the MHD equilibrium flux surfaces the toroidal field profile used is [15]

\[ B_t = B_0 R_g g, \]

where

\[ g = \left[ 1 - g_p \frac{(\psi - \psi_o)}{(\psi_L - \psi_o)} \right], \]

and the pressure profile used is

\[ P = P_o \left( \frac{\psi - \psi_o}{\psi_L - \psi_o} \right)^\beta, \]

where \( \psi_o \) and \( \psi_L \) are fluxes at the magnetic axis and limiter, respectively. \( P_o \) is the peak pressure and \( \alpha, \beta \) and \( g_p \) are constants. To obtain temperature and density profiles we write

\[ P = n_o(\psi) T_o(\psi) \]

\[ = n_o T_o \left( \frac{\psi - \psi_o}{\psi_L - \psi_o} \right)^{\beta_n} \left( \frac{\psi - \psi_o}{\psi_L - \psi_o} \right)^{\beta_p} \]

1.3
To study the interaction of test particles with background plasma the following Coulomb collision terms \[16\] were included in the ion collisional operator: electron drag, ion drag, ion pitch angle scattering, and ion energy scattering.

The electric fields can be derived from the MHD relationship which gives

\[ E_x(\psi) = -\frac{1}{2\pi} \nabla P , \]

and

\[ E_\phi(R) = \frac{1}{\sigma} J_\phi(R) , \]

where \( J_\phi = 2\pi(R^2 p' + B_0^2 R'_0 g g'/\mu, R) \) and \( \sigma \) is the Spitzer conductivity. The flux in the ripple region is modified according to the form of Eq. (1).

The typical orbits for a 10 keV particle launched at the edge and halfway inside the plasma are shown in Fig. 6. The particle launched near the edge is drifting into the divertor channel as shown by Fig. 6a. Figure 6b shows the orbit of the particle launched in the middle of the plasma and inside the ripple. Figure 6c shows the variation of the phase angle \( \psi_\parallel/\psi \). The orbit and phase angles oscillate rapidly when the particle is trapped inside the ripple. Banana orbit and phase angle oscillate slowly when it is detrapped. The particle can detrap itself by drifting upward into the lower ripple region above the mid-plane. Collisions and electric field can also change the ripple trapped orbits into banana or circulating orbits, and vice versa. Many trappings and detrappings occur along the particle orbit, thereby justifying the diffusive model of transport.

The relaxation of the particle distribution as a function of flux at 0.11 ms, 3.3 ms and 11 ms is shown in Fig. 7. The measured thermal conductivity for the tokamak with the optimized divertor is \( \chi = 0.29 \) which is 40% larger than the axisymmetric neoclassical conductivity \( \chi_{NC} = 0.21 \). As shown in Fig. 8 a variational study shows that: (1) The thermal conductivity is reduced when the toroidal separatrix is separated from the plasma surface by approximately half the width of the island. This is due to the fact that the separatrix will be outside the island and boundary surface will stay closed. Otherwise the boundary surface will be open and cause large heat leakage across the islands. This suggests that bundle divertors can be used for burn control by changing the separatrix position; (2) The conductivity is reduced when the distance between the divertor and the plasma is increased with height. This is due to the fact that the effect of the fringe field of the divertor is reduced; (3) Because of the higher order multipole effect, the confinement is improved by using more T coils. The choice has to be made based on the trade-off of reasonable confinement and engineering constraints.
1.1.4 Impurity Transport

Several factors must be included in impurity transport calculations. If the impurity is not fully ionized, the line radiation and charge exchange have to be considered. Therefore, oxygen is chosen as the test particle for impurity. The plasma temperature is assumed to be 300 eV in the scrape-off layer. The oxygen is fully ionized. Four cases were studied: The impurity transports inside the plasma and in the scrape-off layer with and without the divertor.

The impurity is launched on the flux surface at 25% of the boundary. As shown by the first column in Fig. 9 and Fig. 10a, the impurity moved to the center of the plasma for axisymmetric tokamak. When the divertor is turned on the impurity diffuses inward as well as outward like the thermal particle shown by the second column in Fig. 9 and Fig. 10b. 12% of the impurity launched has left the plasma. The inward impurity transport in axisymmetric toroidal plasma has been discussed in detail [19-22]. The impurity would diffuse toward the center of the plasma due to the frictional force which is given by

\[ F_{zi} = m_{zi} n_i \nu_{iz} (V_{i\theta} - V_{i\phi}), \]  

(14)

and

\[ \dot{V}_{i\theta} = \frac{\nabla P_{i\theta}}{(ze) n_i B_\phi}, \]  

(15)

where

\[ \nu_{iz} = \frac{2n_z e^4 \ln \Lambda}{3(2\pi)^{3/2} \varepsilon_0^2 m_{zi}^{1/2} (kT)^{3/2}}, \]  

(16)

is the coefficient of friction, \( m_{zi} = \frac{m_z}{m_z + m_i}, \) \( \Lambda = 12\pi(\varepsilon_0 T e^2)^{3/2} / \sqrt{n_e}, \) and \( \varepsilon_0 \) is the dielectric constant and the subscripts \( z \) and \( i \) denote impurity and ion respectively. This frictional force gives rise to an averaged radially inward drifting velocity

\[ \dot{V}_{r} = -\frac{\vec{F}_{zi}}{n_i ze} B_\phi. \]  

(17)

Another inward drifting term is the Ware pinch [23] given by

\[ V_{r}^{E \times B} = -\frac{E_\phi}{B_0}. \]  

(18)

For heavy ions the vertical drift due to field gradient \( \frac{\mu}{ze} \frac{B \times \nabla B}{B^2} \) is reduced by a factor of \( z \) in comparison with a light ion having the same \( \mu \), therefore \( V_{r}^{f} \) and \( V_{r}^{E \times B} \) become the dominant drifts. Since the impurity is confined by the strong electrostatic potential well \( ze\phi \) [24] they are driven into the center due to the frictional force and Ware pinch. Since the frictional force is
related to \( E_r \) through \( \nabla P \), there will be no inward diffusion when the electric field is not included as shown in Fig. 11. Taking \( T_i = 10 \) keV, \( n_i = 3 \times 10^{20} \) m\(^{-3} \) and \( E_{\phi} \) approximately 0.5 V/m and \( B_0 \approx 0.5 \) T, then the average inward drifting velocities are \( \dot{V}_f = -12 \) m/s and \( \dot{V}_{E \times B} = -1 \) m/s. The drift due to friction is 10 times larger than the Ware pinch. The total inward drift is estimated to be 0.6 m in 50 ms which approximately agrees with the Monte Carlo result.

The divertor clearly prevents the impurity from concentrating in the center; instead they diffuse outwardly and are removed by the divertor. The ripple, due to the divertor, is a source for anomalous transport. When the impurity was launched in the scrape-off layer without the divertor, they hardly diffused due to the low temperature at the edge. As shown by Fig. 10c, the impurity diffuses rapidly when the divertor is turned on. Very few of them diffuse into the center of the plasma and most of them are removed by the divertor. At the end of 50 ms of tracking time, only 30% of the impurity launched remains. The impurity transport in the scrape-off layer depends on the temperature. A more detailed study is needed.

1.1.5 Engineering Feasibility

As is discussed in the introduction, the most attractive feature of a bundle divertor is its maintainability and the possibility for external cleaning. However, the high current density, large forces, and lack of shielding spaces make the bundle divertor engineering difficult. This section will examine the answer to these problems for the configurations studied. Let us specify the criteria for a feasible divertor. In order to keep the power consumption low the divertor coil has to be superconducting. The reasonable average current density for a stable superconducting coil at 10 T maximum field should be less than \( 5 \) k Amp/cm\(^2 \) [13]. A commercial Nb\(_3\)Sn cable which can carry 3.5 k Amp/cm\(^2 \) is available. Therefore this value is chosen as the current density criterion. In order to protect the insulation material and the superconductor a 60 cm shield of Tungsten and borated water composite is chosen which will give a lifetime of 5 MW years [6]. The forces are not the worst problems. A 100 MN force can be properly handled [14]. However, it should be kept as low as possible. The force is reduced when the current and coil size are reduced and the divertor coils are situated in the weaker toroidal field region. The last criterion allows easy maintenance by means of a plug-in unit.

In this study the width of the divertor is kept smaller than the space between the TF coils as shown in Fig. 3. The outward translational force is about 20 MN. There is 60 cm of shielding space in front of the coils facing the plasma. Therefore these configurations satisfy the engineering criteria as well as giving good confinement. The divertor coil height has been varied from 1.4 m to 2.2 m and the distances from the plasma have been varied from 1.0 m to 1.2 m. The flux surfaces for these cases are all nonergodic and ripple is reasonable. The current increases from 8.75 to
12.3 and 13.55 MA-T when the distance increases from 1.0 m to 1.1 m and 1.2 m. However, the current density can be kept constant by increasing the conductor cross-section proportionally. This demonstrates that a range of designs can be obtained. The choice is a matter of trade-off.

The engineering concept of a cascade bundle divertor and a monolithic bundle divertor assembly are shown in Fig. 12. The divertor assembly is a single unit construction. The forces are transmitted to TF coils through the two horizontal bars and heat stations which are specially designed to minimize the heat leakage and disconnection time. The bars are keyed to the divertor casing and attached to the heat station by a cylindrical bearing. The divertor assembly can be freed and extracted simply by lifting up and dropping down the bars.

1.1.6 Conclusion

The Monte Carlo modeling of the confinement and impurity transport of a tokamak with a cascade bundle configuration has shown that the thermal conductivity is enhanced only 40% over neoclassical value for axisymmetric plasma and that the divertor will increase the outward impurity transport. The bundle divertor will screen the impurity. However, the screening effect may depend on the plasma edge temperature and species. A very careful study is needed for designing and carrying out an experiment. A tool for such a study can be developed based on this work.

The study here also shows that the confinement characteristics of a tokamak system with a bundle divertor is strongly influenced by the divertor coil configuration. A wide range of configurations exist which do not cause ergodicity on the flux surfaces in the plasma although magnetic islands are formed due to the ripple.

The toroidal separatrix should not be placed on the boundary determined from MHD equilibrium calculation because of finite island width. The poloidal separatrix should be carefully located outside the scrape-off layer.

The cascade T-shaped configuration represents the best trade-off and can be designed so that the key engineering constraints can be met. Further shape optimization is possible, such as bending the coil toward the plasma and/or situating the coil away from the middle plane. The effect on energetic particles, alpha particle confinement, and on RF heating has to be studied. All this work will be carried out when the computational time of the code is reduced. This code modification is in progress.

REFERENCES


Fig. 1. Divertor coil configurations studied. Configurations (a) through (d) are larger than the space between two adjacent TF coils. Configurations (e) through (i) are smaller than the said space.
Fig. 2. Divertor ripple on axis for the configurations studied. The ripple for multiple T configurations are the lowest. They all fall within the shaded region.
Fig. 3  A typical toroidal magnetic flux pattern computed for the divertor coil configuration in Fig. 1.
Fig. 4. Flux surfaces used in this study. This illustrates that the distance between the plasma boundary defined by the bundle divertor and poloidal separatrix has to be greater than that of the scrape-off layer thickness.
Fig. 5. Magnetic flux surfaces of the tokamak with a bundle divertor. Picture (a) is for configuration Figure 1e and picture (b) is for configuration Figure 1h.
Fig. 6. The orbits of a 10 keV particle launched at the edge and middle of the plasma and inside the ripple. Picture (a) shows that the particle is drifting into the divertor. Picture (b) shows that the particle was first trapped in the ripple and detrapped a fraction of a ms later and became a banana orbit. This effect is further verified by the fluctuation of the phase angle as a function of time in picture (c).
Fig. 7. The relaxation of the test particles in a Maxwellian plasma launched on a surface at $\psi = 25\%$ of $\psi$ separatrix.
Fig. 8. Relative thermal conductivity of a tokamak with a bundle divertor versus radial position and height of the divertor. $\Delta R_{\text{sep}}$ is the separatrix from the axisymmetric plasma boundary.
Fig. 9. The diffusion of full ionized oxygen. The oxygen will diffuse toward the center of the plasma 16 ms after being launched without a divertor. It diffused outward when the divertor is on. The scales are in units of meters.
The impurity profiles as a function of flux surfaces at various times: (a) no divertor, (b) with divertor, and (c) impurity launched in the scrape-off layer.

Fig. 10
Fig. 11. The diffusion of oxygen in axisymmetric toroidal plasma when the electric field is not included. The dashed curve is at 0.11 ms, the solid curve is at 3.3 ms and the solid dot is at 49.5 ms.
Fig. 12. The engineering concept of a plug-in bundle divertor.
1.2 A TOROIDAL HYBRID DIVERTOR – KEY HOLE DIVERTOR

1.2.1 Introduction

The optimization of a regular bundle divertor has been discussed in a previous section. A cascade T-shaped bundle divertor will improve the efficiency and be engineeringly feasible for large tokamaks having intermediate magnetic field strength. Regular bundle divertors have two main drawbacks: (1) the number of turns a flux tube will travel around the torus before being diverted is usually greater than 10, so the efficiency is lower than that of a poloidal divertor; and (2) the bundle divertor was considered to be extremely difficult to design for a compact high field tokamak. A toroidal hybrid bundle divertor will reduce $q_0$ and improve the efficiency. It can also be inserted into the narrow access slots of a compact device. Several divertors pulsed sequentially will solve the long pulse needs of a high field compact reactor.

1.2.2 The Toroidal Hybrid Divertor

The meaning of the toroidal hybrid bundle divertor, the toroidal divertor, the bundle divertor and the poloidal hybrid divertor is described in the following. The toroidal divertor is a conductor ring around the plasma. A complete circle of null trace in the toroidal magnetic field is generated. This type of divertor has been used on stellarators at Princeton [1]. The disadvantage is its substantial perturbation on the toroidal field. In the model C stellarator, the toroidal magnetic field perturbation is about 60% at the center of the plasma. The bundle divertor is a variation of the toroidal divertor [2]. Two opposing current loops adjacent to each other divert a small bundle of magnetic flux. The main advantage of this approach is that it produced only a minor perturbation ($\sim 1\%$) in the field at the center of the plasma. The disadvantage would be that it would take many turns around the torus before being diverted. The diverting efficiency is much reduced. The poloidal hybrid divertor makes the two adjacent coils into a T-shape [3]. The horizontal arms were extended in the toroidal direction so that they are acting like partial poloidal coils. These conductors will help to pull the flux outward from the plasma gradually so that the current requirement in the coils can be reduced. The advantage is that null point is not required and the perturbation to toroidal field is further reduced. The difficulty is that the long arm makes the engineering difficult and the magnetic surface will become ergodic.

As is shown by Fig. 1, the toroidal hybrid divertor proposed here is a T-shaped coil with short horizontal arms but long vertical legs extended in a poloidal direction. All the legs will bend toward the plasma and be contoured like the plasma surface. The null trace is an arc which nearly has the same curvature of the plasma surface. The height of the divertor is larger than the
plasma minor radius. $q_D$ will become 6 or less for this divertor. This is about half of the $q_D$ of a regular bundle divertor. This means that larger bundles of flux can be diverted in less than 6 turns around the torus. Therefore, the efficiency should be much improved. A Monte Carlo simulation study of diverting efficiency has been started and will be discussed the the future. In addition to the improvement in efficiency another advantage is that the divertor can be made very narrow so that it will fit into a very narrow access of a compact high field tokamak like Alcator. Such an application will be discussed in the next section.

1.2.3 Toroidal Hybrid Divertor for Alcator

The plasma edge conditions of Alcator have very high particle and power densities (about $5 \times 10^{13}$ cm$^{-3}$ and 5 kW/cm$^2$) which is near reactor regime. Due to this high power density, the limiter cannot last very long. Thus, Alcator would be a very significant test for a divertor. The access slots in Alcator C are only 4.5 cm wide and 30 cm tall. It is quite a challenge to design a divertor which can fit such a narrow port. Utilizing the full space as much as possible, the half-width is chosen to be the horizontal opening of the port. The divertor must therefore be built in two halves, each inserted separately into the vessel. The overall divertor width is slightly larger than the port opening. Based on this concept the divertor magnetic configuration is shown in Fig. 2. The coil configuration is shown in Fig. 1. The plasma and divertor parameters are listed in Table 1. The current density is 180 kAmp/cm$^2$. The divertor has to be pulsed. To test the concept a 20 ms pulse is sufficient. The coils are precooled by liquid nitrogen (to 70 K) or liquid helium gas (to 15 K). For 20 ms flat top and 2 ms rising and decaying time the temperature rise is 75 K. It takes 5 minutes to cool down after the pulse. Major advantages of such a small pulsed system are that: (1) the net force is small (less than 12 kN in this particular design) and easy to handle; and (2) the fabrication is much easier and less expensive. The divertor flux bundle is shown in Fig. 3a. The flux bundle is now expanded vertically. The null trace is shown by Fig. 3b. The trace has the same curvature as the plasma surface. The calculated $q_D$ is 6. These two effects will improve the efficiency of the current bundle divertor. The comparison of ripple of the divertor studied is shown in Fig. 4. The ripple has been drastically reduced and the zero ripple has been moved further out from the axis.

REFERENCES

Table 1

a.) Alcator C Plasma Parameters for Divertor Operation

\[ R = 64 \text{cm} \]
\[ a = 14 \text{cm} \]
\[ B_o = 5 \text{Tesla} \]
\[ q_o \sim 4 \]

b.) Alcator C Bundle Divertor Parameters (see definition on Fig. 2)

<table>
<thead>
<tr>
<th>( x_o )</th>
<th>( x_e )</th>
<th>( y_e )</th>
<th>( z_e )</th>
<th>( I ) (kAmps)</th>
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</thead>
<tbody>
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<td>80</td>
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<td>82</td>
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<td>3.3</td>
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</tbody>
</table>
Fig. 1. Perspective view of a toroidal hybrid key hole divertor.
Fig. 2. Plane view of the key hole divertor on Alcator C.
Fig. 3. Diverted flux bundle of a key hole divertor for Alcator C.
Fig. 4. Divertor ripple on axis for the configurations studied. The ripple for single "T" and cascade are the lowest. They all fall within the shaded region.
1.3 TEXTOR DIVERTOR DESIGN

1.3.1 Introduction

The overall objectives of TEXTOR [1], are to evaluate the processes leading to impurity build-up in tokamaks and to damage of the first wall under different operating conditions; to search for appropriate first wall materials, structures and temperatures that are optimized with respect to particle release and wall material behavior; and to develop and test methods to control the boundary. To accomplish the first objective, some control mechanism is needed. TEXTOR has given the bundle divertor considerable thought as has been reported in the Divertor Workshop held at Culham Laboratory in England in 1977. There are provisions for two such divertors to be installed.

An optimization study [2] has shown that the performance of the bundle divertor is favored in a large device with a large aspect ratio among the existing tokamaks. TEXTOR is thus a good device for operating a bundle divertor.

High poloidal beta and long pulse (5–10 seconds) may require effective impurity control methods of which none currently exist. The bundle divertor has proven at least 30% efficiency at less favorable conditions, hence this option should be vigorously investigated and supported. The simplified plane view of TEXTOR with the implementation of an advanced bundle divertor is shown in Fig. 1. Figures 2 and 3 show cross sectional and trimetric views. There are two large ports of $60 \times 80$ cm$^2$ each on the midplane of the vessel for housing the bundle divertor which are currently being used as beam dumps. With these ports used for the bundle divertor, the beam dump could still fit into the large space between the vacuum vessel and the divertor. The bundle divertor shown here is a monolithic single unit construction which can be externally assembled and then inserted into the existing space. The divertor coil can be of either copper or superconducting conductors. In the case of a copper conductor the coils can be operated at steady state with 7 MW of power consumption.

1.3.2 TEXTOR Bundle Divertor

The magnetic configuration of a reference bundle divertor designed for TEXTOR is shown in Fig. 4. The diverted magnetic flux design was expanded to reduce the thermal load on the target and increase the pumping efficiency. Coil parameters are listed in Table 1. The plane view and cross sectional view of this divertor as if installed on TEXTOR are shown in Figs. 1 and 2.
The divertor coils are cascade T-shaped. The width is limited by the space between the two adjacent TF coils, as the coils are designed to be inserted into the tokamak or pulled out as a single unit. The cascade T-shaped coil configuration dramatically reduces the ripple and current density in the conductor while making the divertor compact. The magnetic flux surfaces were found to be nonergodic and the particle confinement from the guiding center orbit study was also improved. This bundle divertor would not only provide for a long pulse impurity control experiment on TEXTOR, but the results will also be significant for reactors generally since this cascade-T bundle divertor performs better on larger machines.

In order to select an appropriate dimension and location of the divertor coils within the constraints of the existing space, a variation study has been done. The current and location may be readjusted as the design is refined.

The preliminary thermal characteristics are listed in Table 1. The thermal analysis was done using the same conductor as was used for the ISX-B bundle divertor. The conductor configuration is shown at the bottom of Table 1. The conductor of each turn has a cross section of $1.1 \times 1.1$ cm and the diameter of the cooling channel is 0.4 cm. There is a thickness of 1 mm insulation material (fiberglass tape and G-10 epoxy). The total number of turns is 384. The parameters listed are for steady state operation. The power consumption is less than 7 MW.

The engineering concept of the divertor is illustrated by the trimetric view shown in Fig. 3. The coil containment structure can be a monolithic unit; hence there is only one mechanical vacuum seal, as indicated by dashed lines on the front face, which will be bolted to the flange of the vessel. The passage for the diverted flux and plasma may be machined into the divertor housing. The top, bottom, side and back panels are all removable so that windings can slide into place and be available for improvement or repair. The divertor assembly is attached to the TF coils by two horizontal bars on the top and bottom. The bars are keyed to the divertor casing and attached to the TF coil structure by a cylindrical bearing. The net magnetic forces are transmitted to the TF coils; therefore the force balance for axisymmetric system is restored. This design will minimize the installation and disconnection time. The divertor assembly can be freed and extracted simply by lifting up and dropping down the holding bars.

1.3.3 Comparison of the Cascade T-Shaped and Conventional Circular Divertors

For comparison purposes, three different magnetic divertor configurations for TEXTOR using a DITE MK-type [3,4] divertor have been calculated. The MK-type divertor has planar circular coils. The divertor performance and coil current are very sensitive to the size (radius of the circular coils) and angle $\alpha$ as shown in Fig. 5. The cross section of the coil is also shown in this figure. Comparative coil parameters, current, current densities, and ripple on axis are given in Table 2.
The relative position of the divertor is given by X located on the front surface. The size is given by the width of the front opening. The current requirement for the advanced type divertor at the corresponding location W is given in the last column of Table 2. (The baseline divertor center is located further away at 245 cm.) Figure 6 compares the ripple MK-type and the advanced divertor.

All DITE MK-type divertors have to be placed very close to the plasma and have a larger size. The current density is at least twice as large as the case of the cascade-T type. The resistive heating for the MK-type is judged to be too large for long pulse operation. All the MK-type coils would interfere with the TF coils and the installation and service would be difficult. Due to space constraints the size of the MK-type divertor has to be reduced when it is moved away from the plasma, whereas for the advanced type the size can be fixed for various radial positions.

The advantages of the advanced type TEXTOR bundle divertor may now be summarized as follows:

1. The current density is low enough to permit steady operation at reasonable power consumption.
2. The ripple is low and flux surfaces are not ergodic. Thus the effect of the divertor on the confinement would be much less.
3. The null trace is straight vertically or concave toward the plasma and conforms better to the shape of the scrape-off layer; the diversion frequency, $q_D$, is thus smaller.
4. The successful experiment and data would be reactor significant.

REFERENCES

Table 1
Magnetic and Thermal Design Parameters for TEXTOR
Bundle Divertor Reference Design

\[
\begin{align*}
B_0 &= 2.0 \text{ T} & B_{\text{max}} &= 6.0 \text{ T} \\
R_a &= 175 \text{ cm} & I_d &= 1.35 \text{ MA-T} \\
a &= 50 \text{ cm} & J_d &= 4.5 \text{ kA/cm}^2
\end{align*}
\]

Normal Conductor Option for Steady State Operation

#Turns = 64
\[
\begin{align*}
p_{\text{in}} &= 1.1 \text{ MPa} & p_{\text{out}} &= 0.15 \text{ MPa} \\
T_{\text{in}} &= 27 \text{ C} & T_{\text{out}} &= 57 \text{ C} \\
w &= 60 \text{ kg/s} & T_{\text{copper}} &= 69 \text{ C} \\
V_{\text{in}} &= 13 \text{ m/s} & \text{Power} &= 6.5 \text{ MW}
\end{align*}
\]

Conductor Configuration

\[
\begin{align*}
\text{Total area/turn} &= 1.44 \text{ cm}^2 & \text{Epoxy area/turn} &= 0.23 \text{ cm}^2 \\
\text{Conductor area/turn} &= 1.084 \text{ cm}^2 & \text{Coolant area/turn} &= 0.126 \text{ cm}^2
\end{align*}
\]

Table 2
Comparison of Advanced Bundle Divertors with MK-type
Bundle Divertor of Three Different Sizes

<table>
<thead>
<tr>
<th>Divertor Type</th>
<th>X (cm)</th>
<th>Width (cm)</th>
<th>(\alpha) (deg)</th>
<th>(I_d) (MA-T)</th>
<th>(J_d) (amp/cm(^2))</th>
<th>Ripple (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>MK</td>
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<td>42.5</td>
<td>45°</td>
<td>1.04</td>
<td>8.32</td>
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<td></td>
<td>240</td>
<td>42.5</td>
<td>45°</td>
<td>1.65</td>
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<td>.87</td>
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<td></td>
<td>240</td>
<td>40</td>
<td>60°</td>
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<td>Advanced</td>
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<td>30</td>
<td>T-shaped</td>
<td>1.32</td>
<td>4.4</td>
<td>.3</td>
</tr>
</tbody>
</table>
Fig. 1. Simplified layout of TEXTOR vacuum vessel with the implementation of an advanced bundle divertor. (not to exact scale)

Fig. 2. Cross sectional view of the TEXTOR vacuum vessel with divertor coils. (not to exact scale)
Fig. 3. Illustration of engineering concepts of the TEXTOR bundle diverter assembly.

Fig. 4. The magnetic configuration for a reference TEXTOR bundle divertor. The diverted flux bundle is expanded.
Fig. 5. Comparison of advanced cascade T-shaped TEXTOR bundle divertor (a) and MK-type planar circular TEXTOR divertor (b).

Fig. 6. Comparison of ripples for circular and cascade T-shaped divertors for TEXTOR.
1.4 APPLICATIONS OF CRYOGENIC MAGNETS

1.4.1 Introduction

One possibility for using conventional resistive magnets in fusion reactors is to operate them at low temperatures where resistivity can be orders of magnitude lower than at room temperature, thus reducing the required electrical power [1]. This approach is already used on the ALCATOR tokamak, for example, where the copper Bitter coils are cooled to liquid nitrogen temperatures. However this approach requires power being expended on refrigeration, which increases roughly inversely as the temperature decreases. Simple scaling studies have found a factor of three to ten minimum in magnet power plus refrigeration power, depending on the conductor, at around 10 to 30 K [2,3,10,11,12,13]. Other calculations indicate that the pumping power can be a significant fraction of the total power [9].

The objective of the present work is to identify more accurately the true benefits of cryogenic cooling, taking pumping power, nuclear heating, inefficiencies and magnetoresistance (and other properties) into account. These are particularly important at cryogenic temperatures because small heat inputs can require substantial refrigeration power to remove and completely change the viability of cryogenic cooling. A 1½-D compressible flow computer program, CCAN, has been developed for this purpose. We will describe the results of some simple scaling calculations, including a comparison between cryogenic magnets (liquid nitrogen or gaseous helium cooled) and water-cooled magnets for use in TEXTOR bundle divertor coils and AFTR TF coils. At present, the compressible flow code is being tested and property correlations established. It will be used to perform a more comprehensive analysis of cryogenic cooling for fusion reactor applications.

1.4.2 Power Requirements

Total power, the quantity to be minimized, is roughly the sum of magnet electrical power, refrigeration power and pumping power. The magnet electrical power is \( I^2R \), where \( R \) is the resistance and \( I \) the current. Resistance is a function of temperature, magnetic field strength, and material. We concentrate on pure copper and aluminum because these are convenient conductors. Stronger alloys could be used because pure copper and aluminum are fairly weak, but would have higher resistivities [3]. Alternately, other pure metals such as sodium can have lower resistivities at sufficiently low temperatures, but would be much harder to work with.

Refrigeration power is roughly \( Q_{\text{total}}(T_h/T_c - 1)/\eta_{\text{refr}} \) where \( Q_{\text{total}} \) is heat input, \( T_h \) and \( T_c \) are the sink temperature (i.e. atmosphere) and the coil temperature, respectively; \((T_h/T_c - 1)\) is the refrigeration Carnot efficiency, and \( \eta_{\text{refr}} \) accounts for the non-Carnot, non-ideal refrigeration cycle.
actually used. Heat inputs include resistive heating of the magnet, pumping friction, heat leakage and nuclear heating.

Pumping power is roughly $w \Delta p / \rho$ where $\Delta p$ is the pressure drop, $w$ the coolant mass flow rate, and $\rho$ the coolant density. Pressure drop can be accurately calculated along the magnet coil allowing for compressibility, acceleration and the Joule-Thompson effect (the change of temperature with pressure under isenthalpic expansion).

Summing these terms, the total power required for steady-state operation of cryogenically cooled magnet coils is

$$P = \int A_m j^2 dx + \frac{1}{\eta_{\text{pump}}} \int \frac{w}{p} dp \left[ 1 + \frac{1}{\eta_{\text{refr}}} \left( \frac{T_h}{T_c} - 1 \right) \right] + \frac{Q_{\text{heat}}}{\eta_{\text{refr}}} \left( \frac{T_h}{T_c} - 1 \right)$$

where $P$ is the total power, $j$ is the current density, $\sigma$ the copper conductivity, $A_m$ the magnet cross-sectional area, $x$ the axial coordinate, $\eta_{\text{pump}}$ the pump hydraulic efficiency and $Q_{\text{heat}}$ is other heat sources such as nuclear heating or heat leaks from adjacent structure.

**Simple Scaling**

For a given conductor, the total power $P$ given by Eqn.(1) varies considerably with operating temperature. This operating temperature range governs the selection of coolants and their states. Water (room temperature), liquid nitrogen (60-75 K), liquid helium (below 4.2 K), and gaseous helium (over 10 K) cover the various temperature regimes of interest. Also, below 100 K, the conductors become very sensitive to variations of temperature, magnetic field strength, and material purity.

A simple scaling study serves to demonstrate the advantage of cryogenic magnets. If we neglect pumping power and non-resistive heat sources, and assume constant properties and that the conductor is at the same temperature as the coolant, then Eqn.(1) becomes

$$P = \rho_{\text{eff}} j^2$$

$$\rho_{\text{eff}} = \frac{1}{\sigma(T_c, B, \text{RRR})} \left[ 1 + \frac{1}{\eta_{\text{refr}}} \left( \frac{T_h}{T_c} - 1 \right) \right]$$

where $\rho_{\text{eff}}$ is the effective "resistivity" of the magnet, including refrigeration power, and $T_c$ is the average magnet temperature. Since, $A_m$, $L$ and $j$ are fixed for a given magnet, $\rho_{\text{eff}}$ is a measure of the power consumption. Figure 1 plots $\rho_{\text{eff}}$ as a function of temperature for copper (RRR = 300) and aluminum (RRR = 2000) at a magnetic field strength of 6 Tesla. These figures show that steady-state power consumption initially increases as the temperature increases because

1.37
resistivity changes slowly but the refrigeration requirements are appreciable. However at some point, resistivity drops sharply and overall power savings of factors of three or so are possible, compared to room-temperature operation. At even lower temperatures, refrigeration power makes cryogenic operation unreasonable again for these conductors. (Superconductors can operate successfully in the 4 K temperature range however, but are not considered here.) Though simple-minded, \( \rho_{eff} \) demonstrates the possible benefits of operating magnets at cryogenic temperatures. This also demonstrates the disadvantage of steady-state magnets operating at liquid nitrogen temperature.

Compressible Flow Equations

The analysis is restricted to single-phase, one-dimensional, constant area, steady-state flow. The large property variations at cryogenic temperatures with modest temperature changes (especially for gaseous coolants) requires the use of compressible flow equations to properly determine coolant pressure drop, coolant temperature, conductor temperature, conductor resistivity, and finally magnet electrical power requirement. The resulting equations do not lend themselves to analytic solution, but under the above assumptions with the additional neglect of axial thermal conduction with respect to radial thermal conduction, a simple marching procedure is adequate with straightforward differencing at each axial zone into coolant, conductor surface and conductor bulk nodes. Care must be taken to avoid choked flow, possible flow instabilities [7], critical heat flux with liquid coolants, and thermal runaway in the temperature range where resistivity rapidly increases.

In addition to the above assumptions, we neglect azimuthal property variations and consider only an average axial velocity, \( v \). Then, for the coolant, conservation of mass becomes

\[
\frac{d}{dx}(\rho v) = \frac{dG}{dx} = 0 \tag{4}
\]

where \( G = \rho v \) is the mass flux, \( \rho \) the coolant density. The momentum equation is

\[
pvdv + dp + \frac{4\tau_w dx}{D} + \rho gdz = 0 \tag{5}
\]

where \( p \) is pressure, \( \tau_w \) is wall shear, \( x \) is the axial coordinate, \( D \) the hydraulic diameter, \( g \) gravitational acceleration and \( \theta \) is the conductor orientation angle clockwise from vertical. Expressing the wall shear in terms of a conventional friction factor \( f \),

\[
\frac{dp}{dx} = -\frac{f G^2}{2D} - \rho g \cos \theta + \frac{G^2}{\rho^2} \frac{dp}{dx} \tag{6}
\]

Finally, the energy equation is

\[
\rho v (dh + vdv) - \frac{4q'' dx}{D} + \rho gdz = 0 \tag{7}
\]
where $h$ is enthalpy and $q''$ is the heat flux from conductor to coolant.

The conductor equations are obtained from the cylindrical heat conduction equation

$$\frac{1}{r} \frac{\partial}{\partial r} \left( r \frac{\partial T}{\partial r} \right) = -\frac{q'''}{k}$$

where $r$ is radius, $T$ is temperature, $q'''$ is volumetric heat source and $k$ is thermal conductivity.

Applying an adiabatic outer boundary condition, we obtain

$$\Delta T = T_o - T_i = \frac{q'''}{4\pi k} \left[ (A_m + A_c) \ln \left( \frac{A_c + A_m}{A_c} \right) - A_m \right]$$

where $\Delta T$ is the temperature rise from the inner cooled surface to the outer surface, and the result has been expressed in terms of coolant cross-sectional area $A_c$ and magnet area $A_m$.

We wish to determine $p(x)$ and $T(x)$. Other variables such as $\rho$ and $h$ are thus, in general, functions of $p$ and $T$. So

$$\frac{dp}{dx} = \rho \left[ \frac{dp}{dT} \right] _{T, \rho} + \frac{\partial p}{\partial T} \frac{dT}{dx}$$

and similarly for $(dh/dx)$. Making these expansions, substituting into the momentum and energy equations, and making use of the definitions of compressibility $K$, thermal expansivity $\beta$, specific heat at constant pressure $c_p$, and Joule-Thompson coefficient $\psi$, we finally obtain

$$\frac{dT}{dx} = \frac{1}{C} \left[ 4q'' + K \frac{G^2}{C} \left( \frac{4q'' + f \frac{G^2}{D} \frac{G^2}{D^2 \rho^2}}{D^2 \rho^2} \right) - \psi \frac{f \frac{G^2}{D^2 \rho^2} + g \cos \theta}{c_p} \right]$$

$$\frac{dp}{dx} = \frac{1}{C} \left[ -\frac{\beta G^2}{c_p} \left( \frac{4q'' + f \frac{G^2}{D} \frac{G^2}{D^2 \rho^2}}{D^2 \rho^2} \right) - \rho \left( \frac{f \frac{G^2}{D^2 \rho^2} + g \cos \theta}{c_p} \right) \right]$$

where

$$C = 1 + \frac{\beta G^2}{c_p} \left[ \frac{1}{\rho} + \psi c_p - \frac{K c_p}{\beta} \right]$$

Using standard correlations for friction factor $f$ and heat transfer coefficient, and property data, we can solve for $p(x), T(x)$ and all the desired quantities.

### 1.4.3 Applications

The selection of a magnet design is based on many criteria. Therefore, the true merits of cryogenic magnets must be judged on the basis of individual cases. The following section present two specific magnet designs: TEXTOR Bundle Divertor coils and AFTR (Advanced Fusion Tokamak Reactor) TF coils. Copper and aluminum are chosen as possible conductors, with water, liquid nitrogen, and gaseous helium as coolants.
Textor Bundle Divertor Design

In October 1977, an IEA-Implement Agreement set out a program of research and development on plasma-wall interaction in TEXTOR, a medium-sized tokamak at Julich. The major experimental parameters are 175 cm major radius, 50 cm minor radius, and an on-axis field of 2.0 T. One of the main objectives of the program is to test the effectiveness of bundle divertors in impurity control and helium ash removal. Studies at MIT [14] have produced an initial design of the bundle divertor using the 3-T cascade configuration (see Table 1).

We can view the T coil as combination of two L-shaped coils. Taking a cross section of a L-shaped coil we can divide the coil into many unit cells. CCAN calculates total power requirement as we vary parameters governing conductor geometry and condition, coolant flow rate and states, and magnetic environments. For the TEXTOR bundle divertor design, we determine the location of peak magnetic field strength (in the third coil) and (conservatively) use the peak field as the uniform field experienced by the entire divertor coil. We also expect little heat entering the magnet since TEXTOR uses hydrogen plasma and shielding is expected to prevent heat leakage into the conductors. This set-up will give us a worst case optimization of $P$. The results are summarized in Table 2.

Taking the room temperature, water-cooled copper magnet as our reference case we see that the total power requirement is dominated by the electrical power. Therefore the resistivity determines $P$, and since the effects of magnetoresistivity and RRR are negligibly small, the temperature variation is the primary factor. Using water with inlet temperature ($T_{in}$) of 288 K, the optimized $P$ now depends on the fluid flow rate and conductor geometry. The results indicate that the magnet prefers smaller conductor cross section ($A_{in}$) and high mass flow rate ($w$). Lower limit exists for $A_{in}$ based on limiting fabrication capabilities. An upper limit for $w$ exists based on pumping power requirements and maximum flow rate before flow instabilities and cavitation begin.

Although aluminum has superior properties (particularly low mass density and neutron activation cross section), we did not consider water-cooled aluminum magnets because of the higher room-temperature electrical resistivity.

These results also demonstrate the high electrical power requirement at liquid nitrogen temperatures. Cases studied indicate a factor of three times higher $P$ than the reference room temperature case because a modest improvement in resistivity is more than offset by the refrigeration power.

At 10 to 50 K, gaseous helium cooled aluminum and copper conductors were analyzed. Conductor purity was selected on the basis of commercial availability and cost (Cu RRR = 300, Al RRR = 2000). These are high purity but achievable values. At these conditions, we found that pumping power becomes an important consideration in the optimization process. For this study
we used coolant inlet temperature, and coolant flow rate as optimization variables. As Table 3 shows, the power requirement of copper magnets in this temperature range is comparable to the base case result. Our optimization study found appreciable power savings with cryogenic aluminum magnets as compared to the reference case – a power reduction as high as a factor of three was observed.

A disadvantage of using high purity aluminum conductor is its weak strength, even at low temperatures. Possible solutions to this problem are to use aluminum alloys or fiber-reinforced conductors [15]. The former will increase resistivity and eliminate the advantage of cryogenic design. The latter has been suggested in applications to superconductors. Calculations have shown that approximately 10% of total magnet volume is needed for structural purpose. This should still leave enough conductor volume to demonstrate the superiority of cryogenic gas-cooled aluminum magnets.

**AFTR TF Coil Design**

A recent scoping study [16] utilizes the advantages of high-performance resistive magnets in the design of Advanced Fusion Tokamak Reactor (AFTR). The goals of the AFTR design include DT ignition with large physics margins; high duty cycle, long pulsed operation; and DD-DT operation with low tritium concentration. AFTR has a 3.5 m major radius, 0.9 m minor radius and an on-axis field of 7.3 T.

In analyzing the thermal-hydraulics of the AFTR magnets we simplify the geometry. We first divide the sector of the AFTR Bitter-type coil into an inner and outer section. Each section is further approximated by vertical columns of unit cells, each cooling the same volume of conductor. The coolant enters from bottom and drains in one passing. For simplicity we set all unit cells to equal dimensions. Due to the toroidal geometry the inner and outer sections operate at different conditions. The inner cells must carry higher current density while operating at higher field strength. Table 3 lists the geometry and conditions used for the design of the AFTR TF coils.

A major difference in the AFTR design as compared with the TEXTOR Bundle Divertor design is the heat source from the blanket due to insufficient heat shielding. The AFTR design estimates an average of 5.5 kW/m³ internal neutron heating. This heat source is insignificant at room temperature, but at cryogenic temperatures any additional heat source requires large amounts of refrigeration power.

Once again, the room-temperature, water-cooled copper magnets is used as the reference case. The analysis here parallels the TEXTOR Bundle Divertor design. The optimized study estimated total power requirement to be 390 MW. The preliminary study also demonstrated the disadvantage of using liquid nitrogen as coolant. Comparing with our reference case, a liquid nitrogen-cooled
resistive magnet requires approximately two times more power than the water-cooled magnet at room-temperature.

At the cryogenic gas-cooled temperature regime, we see the impact of nuclear heating on the power requirement. Under the same operating condition, the effect of nuclear heating will increase the power requirement by over a factor of two. The optimized design requires a minimum of 230 MW to cool cryogenic aluminum magnets. Copper magnets require even higher power. With relatively little power saving, there is no advantage to cryogenic gas-cooled magnets in the AFTR design. However, if nuclear heating can be drastically reduced by increased shielding, we can again consider the cryogenic gas-cooled concept.

Conclusions

In this study, we report some preliminary results on the advantages of cryogenically-cooled magnets, as compared with superconducting and room-temperature water cooled designs. Simple scaling studies indicate that factors of three to ten improvements can be made, based on an optimization between resistive power (which decreases with decreasing temperature), and refrigeration power (which increases with decreasing temperature). More detailed calculations for two specific applications, the TEXTOR bundle divertor and the AFTR TF coils, show that realistic factors of two reduction in power requirements over room-temperature water-cooled designs are possible. This level of improvement assumes high purity aluminum and the elimination of external heat sources. Thus, helium gas cooling of the TEXTOR is reasonable at temperatures of around 25 K, while the AFTR coils have too much internal neutron heating for cryogenic cooling to be useful. At present, the thermal-hydraulic codes are being upgraded and better property correlations developed. A more comprehensive analysis of cryogenic cooling for fusion reactor applications will then be completed.

REFERENCES


Table 1

TEXTOR Bundle Divertor Design Parameters

<table>
<thead>
<tr>
<th>Coil</th>
<th>Height (cm)</th>
<th>Width (cm)</th>
<th>Length (cm)</th>
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<tr>
<td>1</td>
<td>35</td>
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<td>2</td>
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<td>60</td>
<td>15</td>
</tr>
<tr>
<td>3</td>
<td>75</td>
<td>60</td>
<td>15</td>
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Current per coil = 0.45 MAmps
Maximum field = 6.0 T

Table 2

TEXTOR Bundle Divertor Thermal-Hydraulic Design Results

<table>
<thead>
<tr>
<th>Coolant</th>
<th>Conductor/RRR</th>
<th>$p_{in}$ (MPa)</th>
<th>$T_{in}$ (K)</th>
<th>$w$ (kg/s)</th>
<th>$P_{elec}$ (MW)</th>
<th>$P_{pump}$ (MW)</th>
<th>$P_{total}$ (MW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Water</td>
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<td>300</td>
<td>60.</td>
<td>6.44</td>
<td>0.069</td>
<td>6.5</td>
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<td>0.024</td>
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<td>30</td>
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<td>0.001</td>
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</table>
### Table 3

AFTF TF Coil Thermal-Hydraulic Design Parameters

<table>
<thead>
<tr>
<th>Length (m) of Unit Cell</th>
<th>Number of Channels</th>
<th>Average Current Density (kA/cm²)</th>
<th>Average B (T)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inner Channels</td>
<td>6.9</td>
<td>$7.64 \times 10^4$</td>
<td>1.48</td>
</tr>
<tr>
<td>Outer Channels</td>
<td>6.9</td>
<td>$2.61 \times 10^5$</td>
<td>0.43</td>
</tr>
</tbody>
</table>

$\eta_{refr} = 25\%$   $\eta_{pump} = 70\%$   $A_{mag} = 1.1\text{cm}^2$   $A_{coolant} = 0.11\text{cm}^2$

### Table 4

AFTF TF Coil Thermal-Hydraulic Design Results

<table>
<thead>
<tr>
<th>Coolant</th>
<th>Conductor/RRR</th>
<th>$p_{in}$ (MPa)</th>
<th>$T_{in}$ (k)</th>
<th>$w$ (kg/s)</th>
<th>$Q_{heat}$ (kW/m³)</th>
<th>$P_{elec}$ (MW)</th>
<th>$P_{pump}$ (MW)</th>
<th>$P_{total}$ (MW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Water</td>
<td>Cu/300</td>
<td>4.0</td>
<td>300</td>
<td>26300</td>
<td>5.5</td>
<td>363.</td>
<td>27</td>
<td>390.</td>
</tr>
<tr>
<td>Nitrogen</td>
<td>Cu/300</td>
<td>4.0</td>
<td>65</td>
<td>17500.</td>
<td>5.5</td>
<td>41.</td>
<td>10</td>
<td>785.</td>
</tr>
<tr>
<td>Nitrogen</td>
<td>Al/2000</td>
<td>4.0</td>
<td>65</td>
<td>17500.</td>
<td>5.5</td>
<td>27.7</td>
<td>10</td>
<td>590.</td>
</tr>
<tr>
<td>Helium</td>
<td>Cu/300</td>
<td>2.0</td>
<td>30</td>
<td>440.</td>
<td>5.5</td>
<td>9.7</td>
<td>0.15</td>
<td>350.</td>
</tr>
<tr>
<td>Helium</td>
<td>Al/2000</td>
<td>2.0</td>
<td>15</td>
<td>440.</td>
<td>5.5</td>
<td>1.27</td>
<td>0.065</td>
<td>230.</td>
</tr>
<tr>
<td>Helium</td>
<td>Al/2000</td>
<td>2.0</td>
<td>15</td>
<td>440.</td>
<td>0</td>
<td>1.25</td>
<td>0.062</td>
<td>97.</td>
</tr>
</tbody>
</table>
Figure 1. Effective Resistivity (Including Refrigeration Power) of Copper (RRR=300) and Aluminum (RRR=2000) at 6 Tesla.
2.1 DIVERTOR SHIELDING ASSESSMENT

2.1.1 Introduction

The coil component in front of and around the front legs of the divertor coils suffers the most critical radiation damage. It is the purpose of this work to search for an optimum combination of shielding materials for best protection of the divertor coils in the minimum possible space.

The radiation damage characteristics and the radiation exposure limit for the superconducting and normal magnets have been discussed in detail in Refs. 2 and 3. For a superconducting magnet, the magnet insulation, stabilizer, and superconductors suffer severe radiation damage characterized by mechanical and electrical property degradation of the insulation due to radiation dose, resistivity increase of the stabilizer due to atomic displacement, and critical temperature and current density changes in the superconductor due to neutron transmutation and atom displacement. The superconductor damage is very small in the currently considered radiation environment. The resistivity in the stabilizer may be partly recovered if the magnet is annealed. Hence, the most critical damage is the insulation dose damage which is unrecoverable. For a normal magnet, radiation damage is characterized by the insulation property changes due to dose degradation and resistance increase in the conductor due to atomic displacement. DPA damage may be annealed out and it appears that the most critical damage is again the insulation dose damage.

2.1.2 Design Considerations for Superconducting Divertor Components

The operation of a superconducting (SC) magnet requires maintaining the superconducting state – any external disturbance which causes a large section of superconductor to undergo the phase transition into the normal state will render the coil inoperable. Possible forms of disturbances include heating, radiation damage, mechanical impact or frictional heating and magnetic field pulses. Large SC magnets are almost exclusively designed to be cryostable, i.e., they are designed to allow a limited section of conductor to be driven into a transient normal state and to be able to recover. This function is achieved by the normal stabilizer which provides a temporary current flow path. The amount of stabilizer must be sufficient so that the ohmic heating in the normal region does not exceed the available surface dissipation rate, which is determined by the heat transfer to the liquid helium (LHe) coolant. The ohmic heating rate is determined directly by the electrical resistivity of the stabilizer, which will be increased by irradiation.
Material Properties Related Considerations

The issues include the effects of irradiation on the superconductor, stabilizer, and insulator.

1. Irradiation Effect on Superconductors – NbTi and Nb₃Sn are the two most commonly used superconductors. Figure 1 shows the typical effect of irradiation on the critical current densities ($J_c$) for these two materials [4]. It can be seen that NbTi is much more tolerant to irradiation. However, it is expected that the deterioration in critical current density will accelerate for doses higher than $10^{20}$ n/cm². For the case of Nb₃Sn, the deterioration of $J_c$ is very rapid for a dose beyond $3 \times 10^{18}$ n/cm².

2. Irradiation Effect on Stabilizer – Figure 2 shows the radiation induced resistivity increase $\rho_D$ of Cu and Al [5], which are the most commonly used stabilizer materials. It can be seen that Cu is more tolerant to radiation than Al. It is expected that $\rho_D$ for both Cu and Al are steadily increasing with dosage.

3. Irradiation Effects on Insulators – Although the operating voltages are usually low ($\leq 50$ volts) for large DC or slowly pulsed SC magnets, during coil quenches, higher voltages (up to $10^3$ volts) may be generated depending on the design of the protection circuitry. It is important to prevent excessive degradation in the insulation, otherwise electrical breakdown may lead to arcing and local melting. A related subject is the mechanical strength of the insulation material. It may be necessary for the insulation to withstand a bearing stress of up to 10,000 psi in a high field, high energy density magnet. Unfortunately, the most commonly used insulators, (G-10, mylar, kapton), are not well characterized for their behavior under irradiation at low temperature [6]. Figure 3 shows the degradation of the compressive strength of these materials due to neutron irradiation. Probably $10^7$ Gy ($10^9$ rads, $\approx 10^{19}$ n/cm²) is the upper limit of the dose for these organic insulators. Recent irradiation tests [7] indicate that G-10 epoxy/E-glass insulation is capable of $10^9$ Gy ($10^{11}$ rads) with good mechanical property retention, although it remains to be confirmed.

Considerations Relating to Refrigeration

The operation of SC magnets requires maintaining a low temperature ($\leq 6$ K), so that the superconductor can carry the required current at the operating ambient field strength without going into the normal state. This requires cooling of the entire winding and extraction of the nuclear heat generated. There may be systems-related questions concerning the overall power balance of the reactor.
1. Cooling of Conductor – Typically, a large SC magnet is designed so that the winding can tolerate the introduction of external disturbances with energy content below a certain limit. For example, the ETF SC TF coils are designed to be stable against disturbances up to 0.10 J/cm³ in the conductor. A disturbance of such a magnitude is usually sufficient to adiabatically raise the temperature of the conductor to about 25 K, which is far higher than the transition temperature of the conductor. Thus, if the irradiation flux is high enough to introduce heating into the winding near 0.1 W/cm³, it may be very difficult to keep the coil in operation, because it would require pumping LHe at a very high flow rate to extract the heat generated. Another limit of volumetric heating rate is the corresponding surface heat flux over the conductor surface exposed to the LHe coolant. In a pool-boiling arrangement, the heat flux limit is about 0.5 W/cm². In a forced flow arrangement, the tolerable heat flux may be higher, but it is doubtful the value can exceed 2 W/cm². Furthermore, in a forced flow cooled magnet, the length of the cooling channel may have to be very short so that the downstream coolant temperature will not be excessively high.

2. Refrigeration Power – Assuming that the coil winding can be adequately cooled by proper arrangement of coolant flow, there is the question of the required refrigeration power. Since it takes more than 350 watts of refrigeration power at room temperature for the removal of each watt of power generated at 4 K, overall power balance and economics may preclude using SC magnets if there is too much nuclear heating.

2.1.3 Shielding Analysis

Various shielding combinations of shielding materials, such as 316SS + B₄C and W + B₄C, have been studied for regular fusion reactor shield designs [8]. Tungsten appears to be the best shielding material. Recently, shield combinations of tungsten with advanced materials such as TiH₂ and ZrH₂ are proposed for the Engineering Test Facility divertor shielding design [9]. We report here a preliminary shielding requirement study performed using one and two dimensional models and tungsten with borated water as the shield material. More detailed and particularly three dimensional calculations are needed in later design phase studies. Several neutronics calculations to estimate the shielding thickness needed for the normal coil divertor design were performed. The 1-D and 2-D discrete ordinates transport codes ANISN [10] and DOT [11] were employed with P₃S₆ and P₃S₄ approximations, respectively, in cylindrical geometry [7].
One-Dimensional Analysis

The one dimensional shield model consists of a 20 mm stainless steel first wall, with 50% of the space filled with water for cooling, a variable thickness of shield, and a 0.4 m 40% SS + 60% Cu superconducting magnet copper and SS structure zone representing the superconducting magnet. Three combinations of shielding materials are considered: 10% H₂O (B) + 90% W; 30% H₂O (B) + 70% W; and 50% H₂O (B) + 50% W. The borated water is employed both as neutron absorber and coolant. A density factor of 0.9 is used for tungsten to account for the packing effect. The calculational results of the radiation dosage on the insulation material as a function of shield thickness for the above three shielding material combinations are shown in Fig. 4.

Figure 4 shows that the 10% H₂O (B) + 90% W shield is the best material combination. The radiation dose on the insulation material can be expressed as

\[ D(t) = D_o \exp(-14.01t - 3.33t^2) \]

where \( D_o \), which is \( 2.5 \times 10^{11} \) Gy/yr at 1 MW/m² wall loading, is the dose on the insulator if there were no shield between the first wall and divertor coils, and \( t \) is the shield thickness in meters. Note that the dose attenuation coefficient is not linear. It depends somewhat on the effective neutron energy which is the result of neutron moderation in the shield. Similar expressions for the neutron flux, atomic displacement rate on copper, and nuclear heating at the magnet position close to the plasma were obtained.

Neutron flux (n/cm² year at 1MW/m²): \( F(t) = 1.13 \times 10^{22}e^{-0.158t - 0.0464t^2} \).

Atomic displacement rate (dpa/year): \( DPA(t) = 10.0e^{-0.1463t - 2.718 \times 10^{-4}t^2} \).

Nuclear heating (W/cm³): \( H(t) = 10.5e^{0.1666t + 1.163 \times 10^{-4}t^2} \).

The neutron flux and nuclear heating distributions in the superconducting magnet are further displayed in Fig. 6 and 7 as a function of shield (10% H₂O (B) + 90% W) thickness.

The lifetime of the insulation material is depicted in Fig. 8 as a function of dose limit and shield thickness. Since the divertor can be designed as a plug-in unit, a one year replacement schedule should be reasonable. Considering a lifetime of 1 MW-yr/m², the minimum shield thicknesses required would be 0.63 and 0.37 m, respectively, if the dose limits on the insulator are \( 10^7 \) and \( 10^9 \) Gy, depending on type and form of the insulator.
Two-Dimensional Analysis

A bundle divertor conceptual design was used for the 2-D analyses. Three 2-D calculations (DOT, P₃S₄ runs) were performed and a representative design is shown in Fig. 9:

**Design 1:** The tungsten + borated water shield is 0.6 m thick. Its height is 0.5 m, the same as that of the superconducting magnet.

**Design 2:** The thickness of the shield is kept at 0.6 m; however, the height of the shield is increased to 0.7 m to give more attenuation at the magnet.

**Design 3:** As design 2, except that the shield thickness is increased to 0.7 m.

Note that in the 2-D models, the plasma major and minor radii are 5 m and 1 m respectively. The plasma chamber radius is 1.25 m. The tungsten shield and divertor magnet layers are sitting in a reactor configuration which consists of a 0.7 m Li₂O blanket and a 0.2 m 316SS reflector. The Li₂O blanket is a typical helium cooled tritium breeding blanket for a power reactor, and is composed of 72.25% Li₂O (80% dense) 8.4% SS and balance of helium, all by volume.

Figure 10 shows the maximum radiation dose rates on the epoxy-based insulator across the superconducting magnet above midplane. Some observations can be made from this figure.

1. The scattered neutron effect, due to the small attenuation coefficient of the Li₂O blanket, is significant in that it appreciably increases the radiation dose rate on the epoxy-based insulator.

2. Additional 0.1 m high tungsten shield reduces the radiation dose rate by about a factor of 20 at the edge of the magnet (where it always shows the highest dosage).

3. The radiation dose rate at midplane obtained for a 0.6 m thick and 0.7 m high tungsten shield (design 2) is consistent with that obtained from a one dimensional calculation. (Both are about \(2 \times 10^7\) Gy/year at 1 MW/m² wall loading.)

4. If the shield increases from 0.6 m to 0.7 m, the radiation dose rate on the epoxy-based insulator at midplane would decrease by about a factor of 5. In other words, it takes about 0.14 m shield to attenuate the radiation dose by one decade.

2.1.4 Conclusions and Recommendations

One- and two-dimensional radiation shielding analyses show that for a divertor lifetime of 1 MW-yr/m² the required shield thickness is at least 0.6 m if the dose limit on the G-10 type organic insulator is \(10^7\) Gy. It is 0.4 m, however, if the dose limit can go up to \(10^9\) Gy.
REFERENCES


Fig. 1: Effect of neutron irradiation on the critical current in NbTi and Nb₃Sn.

Fig. 2: \( \rho_p \), radiation-induced resistivity increase for Cu and Al versus fast-neutron fluence \( (E > 0.1 \text{ MeV}) \). The irradiation temperature was 4.9 K.
Fig. 3: Compressive strength of several organic insulators under irradiation.

\[ L = 0.24 \text{ ft}, \quad H = 1.0 \times 10^{10} \text{ rad}(\gamma) + 8.7 \times 10^{-10} \text{ rad} < 0.1 \text{ MeV} \]

IRRADIATION AT 4.9K – TEST AT 78K AFTER WARMUP TO 307K.

Fig. 4: Dose rate as a function of shield thickness for three shielding material combinations.
Fig. 5: Dose rate on the insulation material for the shielding material 10% H₂O(B) + 90% W as a function of shield thickness and at depth into the superconducting magnet.

Fig. 6: Neutron flux distribution in the superconducting magnet as a function of shield thickness. The shielding material used here is 10% H₂O(B) + 90% W.
Nuclear heating distribution in the superconducting magnet as a function of shield thickness. The shielding material used here is 10% H$_2$O(B) + 90% W.

Fig. 8: Lifetime of insulation material as a function of dose limit and shield thickness.
Fig. 9: Schematic of the two-dimensional shield model - design 2. (Shield thickness = 0.6 m; height = 0.7 m)

Fig. 10: Maximum radiation dose on epoxy insulators (average neutron wall loading 1 MW/m²). (1 Rad = 10⁻² Gy)
3.0 DIVERTOR PHYSICS

3.1 PLASMA/NEUTRAL GAS TRANSPORT IN DIVERTORS

3.1.1 Introduction

Steady-state operation of a fusion reactor requires a method to remove impurities and helium ash from the main reactor chamber. Furthermore, it is desirable to protect the first wall from energetic plasma particles, and to shield the plasma from wall-generated impurities. The method must also be able to handle the high particle and energy fluxes that accompany any interaction of the reactor with the plasma. The solutions to this "exhaust" problem are generally classed as either magnetic divertors or mechanical divertors, depending upon the procedure used to collect the plasma into the processing region. For tokamaks and similar toroidal machines, the primary candidates are the bundle divertor, the poloidal divertor and the pumped limiter.

The physics of the "scrape-off" region between the confined plasma volume and the divertor, as well as the divertor region itself is not yet well understood. Besides the usual complications of plasma particles in a magnetic field, this is a region of strong gradients in density and temperature between the 100,000,000 K plasma and the 1000 K reactor vessel. Sputtering, arcing and degassing from the vessel plus the refluxing helium, deuterium and tritium produce a large amount of neutral or partially charged particles.

In steady-state, the hot plasma particles will travel through this region and deposit energy and momentum on the first wall and divertor neutralizer target. Simple scaling calculations show that the resultant particle and energy flux can deposit 1 kW/m² and erode any known material at rates of mm/year to cm/year. The engineering design of the divertor region subject to this kind of erosion and heat flux is very difficult. Table 1 reviews high heat flux experience. While it is clear that comparable fluxes have been handled successfully for short periods or over small areas, there is as yet little cumulated experience to assure system reliability and even if the heat flux can be handled, the high erosion implies both short life for the target, as well as a large source of high atomic number impurities that can find their way back to the main plasma and quench it.

Several innovative approaches have been proposed to alleviate the engineering problems, such as liquid or gaseous targets. These reduce the heat flux and erosion rate as limiting factors, but increase the demand on the pumping system associated with the divertor.

However, at this point there remain some fundamental physics questions that bear directly on the engineering design. For example, what is the interaction between the incoming plasma ions and any refluxing impurities or hydrogen? There is experimental evidence that the plasma can
effectively plug the divertor throat as it streams in at high velocity. This automatically reduces the number of divertor-generated impurities returning to the plasma. Furthermore, it increases the neutral gas density in the divertor and makes the vacuum pumps more effective. If the neutral gas density is large enough, the incoming plasma could be relaxed in energy, with the actual flux to the divertor chamber and target reduced to radiated energy and particles with energies below the sputtering threshold of around a hundred electron volts.

The purpose of the present study is to treat the plasma particle and neutral gas interaction that occur in divertor and limiter ducts in sufficient detail to answer some of the questions as to the actual heat flux, erosion rates and pumping that will be expected in fusion reactors.

3.1.2 Plasma/Neutral Gas Interactions

The dominant processes of interest are the collisions occurring in the divertor chamber among all the species present. In this analysis, we will neglect impurity transport and concentrate on solving hydrogen transport self-consistently since this will provide the background conditions for the much small impurity and helium concentrations. We will also not distinguish between deuterium or tritium, but rather use a representative hydrogenic atom with averaged properties. Most data used in this section is obtained from protium data, but will be sufficiently accurate for present purposes since the collision processes of interest here are independent of isotope within the general accuracy of the data if cross-sections are compared on the basis of interaction velocity. We will also anticipate that the average electron and ion energies will be comparable so that the ion velocity can be neglected with respect to the electron in electron impact cross-sections.

The primary species are $\text{H}^+$ and $\text{H}$ from the main plasma and scrape-off layer, and $\text{H}, \text{H}^+, \text{H}_2^+$ and $\text{H}_2$ returning from the divertor target or neutralizer plate, and the accompanying electrons. A survey of the literature identified the reactions and corresponding cross-sections shown in Figures 1 to 6.

In order to simplify calculations, only the dominant cross-sections are included in the analysis. The likely energy range for the different species is 10 eV to 10 keV, the former assuming a high neutral density scrape-off layer and the latter representing particles that escape from the reactor core directly into the duct. Considering the ion-ion interactions (Figure 1 and 2), we neglect, for example, $\text{H}^+ + \text{H}$ going to $2\text{H}^+$ when compared to the corresponding resonant charge exchange reaction. The former only dominates at tens of keV energies. Similarly, we note that the reactions which produce $\text{H}^-$, $\text{H}(2s)$ or $\text{H}_3^+$ are less likely than reactions which produce $\text{H}, \text{H}^+, \text{H}_2$ and $\text{H}_2^+$, so we can neglect these particles. The biggest exception to this latter rule is the reaction $\text{H}_2^+ + \text{H}_2$ producing $\text{H}_3^+$ which, while the data was not entirely consistent, dominated over the corresponding charge exchange reaction at low energies (less than about 1 eV). However, this is
not expected to affect the overall results. Finally, as Figure 5 and 6 show, the reactions which convert the additional species to the more conventional ones have large cross-sections - often an order-of-magnitude larger than the corresponding conventional reactions. Thus H\(^-\), H(2s) and H\(_3^+\) are less likely to be produced, and more likely to be consumed, justifying the neglect of these particles as major contributors.

With these kinds of considerations, we narrow down the interactions to (for 10 eV < \(T_i, T_e\) < 10 keV):

1) e + H\(_2\) = H\(_2^+\) + 2e electron impact ionization
2) e + H\(_2\) = 2 H + e electron impact dissociation
3) e + H\(_2\) = H + H\(^+\) + 2e electron impact dissociative ionization
4) e + H\(_2^+\) = 2 H electron impact dissociative recombination
5) e + H\(_2^+\) = H + H\(^+\) + e electron impact dissociation
6) e + H = H\(^+\) + 2e electron impact ionization
7) H\(_2^+\) + H\(_2\) = H\(_2\) + H\(_2^+\) resonant charge exchange
8) H\(^+\) + H = H + H\(^+\) resonant charge exchange

The charge exchange processes are resonant so involve little energy loss or gain. Ionization is generally endothermic, while dissociation is generally exothermic (the cross-section shape gives a reasonable idea as to the energies involved). For the reactions above, these energies are typically 10 eV or less. At present, both energy changes and momentum transfer because of atomic processes is neglected in subsequent analysis.

In order to perform fast calculations, functional fits to the cross-sections have been developed. These correlations generally fit the data to within the variation in reported values.

### 3.1.3 Analysis Procedure

To more fully characterize the interactions between the plasma particles, the neutral gas and the self-consistent electric field that develops, a kinetic treatment of the transport has been developed.

The most general solution starts with the Boltzmann equation for each species

\[
\frac{df}{dt} = \frac{\partial f}{\partial t} + \mathbf{v} \cdot \nabla f + \frac{F \partial f}{m \partial \mathbf{v}} + \left( \frac{\partial f}{\partial t} \right)_e
\]

The problem is made more tractable by considering the conditions in the divertor regions we wish to analyze, i.e. the one-dimensional geometry of magnetic divertor or pumped limiter ducts where the primary velocity and magnetic field components are along the duct axis.

In practice, a 1-D model is only a first approximation and it is worthwhile considering how well it can describe the particle transport. In bundle divertors, the magnetic field runs down the
duct and the particles tend to acquire large parallel velocities because of the expansion of the magnetic field from the divertor throat to the target. This is accompanied by an expansion of the chamber area which is not modelled in this 1-D analysis. In poloidal divertors, the magnetic field is dominated by the toroidal component (in tokamaks) which carries particles along the duct and into a shallow grazing incidence on the target. Neutral gas particles can easily move perpendicular to the field lines and wander vertically back into the main plasma, unless the vertical geometry has a throat. Thus the gas particles may not be well-described by a 1-D analysis. In pumped limiters, the magnetic field is primarily toroidal in the duct itself, and the gas motion is largely ballistic collisions with the duct walls as the particles meander down to the vacuum pumps. Thus, while the plasma particles may be described by a large parallel velocity and gyromotion, the neutral gas particle motion is not as easy to describe. In all these cases, the perpendicular drift motion of the neutral particles may be significant. However, within the accuracy of the final result, we anticipate that strong neutral/plasma interactions will tend to give neutral particles large parallel velocities, or that neutral gas motion down the duct can be described in terms of an effective or projected velocity along the duct axis.

For this 1-D geometry, it is more convenient to use the drift-kinetic equation. This is obtained (following Ref.[1]) by replacing the usual distribution function $f(t, x, v)$ with the guiding center distribution function $f(t, x, W, \mu, v_d)$, where $W$ is the particle energy, $\mu$ is the adiabatic invariant, $\phi$ is an electrostatic potential, and $v_d$ is a drift velocity (neither parallel motion nor gyromotion). Then

$$\frac{df}{dt} = \frac{\partial f}{\partial t} + \frac{dx}{dt} \cdot \nabla f + \frac{dW}{dt} \frac{\partial f}{\partial W} + \frac{d\mu}{dt} \frac{\partial f}{\partial \mu} + \frac{dv_d}{dt} \cdot \nabla_{v_d} \frac{\partial f}{\partial t}$$

(2)

Averaging over a gyroperiod, assuming steady-state, 1-D, and $\mu$ constant, we can reduce Eqn. (2) to the trajectory equation,

$$v_{\parallel} \frac{\partial f}{\partial x} = \left( \frac{\partial f}{\partial t} \right)_c$$

(3)

where it should be noted that, since $f = f(x, W, \mu)$, $v_{\parallel} = v_{\parallel}(x, W, \mu)$ is defined along a line of constant energy $W$, for constant $B$ or small $\mu$.

The collision operator can be expanded in several ways. We are interested in reactions such as discussed earlier, which involve little energy or momentum transfer. Self-collisions among neutral particles can be neglected at low densities. Ion self-collision time, nominally by multiple small-angle scattering through the electric field, will be neglected compared to the transit time for the ions in the duct. Electrons, however, will be assumed highly collisional along the field line because of their light mass. Experimental results actually imply that electrons are anomalously collisional along field lines.

3.4
Thus, for neutrals and ions, \( \left( \frac{\partial f}{\partial t} \right)_c \) is only related to binary collision processes, or

\[
\left( \frac{\partial f_i}{\partial t} \right)_c = \sum_j f_i(v_i, x) \int_{v_j} \sigma_{ij}(|v_i - v_j|) |v_i - v_j| f_j(v_j, x) \, dv_j
\] (4)

Under the assumption of highly collisional electrons, the transport equation for electrons can be simplified to, for \( \phi(0) = 0 \),

\[ n_e(x) = n_e(0) \exp(e \phi(x)/k T_e) \] (5)

This is the usual Boltzmann relation for isothermal electrons. Finally, we assume quasi-neutrality, \( n_i \approx n_e \).

The approximations made in deriving the above set of equations imply an ordering of the fundamental time scales governing the behaviour. Thus, for example, Maxwellian electrons imply that electron-electron collisions are very fast compared with other processes. However, the Boltzmann relation without any Fokker-Planck operator assumes that ion-ion collisions are slow compared to atomic reactions and the duct transit time. In general,

\[ \tau_{ee} \ll \tau_{\text{reactions}} \text{ or } \tau_{\text{transit}} \ll \tau_{ii} \text{ or } \tau_{00} \] (6)

\[
\begin{bmatrix}
\text{electron thermalization} \\
\text{atomic reactions}
\end{bmatrix} \ll \begin{bmatrix}
\text{divertor transit} \\
\text{ion thermalization}
\end{bmatrix} \ll \begin{bmatrix}
\text{neutral thermalization}
\end{bmatrix}
\]

Figure 7 shows these time scales as a function of temperature for plausible divertor conditions. The ordering outlined in Eqn. (6) is not well satisfied anywhere under the given conditions. Below 100 eV, it would be desirable to add self-collisions to the ions, while above 100 eV, allowances should be made for a non-Maxwellian electron distribution. However, the energies involved in the atomic reactions are less than 15 eV, so above \( T_e \approx 100 \text{eV} \), reactions should not appreciable affect the overall electron behaviour. Thus the equations derived in this section, with Boltzmann electrons and non-thermalizing ions and \( n_i \approx n_e \approx n_0 \approx 10^{20} / \text{m}^3 \), are primarily applicable for \( T_i \) and \( T_e \) in the range 100 eV to 1000 eV and \( T_0 \) less than 1000 eV. For other densities, the appropriate temperature range may vary.
3.1.4 Numerical Procedure

The numerical solution to the above system of equations is obtained by an explicit marching algorithm that "shoots" between two boundary conditions. Starting at one boundary (say, the target), an initial incoming flux of $H^+$ ions and $H$ neutrals is assumed. Depending on the surface processes, a consistent set of ion and neutral distributions coming off the target are calculated. The electron density is set equal to the net ion density (from the quasi-neutrality assumption) and the electrostatic potential is defined as zero at this point.

A new value for the potential is guessed and the ion and neutral distributions are stepped forward in co-ordinate space, taking account of collisions and acceleration in the electric field. The resultant ion density is compared with the electron density determined from the Boltzmann relation. If the two are not equal, the potential guess is reevaluated and the process repeated until $n_i = n_e$. This is repeated from the target to the end of the duct (the divertor throat). At this point, the calculated incoming ion and neutral flux (recall that it was guessed at the target to start the calculations) is compared with the desired distribution of ions and neutrals coming in from the plasma. The initial target guess is corrected and the entire calculation repeated until the boundary condition at the divertor throat is met.

3.1.5 Status

Two early attempts involved 1) $H, H^+$ and resonant charge exchange only; and 2) the ion and neutral species and cross-sections discussed in Section 3.1, but only for the particles returning from the target and only with energy-independent cross-sections. Both solutions had similar results for the structure of the density and electrostatic potential profiles in the divertor duct, as well as the same numerical behaviour [2,3].

These early results indicated the presence of an appreciable potential peak that may occur near the plate for high enough neutral densities. This can have an appreciable effect on impurity transport and the energy with which particles strike the neutralizer target. At present, the solution scheme starts just outside the Debye sheath at the target surface. Furthermore, since the emphasis is not on boundary conditions, there are a number of free parameters. The most important ones considered so far are $R$, the ratio of particle flux into the target to the particle flux coming out of the surface, and $P$, the ratio of charged particle density leaving the plate to the incoming charged particle density at the plate. Figure 1 shows that there is a region of acceptable solutions when these parameters are varied. As the boundary is approached from large $P$, the electrostatic potential drop from target to throat, $\Delta \phi$, increases (slowly at first, rapidly near the boundary), until finally $\Delta \phi$ becomes effectively infinite. Physically, the potential drop can be considered as being required to sweep the cold particles formed at the plate away in the steady-state solution.
The smaller the cold ion population (i.e. the smaller \( P \) at a given \( R \)), the larger the cold neutral population, so the harder it is to prevent an accumulation at the plate, and so the larger the required \( \Delta \phi \). At some point, it is no longer possible to sweep the cold neutrals away, no matter how large \( \Delta \phi \) is made. This sets the minimum value of \( P \). As \( R \) increases, the number of cold particles at the plate increases for a given incoming flux, so the larger the minimum value of \( P \) must be.

A representative solution from a particular case is shown in Figures 2 to 4, showing the electric field and potential profile along the duct, the density profiles along the duct, and the distribution shapes at 1 m from the target surface.

Present work is aimed at refining the calculations to include energy-dependent reactions, and to explicitly connect the divertor duct solutions with the scrape-off layer conditions and the sheath and target conditions at the other end. At that point, we will concentrate on deriving parameters of engineering interest and look at pumping, sputtering and heat loads.

REFERENCES

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<th>Heated Area (m²)</th>
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<td>0.0001</td>
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<td>1</td>
<td>inertia</td>
<td>1</td>
<td>0.0001</td>
<td>TiC</td>
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FIGURE 1: ATOM - MOLECULE INTERACTION CROSS-SECTIONS

FIGURE 2: ATOM - MOLECULE INTERACTION CROSS-SECTIONS
FIGURE 3: ELECTRON IMPACT INTERACTION CROSS-SECTIONS

FIGURE 4: ELECTRON IMPACT INTERACTION CROSS-SECTIONS
FIGURE 5: $H^-$, $H_3^+$, H(2s) INTERACTION CROSS-SECTIONS

FIGURE 6: $H^-$, $H_3^+$, H(2s) INTERACTION CROSS-SECTIONS
\[ N_1 = N_{E} = N_{0} = 10^{20}/m^3 \]

\[ A = 2.5; \quad L = 1 \text{ m}; \quad B = 1 \text{ T} \]

**Figure 7:** Time scales for various processes
4.0 POWER TEST OF THE ISX-B BUNDLE DIVERTOR

During the assembly of the bundle divertor many tests have been carried out, including the Doble high voltage leakage test, the pulse high voltage discharge test, and the DC high voltage test. The divertor discharged up to 10 kA at a 1.2 sec pulse length and 210 msec flat top. The temperature rise was below the values predicted.

4.1 Doble Test Insulation

The purpose of the Doble test was to measure the leakage of the conductor and ground. It was performed by Doble Engineering. The testing data are summarized in Table 1. The overall losses at 500 volts were found normal and the insulation was determined to be in satisfactory condition for high voltage testing at 1.0, 1.5, 2.0 and 2.5 kV AC. The power factor increases with voltage. This increase in power factor as a function of voltage is usually considered to be due to the ionization of gas in any voids in the extrapolating material and/or ionization in the insulation system at the points where the coil assemblies are brought out. The coils were also held at 2.5 kV AC for one minute and the milliwatt readings were stable.

4.2 Inductance Measurement

Before the power tests one has to measure the impedance of coil accurately and analyze the circuit characteristics for a specific power supply. Measuring the inductance of the divertor proved a tricky matter - one had to use a low frequency circuit. A one Hertz square wave voltage source was attached across a 1/3mΩ shunt resistor with the divertor connected in parallel. An oscilloscope was used to determine the voltage waveform across the shunt and thereby the divertor itself. A current probe was used in connection with the oscilloscope to determine the current waveform through the divertor. Using

\[ \frac{V_\infty}{I_\infty} = R \text{ and } v_0 = L \left( \frac{dt}{dt} \right) \]

we obtained

\[ L = 1.66 \pm .05 \text{ mH} \]
\[ R \approx 20 \text{ mΩ} \]
4.3 Analysis of Power Supplies for Power Test

The main goal of the testing was to show that the divertor will be able to perform under operating conditions. By passing a representative current, between 40 and 75% of the full operating current, it should be seen if the divertor will maintain its mechanical and thermal integrity. Several possibilities existed to drive the divertor, including capacitor banks and a motor generator set. These power supplies were analyzed and the DC motor-generator set of the National Magnet Laboratory selected.

The maximum voltage of the M-G set is 200 V and the ramp time is .4 sec. It is possible that the M-G set may not start its ramp down as planned, but may have a flattop area at peak current. Using the impedance measured previously we obtain

\[ t_{\text{max}} = 0.457 \text{s} \]
\[ i_{\text{max}} = 8570 \text{A} \]
\[ E = \int_0^{2\pi} i^2 R = 482 \text{kJ} \rightarrow 10.78^\circ \text{C} \rightarrow 19.41^\circ \text{F} \]
\[ P_{\text{peak}} = i_{\text{max}} R = 1469 \text{kJ} \]
\[ E + P_{\text{peak}} t_{\text{peak}} \leq (44.70 \text{kJ/}^\circ \text{C}) \times 50^\circ \text{F}/(1.8^\circ \text{F/}^\circ \text{C}) = 1242 \text{kJ} \]
\[ t_{\text{peak}} \leq (1242 \text{kJ} - E) / P_{\text{peak}} = (1242 - 482) / 1469 = 0.517 \text{sec} \]

If we want to limit the coil heating by 50°F then only .517 sec of the flattop can be tolerated.

4.4 Results of the Power Test

During the power test, there were twelve shots with the M-G set with one reaching 10.1 kA. For this shot:

\[ \tau = 0.50 \text{s} \]
\[ V_1 = 220 \text{V} \]
\[ i(\tau) = 9.2 \text{kA} \]

Plugging in our measured values of \( R \) and \( L \) into:

\[ i(\tau) = \frac{V_1}{R} - \frac{V_1 L}{\tau R^2} \left( 1 - e^{-R \tau / L} \right) \]
\[ = 9.18 \text{kA} \]

thus verifying the accuracy of the \( R \) and \( L \) measurements.

From the current plot of the 10.1 kA shot, one can determine that 935 kJ was deposited in the coil during the first 1.1 sec and 1068 kJ during the entire pulse. This would lead to a temperature...
rise of 20.9° C (37.7° F) in the first 1.1 sec and 23.9° C (430° F) in the entire 20 sec pulse. If there is water sitting in the coils at the time of the shot, which was the case, then the temperature would not rise as much. It would rise 18.3° C (33.0° F) in the first 1.1 sec and 21.0° C (37.7° F) in the full pulse.

The assembled ISX-B divertor was installed in a test bay in the MIT Francis Bitter National Manget Laboratory for testing on August 21, 1981. The motor-generator power source of the laboratory was used to feed programmed current pulses through the dual coils of the divertor. Current pulses ramped up in approximately 0.1 to 0.2 seconds, maintained an approximate constant value for 0.3 to 0.5 seconds and then ramped down in approximately 0.1 to 0.2 seconds. After each current pulse, the coils were cooled back to ambient temperatures using warmed water initially and then adding cooler water gradually to the cooling water reservoir and continuing until the coils were restored to approximately 285 K. Table 2 is a summary of the twelve current pulses used in the August 21 tests.
### Table 1
Doble Insulation Tests

<table>
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<tr>
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<th>MVA</th>
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<th>Power Factor</th>
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<td>.3</td>
<td>2600</td>
<td>6</td>
<td>.23</td>
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<td>thru</td>
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<td>8</td>
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<td>2800</td>
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<td>.43</td>
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<tr>
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<td>2800</td>
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<td>.64</td>
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<tr>
<td></td>
<td>2.5</td>
<td>2800</td>
<td>29</td>
<td>.89</td>
</tr>
</tbody>
</table>

<table>
<thead>
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<th>Coils</th>
<th>Test kV</th>
<th>MVA</th>
<th>MW</th>
<th>Power Factor</th>
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</thead>
<tbody>
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<td>9</td>
<td>.3</td>
<td>2500</td>
<td>6</td>
<td>.24</td>
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<tr>
<td>thru</td>
<td>1.0</td>
<td>2700</td>
<td>10</td>
<td>.37</td>
</tr>
<tr>
<td>16</td>
<td>1.5</td>
<td>2700</td>
<td>13</td>
<td>.48</td>
</tr>
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<td></td>
<td>2.0</td>
<td>2750</td>
<td>18</td>
<td>.65</td>
</tr>
<tr>
<td></td>
<td>2.5</td>
<td>2800</td>
<td>28</td>
<td>.89</td>
</tr>
</tbody>
</table>

| Overall Losses thru | 1.0 | 2700 | 10 | .37 |
|                     | 1.5 | 2800 | 13 | .48 |
|                     | 2.0 | 2750 | 18 | .65 |
|                     | 2.5 | 2800 | 28 | .89 |

**Coils**

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<th>Test kV</th>
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<th>MW</th>
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<td>.37</td>
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<td>16</td>
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<td>.65</td>
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<tr>
<td></td>
<td>2.5</td>
<td>2800</td>
<td>28</td>
<td>.89</td>
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</table>

**Overall Losses thru**

| .5 | 45 | 0 | - | UST between both boil assembles |
| 1.0 | 46 | 0 | - |
| 1.5 | 46 | 0 | - |
| 2.0 | 46 | 0 | - |
| 2.5 | 47 | 0 | - |

### Table 2
Power Test of August 21, 1981

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<td>~ 12</td>
<td>285</td>
<td>285</td>
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<tr>
<td>2</td>
<td>1000</td>
<td>~ 12</td>
<td>285</td>
<td>285</td>
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<tr>
<td>3</td>
<td>(1500)</td>
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<td>285.5</td>
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<tr>
<td>4</td>
<td>2000</td>
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<tr>
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<td>3000</td>
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<td>11</td>
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<td>12</td>
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*Temperature read with Analog Devices AD590 sensors readings in μa correspond to degrees kelvin directly for all sensors used within ~ 0.5 K.

**Current taken from plots using calibrations shown (voltage also).
5.0 DIVERTOR TARGET DESIGN

5.1 EXPERIMENTAL STUDY OF HEAVY-WALLED, WATER COOLED MOLYBDENUM TUBING

5.1.1 Summary

The divertor target or collector region will be subjected to very high surface heat fluxes, primarily in the form of energetic charged particles diverted out of the plasma reaction chamber. The program described here was carried out to demonstrate the active heat removal capability of heavy-walled molybdenum tubes for incident thermal fluxes up to 2 kW/cm² (20 MW/m²). Molybdenum is attractive for these applications by virtue of its good high temperature thermal and mechanical properties. Use of a heavy-walled tube design (12.7 mm outside diameter, 3 mm wall thickness) reflects a decision to provide sufficient material to accommodate surface erosion without loss of mechanical integrity of the internally cooled tubes.

A rastered, high energy electron beam was utilized to simulate the steady state heating anticipated on divertor collector surfaces. No attempt was made to examine the simultaneous effects of charged particle sputtering of the target surfaces. Experiments were performed at the ESURF (Electron Beam Surface Heating Facility) facility located at the Westinghouse Research and Development Center. The electron beam energy used for these experiments was generally 115–125 keV (constant for any given irradiation sequence) with beam power levels from 2 to about 30 kW as required by specific test objectives.

Three test assemblies were electron beam heated during this program. Two of these were single molybdenum tubes with heated surface areas of approximately 9.7 cm² while the other was a double tube assembly with a total heated area of 19.4 cm². The single tubes were heated under repeated "steady state" heating cycles only, while the double tube assembly was exposed to both steady state and transient heating cycles. Steady state cycles provided for ten second constant power level heating with two second ramp-up, ramp-down times, whereas transient pulses were applied in times on the order of 100 ms with each pulse lasting two seconds. Incident power densities up to 2.11 kW/cm² were used for the steady state pulses; in addition, the double tube assembly was exposed to 600 transient pulses with surface heat fluxes up to 1.1 kW/cm².

Temperature measurements were made during all surface heating experiments. Surface temperatures were recorded by an infrared camera system and subsurface temperatures were measured by means of jacketed chromel-alumel thermocouples embedded in the tube walls of the specimens. In addition, the temperature of the pressurized cooling water was measured at both the inlet and...
outlet positions by thermocouples immersed directly into the water. Results of the temperature data recorded during these experiments were in general agreement with pre-test analytically-based predictions. The major exception to this agreement was at the surfaces of the tubes where the infrared measurements seemed to provide only qualitatively correct data.

Post-test metallurgical examination of the tubes were carried out primarily by scanning electron microscopy (SEM) of the tube surfaces and metallographic examination of selected cross sections of the tubes. In addition, precise thermocouple locations were determined by cross-sectioning techniques to permit more accurate correlation of the predicted and measured temperature data.

At the highest surface heat fluxes surface melting occurred. This melting appeared to have occurred over the entire heated surface area of the single tube exposed to several steady state pulses in the 1.6 to 2.1 kW/cm² range. The depth of the melted layer was approximately 100 to 200 μm. The other major effect which was observed was the occurrence of random surface-originated cracking which extended to a maximum depth of approximately 150 μm (0.006 in.). There was no evidence of extensive crack propagation which could lead to structural failure – at least under the conditions used for these experiments.

5.1.2 Test Program

The primary objective of this program was to demonstrate the active heat removal capability of a heavy-walled, water-cooled molybdenum tube assembly for incident thermal loads up to 2 kW/cm² over a surface area of approximately 20 cm². To support this objective and to verify the feasibility of such experiments, preliminary thermal and stress analyses were carried out on several candidate test configurations, including:

- The design, analytical evaluation, and construction of a double tube, Mo test assembly having a minimum wall thickness of 3 mm.
- Analytical evaluations of the thermal and mechanical performance aspects of the heavy-walled Mo tube design, and various alternative geometries.
- Experimental investigations of the most promising geometries of Mo tube assemblies with:
  - Steady state electron beam heating at power input levels of 0.5 to 2 kW/cm² over a minimum surface area of 20 cm².
  - Shock test or transient loading at average thermal loads of 1 to 2 kW/cm² over a minimum surface area of 20 cm².
  - Post-test photographic and metallographic analyses of selected experimental specimens.

5.2
Comparison of observed experimental data with prior analytical efforts.

5.1.3 Thermal and Stress Analyses of Candidate Test Piece Configurations

Pre-test analyses were performed to examine the thermal and mechanical response of the several test piece configurations considered to be candidates for the divertor collector target application. For these analyses three distinct geometries were considered; for one of these geometries the relative performance of molybdenum and niobium were compared. In addition to single material designs, the response of a composite tube, consisting of Mo and Cu, was also examined. The primary purpose of these analyses was to provide the information necessary for recommending a specific geometry for the experimental program.

Thermal Analyses

Three basic tubular geometries were analyzed in support of the experimental program. The geometries and materials considered were: (1) molybdenum and niobium cylindrical tubes; (2) a molybdenum tube with external fins; and (3) a copper-molybdenum composite tube. The geometries are shown in Fig. 1. The smooth, simple cylindrical tube design is of interest because its relative simplicity is most consistent with economical manufacture and assembly. The finned modification of this design represents an attempt to accommodate or redistribute part of the incident heat flux in order to lower the resulting stresses compared to high thermal loading of a curved, heavy walled tube surface. Finally, the composite tube design attempts to utilize the known superior thermal properties of copper; the strength and high temperature capability of Mo are also retained in part in the composite design considered.

The peak molybdenum temperature at the peak heat flux location (top center of the tube) for the Mo tube cases with an incident heat flux of 2 kW/cm² is about 850°C. For the finned tube design, the peak metal temperature occurs at the tip of the fin where the heat conduction path is the longest. Even at that location, however, the temperature is only 1042°C (Table 1); for Mo, in the absence of extreme mechanical loads, this temperature is not necessarily excessive.

For the composite tube design for an incident heat flux of 2 kW/cm², the peak outer wall temperature is about 700°C for the molybdenum shield and 300°C for the copper tube. These temperatures are expected to be within the operating temperature range of these materials.

For a tube design such as the straight cylindrical tube (top view in Fig. 1), if the tube material is Nb instead of Mo the peak temperature is 1390°C. This higher metal temperature reflects the lower thermal conductivity of Nb relative to Mo. At this temperature, the strength of Nb is extremely
low, suggesting this material is unsuited for high heat flux applications using the present design configuration.

For an alternate molybdenum tube with a 2 mm wall thickness instead of the original specification of 3 mm, the peak outer wall temperature is lowered to 565°C (see Table 1). The performance margin of the thinner tube is therefore much improved.

**Stress Analyses**

Elastic stress analyses were performed using the Westinghouse finite element code WECAN [6] utilizing a generalized plane-strain solution with isoparametric elements and quadratic nodes. The solutions include thermal distortion of the original geometry, x-axis stress, y-axis stress, xy-shear stress, normal stress, stress intensity, and the Von Mises effective stress.

The analyses performed here describe the stresses in terms of the effective stress. In general, the results of analyses which are carried out in this manner in an attempt to predict the onset of yielding or gross deformation are measured against the maximum distortion energy, or Von Mises, criterion. This criterion is usually interpreted as predicting yielding whenever the calculated effective stress at a given stressed element exceeds the tensile yield strength of the material. Note that, for the current analyses, the existence of effective stresses which exceed the yield strength does not necessarily imply a failure condition; it merely means that, locally, plastic deformation may result from the applied thermal loads. Hence, the elastic analyses performed for this investigation do not provide information regarding the success or failure of a particular specimen design or material. These results are, however, extremely useful for comparisons of the relative merits of various collector target designs, and for the selection of favorable materials or combinations of materials.

The results of the stress calculations for each of the candidate design configurations have been presented in Table 1. Note that the metal temperatures and the effective stresses are recorded for two positions on each of the tube designs — the position where the metal temperature was a maximum, and the position where the effective stress was a maximum. That the effective stresses calculated for many of these locations exceed the respective yield strengths is not surprising. As discussed above, this reflects the purely elastic nature of the analyses. In addition to the general design case for a 3 mm thick tube wall, a separate calculation was carried out for the simple Mo tube design assuming a 2 mm thick tube wall.

For the baseline case — viz., a simple heavy-walled Mo tube, the effective stresses at both the peak temperature location and the peak stress location exceed the yield strength at the respective temperatures. These stresses drop off rapidly toward the midpoint of the tube wall. Since thermal
stresses are self-limiting, the redistribution and relaxation of stress which will occur locally should permit the Mo tube to safely accommodate these stress concentrations.

For the finned Mo tube, the peak effective stress is greater than that in the simple Mo tube baseline design. Preliminary calculations had indicated, for a thin-walled tube and thin fins, that the stresses were lowered by this design approach. Apparently, this advantage is lost when a heavy wall design is considered. Although the circumferential stress gradient is reduced, the magnitudes of the stresses are higher; hence, no advantage is associated with this design.

For the analyses of a composite tube design, the two materials were assumed to be ideally attached as, for example, by a braze. The effective stresses are quite high along the interface, especially for the copper. This implies the need for more sophisticated analysis, including the identification of specific braze properties.

The effective stresses in the simple have-walled niobium tube design case were quite similar to the corresponding values for the molybdenum tube. As was pointed out in earlier, however, the lower thermal conductivity of niobium leads to much higher peak temperatures. While this does not necessarily preclude the use of niobium, there appear to be no clear advantages of the latter metal over molybdenum.

Reducing the tube wall thickness from 3 mm to 2 mm for a simple Mo tube gave interesting results. The peak temperatures, through-wall temperature gradient, and the associated effective stresses are all noticeably lower for the thin-wall design. If the concerns over material loss by sputtering erosion, which provided the major incentive for the 3 mm wall design were relaxed, this option would appear to offer the best thermal and structural performance of all the configurations considered. For the present study, however, the thicker wall was deemed necessary to provide adequate divertor collector target service life.

The results of the preceding calculations and discussions verified the reasonableness of the simple, heavy-walled Mo tube as a structural element for a divertor collector target. This geometry was therefore adopted for testing in the electron beam heating experiments.

5.1.4 Experimental Facility and Test Procedures

The experimental facility utilized during this program, known as ESURF (Electron Beam Surface Heating Facility), consists of a high power electron beam for surface heat flux deposition and a pressurized water cooling loop for heat removal from the test samples. The beam of electrons is rapidly scanned across the target surface to simulate a larger area source of surface heat flux.

The electron beam used for all of the required tests varies in energy (constant for any given irradiation sequence) from 100 keV to 135 keV. Beam power levels vary from about 2 to 30 kW.
as required by the test conditions. The beam spot size (full-width half-maximum Gaussian) is held to approximately 1 to 3 mm as determined from heat traces on uncooled plates.

The scan circuitry allows for a minimum full length scan time of 50 µs, thereby allowing for a minimum of 0.5 ms for a complete target traversal. A balance between conservative operation of the scan circuitry and minimum target scan time dictated operation at approximately 1.5 ms for complete traversal of a single tube target.

The tests required on the sample tubes are of two basic types, steady state and transient. The steady state case involves a linear power density ramp from zero to the desired level in two seconds, followed by a continuous loading at the desired power level for ten seconds and a two second linear ramp back down to zero power. This cycle is selected to assure that thermal equilibrium is reached at some time during the pulse and that a thermal shock is not introduced during the power ramp procedure (i.e., the rise time to full power should be on the order of the thermal response time of the sample).

For the transient testing mode, it is desired to thermally shock the sample (i.e., power rise time \( \ll \) thermal response time), yet allow equilibrium to be reached during a pulse. In addition, cyclic application of these transient pulses with sufficient time between pulses for the sample to return to its initial thermal state is desired. This latter criterion avoids thermal ratcheting effects and assures uniform steady state initial conditions. To meet these criteria, a sequence of 0.1 s ramp to full power followed by a 2.0 s full power application and a 0.1 s ramp to zero power is selected. For cyclic repetition of pulses, a minimum of 8.0 s between the beginning of one pulse and the start of the next pulse is used.

5.1.5 Experimental Results

Raw data acquired during the tests include visicorder plots (continuous) of the output of the thermocouples located inside on the cold surfaces of the tube walls, and circumferential infrared radiation intensity measurements at the longitudinal midpoint of the heated portion of the tube surface. In addition, the inlet and outlet water temperatures, inlet water pressure, pressure drop, flow rate, calculated calorimetric power, and beam current available to the target are logged every half second. The total beam power (current and energy) is also recorded at the beginning of each run. Prior to external heating of any target, a characteristic curve of pressure drop vs. flow is generated.

During the experimental part of this program, a total of three test assemblies were subjected to high surface heat flux loading. The results obtained during these tests are presented in this section.
The three test assemblies included two single molybdenum tube samples with heated areas of approximately 9.7 cm² (referred to as tubes A7 and A2) and one double tube assembly with a total heated area of 19.4 cm². The double tube assembly is referred to as tubes A1 and A6. Since the two tubes of the double tube assembly reacted similarly, only one tube was subjected to post test metallurgical examinations. Thermal hydraulic data for both tubes is averaged into the data presented in this section. A total of 147 runs at power densities up to about 1.4 kW/cm² were applied to tube A7. Tube A2 experienced a total of 33 runs at power densities up to about 2.1 kW/cm². Both of these samples survived all testing without signs of leakage or rupture.

The double tube assembly was tested in both steady state and transient modes. A steady state load was applied to the two tubes 35 times at power densities up to about 1.1 kW/cm². Following these steady state runs, the tube was inspected in situ. No abnormal surface conditions were observed. Slight surface discoloration, presumably due to oxidation and vapor deposition from other sources in the chamber, was noted and is considered normal. The double tube assembly was then subjected to cyclic heating at approximately 0.8 kW/cm² for 500 cycles. In situ inspections after 100 and 200 cycles indicated no apparent changes in the surface conditions. The target assembly was then subjected to 56 transient cycles at about 0.9 kW/cm² with no apparent change in the surface condition followed by 44 transient cycles at about 1.1 kW/cm². Again no change in the surface was evident; a more thorough surface characterization was performed at this point as detailed later.

Representative steady state tube wall temperature data are plotted for one thermocouple on tube A2 in Fig. 2. A plot of the theoretically determined curves using smooth tube heat transfer coefficient \( h \) and \( 2.5h \) with associated uncertainty bands are shown for comparison. The steady state peak outer wall temperature for all targets vs. incident heat flux is plotted in Fig. 3. The temperature represented here is that determined by measurements with the infrared camera system.

Results of the effect of the transient cycling on the double tube assembly are discussed later. Heat transfer data for the transient mode are unnecessary and difficult to obtain due to the inability of the slowly responding water temperature thermocouples to provide accurate calorimetry data during the short pulse lengths.

5.1.6 Discussion of Results

Tube Wall Temperatures

Comparisons of experimental tube wall temperatures versus those calculated were presented in Fig. 2 for tube A2. The experimental data are compared with two sets of calculated temperatures.
One is based on a smooth tube and the other is based on a rough tube where the heat transfer coefficient is 2.5 times that of a smooth tube. The heat transfer analyses discussed earlier were based on long, smooth-bore tubes with no consideration given to actual wall roughness and channel entrance effects. This is the preferred approach for comparison of the relative merits of candidate targets. However, for comparison of the experimental results with the analysis of the molybdenum tubes, a more realistic determination of the heat transfer coefficient is appropriate. To this end, the Dittus-Boelter [6] heat transfer correlation used in the preliminary analysis was modified to include corrections for entrance effects and wall roughness.

According to McAdam [6] the channel entrance effect on the local heat transfer coefficient is by a factor of \(1 + (D/x)^{0.7}\) where \(x\) is the distance from the channel entrance and \(D\) is the channel hydraulic diameter. This factor approaches 1.0 when \(x\) is large. In the single tube assembly the \(x/D\) at the inlet end of the tube is about 32. The entrance effect is therefore, 1.088 or + 8.8%.

Pressure drop measurements across the test assemblies indicated that the actual pressure drops were higher than the calculated values based on a smooth tube correlation. Figure 4 shows the actual pressure drop versus flow curves for the single and double tube assemblies with manifolding. Taking into consideration the pressure drop attributable to the manifolding and allowing for both tubes in the double tube case, a set of corrected points are plotted which reveal the true pressure drop vs. flow characteristic for a single molybdenum tube. A friction factor is then calculated by comparing these results with that calculated for a smooth tube. The resultant friction factor of 1.85 is used directly in the Dittus-Boelter equation to determine the modified heat transfer coefficient.

The heat transfer coefficient corrected by the above method is a factor of about 2 higher than the original heat transfer coefficient. The augmented heat transfer coefficient (2.5 h) selected for analysis prior to these flow test results still provides a good basis for comparison between the ideal and the more realistic heat transfer conditions.

**Peak Outer Tube Wall Temperatures**

The calculated and observed (by infrared camera) peak outer tube wall temperatures (surface temperatures) are compared in Fig. 3. It is noted that the general trend is well predicted up to an incident heat flux of \(\sim 1.1\) kW/cm². Beyond this heat flux, the observed temperature decreases sharply with increasing heat flux to a minimum value and then increases gradually with further increases in the heat flux. The discrepancies in measured surface temperature from the IR camera could be due to coating of the first surface (Au) mirrors with tungsten vaporized from the beam dump at the higher beam power levels. Considerable coating of the mirrors was observed.
after the high power runs. The decrease in infrared reflectivity of the mirrors would substantially alter the interpreted temperature. Refinement of the technique for absolute surface temperature determination is required, although the relative temperature distribution across the circumference of the sample was discernible.

Post-Test Metallurgical Characterizations of Electron Beam Irradiated Tubes

Selected tubes from the electron beam heating experiments were examined by visual inspection, optical metallography, and scanning electron microscopy (SEM) to characterize the effects of the thermal exposures. Tubes which were examined included those designated A2 and A7 from the single tube, steady state heating experiments, and tube A6 from the double tube assembly experiment which included both steady state and transient heating cycles.

Tube A2 was a Mo tube specimen identical to tube A7. Tube A2, however, was subjected to fewer, but more intense, steady state heating cycles. The maximum incident power density was approximately 2.11 kW/cm². Following the beam heating experiments the area of the surface which had been irradiated was clearly delineated. Most of the tube surface appeared to consist of small hemispherical "domes", ranging generally from 100 to 200 μm in diameter, Fig. 5. Some variations in this uniform pattern, however, were noted at the regions nearest the end of the beam travel. This latter effect seems to clearly reflect the uniqueness of the beam stop or turnaround position and therefore represents a special rather than general case.

In the same areas melting has clearly occurred and several of the domes display a dimpled or donut-like appearance. This topography can be explained on the basis of a molten surface layer undergoing very rapid solidification. The surface contracts until, due to surface tension, individual domes or columns of liquid are formed. As these individual domes solidify, the volume between them is left unfilled, thereby creating the cracked appearance. These regions are not true cracks, however, but represent a shrinkage effect. The small dimples or donut holes represent a final accommodation to the liquid-solid shrinkage effect.

The results of optical metallographic examinations of longitudinal cross sections of tube A2 are shown in Fig. 6. Note that despite the melting and resolidification of the surface layer, which requires a minimum of 2610°C (melting point temperature for Mo), the tube wall below a depth of about 100-150 μm shows very little change due to the electron beam irradiations. These micrographs also clearly illustrate the point that the delineations between the domes are not cracks, but are rather artifacts of the rapid solidification.

These results of post-test metallurgical examinations of the Mo tube which was subjected to heating pulses with power densities up to ~ 2.1 kW/cm² are clearly in disagreement with the
steady state uniform heat flux analytical calculations performed for a simple Mo tube subjected to 2 kW/cm². This apparent discrepancy can be resolved by addressing the actual surface heat load imparted by a rastering electron beam in more detail. A single point on the tube surface will see the local heat flux of the beam \( q''(\sim 30 \text{ MW/cm}^2) \) for a very short time, \( t, (\sim 1\mu s) \) as the beam passes the spot. The local heat flux drops to zero as the beam passes and does not appear until \( \sim 1.5 \text{ ms} \) later as the beam continues to sweep the target. The time averaged power density \( \overline{q} \) is the total beam power divided by the target area. The local surface temperature takes a sharp rise as the beam passes, then begins to decay as the heat source is removed. The average temperature ratchets up to an equilibrium value \( \overline{T} \) while the instantaneous temperature oscillates about this mean with an amplitude \( \Delta T \). Since the local power density depends on the beam size and power and the dwell time is determined by beam size and scan speed, the amplitude of the surface temperature oscillation is determined by the beam size, power, and scan speed. The power and scan speed were readily measured during the tests. The beam size was a much less certain parameter during the tests; however, the beam control settings were duplicated after the tests when a much more accurate beam characterization procedure was developed. It was found that at 2.5 kW, the beam diameter was actually 1 mm (as opposed to 3 mm inferred from heat traces). Also, beam shrinkage was observed with an increase in beam current. The beam diameter was observed to scale roughly as the inverse square root of the beam current. This scaling indicated an actual beam diameter of \( \sim 0.3 \text{ mm} \) for the highest power runs on tube A2. The calculation of the transient temperature rise using these parameters showed peak surface temperatures \( T_m \) in excess of the melting point of molybdenum. Recent calculations which incorporate the penetration depth function of the electrons lower this peak temperature significantly, but still predict melting of the molybdenum surface for the highest power density cases of tube A2. These latest calculations also show, however, that \( \Delta T \) could be limited to about 30°C with proper control of the beam spot size using the present system.

Tube A6 was one tube of a double tube assembly which was subjected to a total of 35 steady state heating cycles followed by a total of 600 transient pulses. The maximum power density used in these experiments was approximately 1.03 kW/cm².

The only visible effects of the electron beam irradiations were noted at one end of the beam scan area and appeared somewhat similar to those observed at the overheated area of tube A7. Again, this is the area where, due to perturbations in the beam raster or power control, the beam could dwell longer than desired. In cross section, none of the cracks appeared to go deeper than about 50-100 \( \mu \text{m} \). An interesting feature was observed on a large fraction (approximately one-half) of the tube surface area. Very fine microspheres were visible on the as-liquid-honed surface of the Mo tube and were identified by energy dispersive x-ray analysis as copper. This fine copper contamination must have resulted from inadvertent extreme heating of the copper mask by the
electron beam. The pock-marked surface seen behind the Cu microspheres is the result of liquid honing the stress-relieved Mo tube.

5.1.7 Conclusions

The following conclusions are offered as a summary of the investigations reported here.

- Water cooled, heavy wall Mo tubes appear capable of sustained operation under steady state surface heat loads up to 2.11 kW/cm².
- A series of 600 transient thermal cycles at incident power densities on the order to 1 kW/cm² had negligible effect on the Mo tubes.
- Shallow (~ 150 µm) surface melting of the Mo tubes appeared to occur for incident power densities greater than about 1.4 kW/cm².
- Testing of a double-tube assembly provided no additionally useful data beyond that available from single tube tests.
- The rastered high power density electron beam available with the ESURF facility appears to provide an acceptable simulation of a larger area surface heat load.
- Simple steady state analyses and predictions of thermal distributions and the mechanical response of the unidirectionally surface heated tubes do not provide an adequate basis for understanding or interpreting the experimental results. This is particularly true near the surfaces of the tubes; subsurface or midwall temperature measurements made with embedded thermocouples appear to agree reasonably well with predicted values.
- Infrared surface temperature measurements seem to provide data which are qualitatively correct—i.e., higher temperatures at higher power densities—and describe temperature distributions well. However, additional work is required on calibration procedures before this technique can produce reliable absolute temperature data.

REFERENCES


# TABLE 1

RESULTS OF THERMAL AND STRESS ANALYSES FOR THE CANDIDATE DIVERTOR COLLECTOR TEST PIECE DESIGNS

<table>
<thead>
<tr>
<th>Design Configuration&lt;sup&gt;a&lt;/sup&gt;</th>
<th>Incident Heat Load, kW/cm²</th>
<th>At Peak Metal Temp. T, °C</th>
<th>σ&lt;sub&gt;eff&lt;/sub&gt;, MPa</th>
<th>At Peak Effective Stress T, °C</th>
<th>σ&lt;sub&gt;eff&lt;/sub&gt;, MPa</th>
</tr>
</thead>
<tbody>
<tr>
<td>Molybdenum</td>
<td>1</td>
<td>500</td>
<td>172.4</td>
<td>210</td>
<td>216.2</td>
</tr>
<tr>
<td>Niobium</td>
<td>1</td>
<td>834</td>
<td>173.1</td>
<td>215</td>
<td>253.0</td>
</tr>
<tr>
<td>Composite</td>
<td>1</td>
<td>250</td>
<td>225.0&lt;sup&gt;b&lt;/sup&gt;</td>
<td>153</td>
<td>452.3&lt;sup&gt;b&lt;/sup&gt;</td>
</tr>
<tr>
<td>- In Cu</td>
<td></td>
<td>535</td>
<td>139.1</td>
<td>153</td>
<td>777.8</td>
</tr>
<tr>
<td>- In Mo</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Molybdenum</td>
<td>2</td>
<td>850</td>
<td>425.4&lt;sup&gt;b&lt;/sup&gt;</td>
<td>217</td>
<td>542.8&lt;sup&gt;b&lt;/sup&gt;</td>
</tr>
<tr>
<td>Niobium</td>
<td>2</td>
<td>1390</td>
<td>435.0&lt;sup&gt;b&lt;/sup&gt;</td>
<td>218</td>
<td>543.3&lt;sup&gt;b&lt;/sup&gt;</td>
</tr>
<tr>
<td>Composite</td>
<td>2</td>
<td>304</td>
<td>336.0&lt;sup&gt;b&lt;/sup&gt;</td>
<td>190</td>
<td>392.5&lt;sup&gt;b&lt;/sup&gt;</td>
</tr>
<tr>
<td>- In Cu</td>
<td></td>
<td>697</td>
<td>236.4</td>
<td>190</td>
<td>832.4&lt;sup&gt;b&lt;/sup&gt;</td>
</tr>
<tr>
<td>- In Mo</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Finned Mo</td>
<td>2</td>
<td>1042</td>
<td>428.9&lt;sup&gt;b&lt;/sup&gt;</td>
<td>862</td>
<td>747.2&lt;sup&gt;b&lt;/sup&gt;</td>
</tr>
<tr>
<td>Molybdenum With 2mm Wall</td>
<td>2</td>
<td>565</td>
<td>238.3</td>
<td>218</td>
<td>307.3</td>
</tr>
</tbody>
</table>

<sup>a</sup>All tubes 3 mm wall thickness unless otherwise noted; see Figure 1.

<sup>b</sup>These effective stresses are above the respective tensile yield strengths at the predicted temperatures.
Figure 1. Candidate Target Design Geometries Which Were Analyzed

Figure 2. Tube Wall Temperature/Water Temperature Difference vs. Heat Flux for Steady State Heating of Tube A2 at the Outlet Thermocouple Position
Figure 3. Peak Outer Tube Wall Temperature vs. Heat Flux

Figure 4. Pressure Drop vs. Water Flow Rate for Single Tube and Double Tube Test Assemblies With and Without Manifolds
5.2 SUPERSONIC GAS TARGET DESIGN

5.2.1 Introduction

One of the major engineering difficulties which divertor designers face is the actual extraction of large quantities of plasma heat (of the order of 100 MW in a typical power reactor design) in an exhaust chamber of reasonable dimensions. These basic difficulties can be reduced by using a gaseous target [1,2]. This would protect the back plate of the divertor from overheating and sputtering and the gas target could be made to circulate through a heat exchanger and transfer the heat from the divertor region by forced convection. Its smaller size simplifies the divertor exhaust chamber design, since it allows for a much more compact device. Also, the system operates at a considerably higher target pressure than a solid, high vacuum collector scheme, hence reducing the pumping technology requirements as well.

A potential problem with gas targets is the backflow of neutral gas from the chamber into the divertor channel. This backflow could produce a low temperature, high density plasma which would lead to enhanced cross-field diffusion and localized hot spots along the divertor walls. Eventually such a situation could lead to divertor choking or other consequences of varying seriousness. However, the backflow problem may be ameliorated by an effect which plays an important role in the present divertor target arrangement: momentum transfer. In general, the plasma exiting the divertor channel is a reasonably well collimated stream of ions and electrons; that is, the momentum is concentrated in the forward direction. The pressure of this exhaust plasma is highly anisotropic. The momentum associated with the speed of the flow could be used to advantage by letting it plug the entrance of the exhaust chamber and thus reduce the flow of gas back into the channel. This effect has been previously suggested and experimentally observed [2]. The geometry of these systems lends itself more naturally to bundle divertors than other configurations.
5.2.2 The Physics of the Interaction

Particle Penetration

When a typical plasma ion interacts with a gaseous target, a wide variety of reactions are likely to occur [6]. Of these, the most influential one is charge exchange. The second most important is the stripping reaction, which is responsible for reionizing charge-exchanged neutrals. For target densities of interest in this concept (i.e., \(10^{21}/m^3\)), the charge exchange mean free path is about 0.1 mm for 4 keV ions, and the stripping mean free path is about 1 mm. Since there is little energy and momentum transfer interactions, the incident ion makes many collisions before thermalizing with the background gas.

The physics of electron penetration are similar to that of the ions; there will be a penetration depth associated with them as well. Inspection of available data [8] indicates that the mean range for electrons in this system is also about 10 cm, comparable to that of the ions [6,7]. Accordingly, the treatment of this complex problem has been simplified by omitting the electron dynamics, since they contribute relatively little momentum to the interaction.

Force Balance

The basic condition for establishing a stable boundary is that the total momentum delivered by the plasma stream must be balanced by the static pressure exerted by the gas. As the plasma stream enters the gas target area, the magnetic field lines tend to fan out and spread because the shaping coil system ends there. In this approximate treatment of the problem, the gyro-motion of the plasma particles and hence their perpendicular pressure is neglected (this is a pessimistic assumption). Under these conditions, the plasma particles experience an angular spread in their velocity vectors as they initially tend to follow the field. The net effect is that the particles are able to exert pressure in the transverse as well as the forward direction. The gyro-motion will tend to enhance this effect and should be included in a more complete analysis. For a given plasma flux and fixed target conditions, there will be only one value of \(R\) where pressure balance is satisfied. An approximate interaction region in the shape of a right circular cylinder is used as shown in Fig. 1. For equilibrium,

\[
n_p m_p V_p^2 A_1 = P A_2
\]  

(1)
where \( n_p \) = plasma density; \( m_p \) = mass of plasma particles; \( V_p = \sqrt{2kT_p/m_p} \) = ion speed; \( T_p \) = ion temperature; \( A_1 \) = divertor channel cross sectional area; \( P \) = target gas pressure; and \( A_2 \) = area bounded by the interaction region. Expressing \( A_1 \) and \( A_2 \) in terms of \( d \) and \( R \).

\[
P = \frac{2n_p k T_p}{(1 + 4R/d)}
\]  

(2)

The plasma ion density \( n_p \) can be further related to the geometry of the system and the plasma temperature. Assuming an ion flux of \( 1.25 \times 10^{23} \text{ s}^{-1} \), equivalent to 20 kA of charged particles \([10]\), a temperature of 4 keV and a mean ion mass of 2.5 proton masses, then

\[
n_p = \frac{5.74 \times 10^{17}}{d^2 \sqrt{T_p(\text{keV})}}
\]  

(3)

**Exhaust Plasma**

Equations (2) and (3) give the physics requirement on the temperature and pressure of the target gas. Combined with the data on range, Refs. [6] and [7] can be used to determine the basic design criteria for the formation of the interaction region. These combined results are shown in Figs. 2 and 3: In both figures, one family of curves represents the value of \( R \) required for pressure balance determined from Eq. (5); the other family represents the actual value of \( R \) for given temperatures and pressures. In order for the boundary to be physically possible, while satisfying the force balance condition, the two curves must match; the value of \( R \) at which they do represents the depth to which the interaction region will form and remain stable. In Fig. 2 the curves do not match at high gas temperatures (i.e., \( 500^\circ \text{C} \) to about \( 50^\circ \text{C} \)). At low temperatures, the curves match for small diameter plasma channels (i.e., 5 cm). For example, the \( 0^\circ \text{C} \) curve matches the \( d = 5 \text{ cm} \) curve at a pressure of approximately 1 Torr; the interaction region will extend in this case to a depth of approximately 20 cm. One can go to small diameter channels and achieve matching with high temperature target, or one can go to a larger diameter channel and achieve matching with low temperature targets; this possibility is shown in Fig. 3. The second situation is the more desirable to avoid a large mirror ratio at the channel exit. Fortunately, this path is also feasible and thermodynamically natural for several reasons:

1. The plasma exiting the divertor has been assumed to be at 4 keV [5], however, some divertor models [4] estimate much lower temperatures and higher densities. If this is the case, the force exerted by the plasma on the target is higher for the same power by the square root of the temperature ratio.

2. Electron pressure, as well as perpendicular pressure, will tend to enhance the available force exerted by the plasma.
3. The low target temperatures required for this situation may be possible because the nozzle expansion of the gas produces substantial cooling.

Heat Removal

The neutral mass flow rate \( m' \) required to remove the plasma exhaust energy \( Q \) is a function of the average temperature increase in the target, \( \Delta T \).

\[
m' = \frac{Q}{C_p \Delta T}
\]

where \( C_p \), the heat capacity of \( D_2 \), is \( 1.43 \times 10^4 \) J/kg°C [11]. A temperature rise of 200°C at the target from an 80 MW heat rate requires 28 kg/s.

If the gas were pumped at the low pressure required at the target, the volumetric flow rate would be very high. Also, if a low velocity gas target were to be used to stop the charged particles in a few centimeters, the pumping rate would be enormous – about \( 10^7 \) l/s for a target pressure of 10 Torr. This is one of the basic problems that must be solved.

5.2.3 Design Concept

Overview

The solution to the problems outlined above is to allow pumping at higher pressure and hence at lower volumetric rates, while allowing a low pressure region to form only near the target, where it is needed. Such a pressure discontinuity implies the existence of a shock which, in this case, is used to advantage. The low pressure region would be consistent with that required by the plasma-gas interface described earlier. This effect can be achieved in principle by isentropically accelerating the flow near the target with a Laval nozzle.

A possible arrangement is depicted in Fig. 4. Deuterium gas in a reservoir is allowed to accelerate to high Mach numbers through a converging/diverging nozzle. The plasma from the divertor is directed at a slight angle to meet the expanding gas near the nozzle exit. The nozzle is designed to attain about 10 Torr at the center of the flow, but gas profile effects produce lower values (1-3 Torr) near the plasma channel exit. Pressure balance produces a plasma-gas interface which reduces the backflow of gas into the divertor. The plasma energy is deposited directly onto the flow and is assumed to be carried away by both conduction and convection as the flow continues downstream through a variable diffuser.

5.20
The maximum mass flow rate in the nozzle results from having sonic conditions at the throat. Downstream from the throat it is desired to reduce the pressure to match that at the divertor exit, and to speed up the flow and thus maintain the high mass flow rate. Therefore, the nozzle must have both converging and diverging sections. The target temperature will also be lowered by the expansion at the nozzle exit; as pointed out earlier, this is a desirable effect since it will allow a wider plasma channel and therefore reduce the mirroring effect at the divertor channel exit.

Nozzle Design

Consider the converging/diverging nozzle shown in Fig. 4. The flowing gas is accelerated to a high Mach number and is then made to blend with the plasma stream at the nozzle exit. The flow at the nozzle throat will be sonic. The flow velocity there is given by

\[ V^* = c = \sqrt{\gamma k T^*/m} \]  

where the superscript (*) refers to properties at the throat, and \( \gamma \approx 1.4 \) is the ratio of the specific heats. Let \( T^* = 500 \text{ K} \) be the temperature at the throat; further assume a stagnation pressure of 1 atm, and set the molecular mass \( m \) at \( 3.34 \times 10^{-27} \text{ kg} \) for pure deuterium gas. Then, \( V^* = 1.70 \times 10^3 \text{ m/s} \). From the isentropic data tables it is found that \( T^*/T_o = 0.8333 \) \( (M^* = 1) \) so that \( T_o = 600 \text{ K} = 327^\circ \text{ C} \), where the subscript \( (o) \) denotes stagnation properties. The throat area \( A^* \) can be obtained from continuity, that is \( m' = \rho^* V^* A^* \) and the ideal gas law, \( p^* = \rho^* k T^*/m. \) From the isentropic data tables, \( p^*/p_o = 0.528 \) so that \( p^* = 401 \text{ Torr} \) at \( p_o = 1 \text{ atm} \). Thus, \( A^* = 0.637 \text{ m}^2 \), or a throat diameter \( d^* \) of 0.90 m.

This flow must be expanded to a pressure, \( p_e \), of approximately 10 Torr at the flow centerline. This gives about 1-3 Torr at the plasma channel exhaust, which is the required pressure to balance the momentum of the plasma stream as discussed previously. Therefore, the pressure ratio is \( p_e/p_o = 0.0132 \) which corresponds to an exit Mach number \( M_e \) of 3.5. The area and temperature ratios pertaining to exit conditions are obtained from isentropic data. Thus \( A_e/A^* = 6.7 \) and \( T_e/T_o = 0.289 \) resulting in an exit area of 4.27 m\(^2\), an exit diameter \( d_e = 2.33 \text{ m} \), and an exit temperature \( T_e = 173 \text{ K} \). Also, \( \rho_e/\rho_o = 0.045 \) and \( \rho^*/\rho_o = 0.635 \) so that \( \rho_o = 0.0409 \text{ kg/m}^3 \), and \( \rho_e = 0.00184 \text{ kg/m}^3 \).
Referring again to Fig. 4, the plasma leaving the nozzle exit does so at very high velocity. For an exit temperature of 173 K, the exit velocity is 3000 m/s, which corresponds to a Mach number of 3.5. At this point, and nearly tangential to the flow, the plasma streams are allowed to enter and deposit their energy on the target gas. The static pressure at these entry ports is below 10 Torr, as required by the plasma gas boundary discussed earlier. Under these conditions, the plasma stream will penetrate several centimeters into the target. The plasma energy raises the bulk temperature of the flow, and a fraction of it can be recovered downstream with a heat exchanger.

As the flow is being heated by the plasma, it is also made to slow down in the duct (which acts as a supersonic diffuser). The heat transfer is highly localized near the source; however, it is expected that substantial mixing will occur as the flow moves downstream. For the present analysis, it is assumed that the heat transferred per unit mass to the flow drops linearly with distance along the duct.

The addition of heat is sufficiently high to reduce the Mach number for both converging and diverging ducts; however, the variation of duct area with distance can be tailored to optimize the flow conditions. It is desirable to have a low Mach number since the shock losses increase as $M^3$; however, the flow must be supersonic to prevent backflow into the divertor channel.

By fixing the back pressure, a stable shock can be obtained downstream of the diffuser throat. This situation allows a significant pressure increase as the flow suddenly decelerates to subsonic conditions. Because of the entropy increase and various other inefficiencies, the pressure downstream of the shock is only a fraction of the initial value; hence, a suitable compressor will be required to complete the cycle. At the same time, the temperature downstream from the shock will be sufficiently high to allow some energy recovery.

The length of the duct is mainly determined by the heat transfer behavior within the gas. It is desired that sufficient mixing occur in a reasonable distance such that the temperature will be essentially uniform in the radial direction immediately before the shock. Such mixing depends on a large variety of flow parameters such as Reynolds number, wall friction, and boundary layer effects, which need to be determined experimentally. For the present calculations, a diffuser length of 5 m has been assumed.
Consider the geometry shown in Fig. 8, where a converging diffuser is used to slow down the flow coming out of the nozzle. The system is in steady state, with exit velocity and temperature denoted by \( V_e \) and \( T_e \), respectively. The mass flow rate is a constant denoted by \( m' \).

Let \( L \) be the length of the diffuser and assume that the heat \( Q \) (Watts) from the plasma is added in a linearly decreasing manner over \( L \).

The energy per unit mass per unit length is therefore

\[
\frac{dq}{dx} = \frac{2Q}{L m'} \left( 1 - \frac{x}{L} \right)
\]

For one-dimensional flow with heat addition but without friction, the fractional change in velocity is related to the Mach number, the fractional area change, and the amount of heat added, as follows [12]:

\[
\frac{dV}{V} = \frac{1}{M^2 - 1} \frac{dA}{A} - \frac{dq}{C_p T}
\]

where \( C_p \) is the heat capacity at constant pressure and \( T \) is the temperature of the fluid. The second term in Eq. (7) can be obtained from energy conservation. The amount of heat \( dq \) added to the flow must go into both random and directed flow energy; that is

\[
\frac{dq}{dx} = C_p \frac{dT}{dx} + V \frac{dV}{dx}
\]

where \( V \) is the flow velocity. The flow temperature can be obtained by integrating Eq. (8)

\[
T = \frac{1}{C_p} \left[ q + \left( V_e^2 - V^2 \right)/2 \right] + T_e
\]

where \( T_e \) and \( V_e \) are initial values of temperature and velocity respectively. Using the definition of Mach number \( M^2 = V^2 m/ (\gamma kT) \) The final result is

\[
\frac{dV}{V} = \frac{dx}{(M^2 - 1)} \left( \frac{2a}{r_o + ax} - \frac{2Q(1 - x/L)}{L m' C_p T} \right)
\]

where \( a \) is the slope of the diffuser walls and \( r_o \) is the initial radius of the duct.

This equation has been solved numerically. The results are presented in Figs. 5 and 6.

Figure 5 shows the actual results for a converging duct with a linear slope of 5% ; the mach number decreases from 3.5 to 1.2, the velocity decreases, and the temperature increases from 173 K to about 600 K. Figure 6, on the other hand, shows the results for a diverging duct. In that case, the Mach number decreases initially and increases slightly toward the diffuser exit. This is a
temperature effect, that is, the cooling due to the expansion of the flow near the end is sufficiently strong to overcome the heating from the plasma stream.

Figures 5 and 6 illustrate how the diffuser contour can be tailored to minimize the amount of frictional and shock losses that will be present. For example, by reducing the velocity of the flow, the frictional losses are also reduced; at the same time, by operating at low Mach numbers (although still greater than 1), the shock losses are also reduced. In the present frictionless design, the converging diffuser is more attractive. In the real case, the frictional effects may have to be compensated for by using a diverging duct.

At the diffuser exit, a throat will exist which is wider than the isentropic throat consistent with the conditions upstream. The flow will therefore not become sonic there. Instead, the walls of the duct will be made to diverge and the back pressure will be fixed such that a normal shock will be established slightly downstream from the throat. Under these conditions, the shock will be stable to small disturbances in the flow and will not be swallowed upstream [13]. The presence of the shock will introduce a sudden deceleration of the gas; its kinetic energy will be partly recovered as pressure downstream. The basic design procedure is as follows.

Assuming a linearly converging duct with slope \( a = -0.05 \), the diffuser exit radius is 0.92 m, corresponding to a diffuser exit area \( A_d = 2.66 \, \text{m}^2 \). The exit Mach number at that point is 1.23. The throat area \( A_d^* \) consistent with this Mach number is 2.57 (from isentropic data). Let the shock exist downstream from the throat, at a point where the duct area is \( A_s = 3 \, \text{m}^2 \). At that point \( A_s/A_d^* = 1.17 \) and immediately before the shock, the Mach number is \( M_x = 1.5 \).

The static pressure at the diffuser exit is given by

\[
p_d = \frac{m'kT_d}{(V_d A_d m)}
\]

where \( d \) refers to diffuser exit. This gives \( p_d = 87.1 \) Torr and from isentropic data \( p_{dO} = 216.8 \) Torr. The pressure ratio before the shock is \( p_{sx}/p_{dO} = 0.272 \) so that the static pressure behind the shock is \( p_{sx} = 59.05 \) Torr and from normal shock data, at \( M_s = 1.5p_y/p_x = 2.458 \) where \( y \) denotes conditions after the shock; hence \( p_y = 145 \) Torr.

In principle, one seeks to obtain the highest possible pressure beyond the shock \( (p_y) \), since this would mean that a smaller compressor would be required to bring the gas to its initial pressure to complete the cycle. In the present design, a pressure of 145 Torr is obtained. This amounts to about 20% of the 1 atm required. A suitable compressor is needed to close the cycle.
Compressor Requirements and Energy Recovery

Since gas downstream from the shock will be hot, some form of heat removal must be supplied; this requires a heat exchanger in series with the compressor. A fraction of the heat removed could be recovered via a conventional thermal cycle and used to help meet the compressor power requirements. The power required can be estimated from the mass flow rate, \( m' \), the required inlet and outlet conditions and the ideal gas law:

\[
W = p_2 V'_2 - p_1 V'_1 = \left( \frac{km'}{m} \right) (T_2 - T_1) \tag{12}
\]

where \( V' \) indicates volumetric flow rate and the subscripts indicate inlet (1) or outlet (2) conditions. If the compression is adiabatic,

\[
T_1 = T_2 \left( \frac{P_2}{P_1} \right)^{\frac{1-\gamma}{\gamma}} \tag{13}
\]

An exit pressure of 760 Torr, an inlet pressure of 145 Torr, and an outlet temperature of 600 K require \( T_1 = 373 \) K. The pumping power may now be estimated from Eq. (12) as 28 MW.

Knowing \( T_1 \), the power recovered by the heat exchanger can be obtained from \( \Delta H = C_p m' \Delta T \) where \( \Delta H \) is the change in enthalpy across the exchanger. Since \( \Delta T = 227 \) K, this yields \( \Delta H = 100 \) MW. Assuming an electric conversion efficiency of 25\%, one could extract almost all of the power needed to run the compressor. Additional power will probably be needed to overcome frictional losses, especially in the heat exchanger; however, it is expected that these losses can be small compared to the power extracted.

5.2.4 Concluding Remarks

Some potential difficulties are envisioned. First, the flow may be drastically affected by the addition of heat from the plasma stream; second, the heat may be transferred from the edge of the jet where the interaction region is via thermal conduction, a process which may be too slow to reach into the central region of the flow. That problem has not been evaluated, and a suitable flow-mixing model needs to be incorporated into the design. This model is expected to have an impact on the design of the supersonic diffuser, particularly its desired length.

An important aspect which has not been evaluated is the heat transferred to the diffuser walls, particularly near the divertor exhaust ports. The materials problems and cooling requirements in these regions await further study.

The effects caused by the presence of a boundary layer and other frictional effects need to be evaluated as they directly impact the pump size and power requirements of the system.
ACKNOWLEDGMENTS

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REFERENCES

Fig. 1: Approximate interaction region geometry used in computations.
Fig. 2: Mean ranges versus target pressure satisfying physics and engineering requirement, showing the effect of smaller diameter plasma streams or lower temperatures.

Fig. 3: Schematic representation of the supersonic gas target concept.
Q = 8.07 \times 10^6 \text{Watt}

To = 600 \text{}^\circ \text{K}

Pe = 760 \text{Torr}

m = 28.07 \text{kg/sec}

Ae = 4.27 \text{m}^2

Te = 173.4 \text{}^\circ \text{K}

Pe = 10 \text{Torr}

Ma = 3.5

Ad = 2.66

M = 1.225

Pd = 80 \text{Torr}

Pe = 199 \text{Torr}

As = 3.86

Ms = 1.86

Pey = 156.05 \text{Torr}

Fig. 4: Nozzle/diffuser/expander regions showing flow parameters at the various stages.

Converging duct

\[ T(\text{final}) = 616 \text{}^\circ \text{K} \]
\[ V(\text{final}) = 2314 \text{ m/sec} \]
\[ Ma(\text{final}) = 1.22 \]
\[ a = -0.05 \]

Fig. 5: Fluid velocity, temperature, and Mach number along a converging diffuser duct.
Fluid velocity, temperature, and Mach number along a diverging diffuser duct.

Fig. 6: Fluid velocity, temperature, and Mach number along a diverging diffuser duct.
5.3 DIVERTOR SOLID TARGET OPTIONS

5.3.1 Introduction

This paper will concentrate on various arrangements of solid target materials under active cooling in order to understand design tradeoffs among conventional solid target options. The major constraints are heat removal, surface erosion rates, and fatigue life. The eight candidate elements and alloys are: Beryllium, Graphite, Aluminum, Titanium Alloy (Ti-6Al-4V), Vanadium Alloy (V-25Cr-8Zt), Copper Alloy (SAC-2), Niobium Alloy (D-43), and Molybdenum Alloy (TZM).

The desired divertor target in this investigation must survive for one year under operating heat loads of 1 kW/cm². Base operating conditions derived from ETF [1] and INTOR [2] studies are
- Pulse duration is 90 seconds.
- Rejuvenation period is 15 seconds.
- Each target undergoes \(10^5\) plasma pulse cycles per year.
- Ion temperature at the target is 1.3 keV.
- Particle current is \(1.5 \times 10^{23}/\text{sec.}\). 
- Available surface area for a bundle divertor target is 9 m².

In addition, the following parameters were chosen as reasonable near-term conditions.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Coolant</td>
<td>subcooled water</td>
</tr>
<tr>
<td>Coolant Channel Length</td>
<td>(L = 10) cm</td>
</tr>
<tr>
<td>Channel Diameter</td>
<td>(D = 1) cm</td>
</tr>
<tr>
<td>Coolant Pressure</td>
<td>(p = 500) psia, 3.45 MPa</td>
</tr>
<tr>
<td>Coolant Inlet Temperature</td>
<td>(T_{in} = 30^\circ)C, 86°F</td>
</tr>
</tbody>
</table>

5.3.2 Thermo-Hydraulics

Applicable CHF Correlations

Various CHF correlations are plotted for the base case as in Fig. 1. None of the physical mechanism boundaries on mass flux comes close to predicting CHF. A better theoretical approach would be determination of the point of Net Vapor Generation, following the procedure of Zuber.
and Saha [19]. A plot of the Saha-Zuber equation appears on Fig. 1, showing good agreement with the Bernath and Bunther correlations at intermediate subcoolings. The agreement is not as close when tube length is varied, but the Saha-Zuber procedure provides a CHF prediction based on a plausible theoretical mechanism.

Of the empirical correlations, the Bernath correlation seems most appropriate for the base case conditions, however, its range does not include particularly high subcooling. Rousar [5] accounts for high subcooling at elevated pressures for tube lengths up to 15 cm and suggests that for very high heat fluxes, local conditions govern CHF, independent of pressure, length, and diameter. Lowdermilk et al. [6], with thin tubes up to 114 cm in 1 kW/cm², indicates a length and diameter effect. It does not, however, include subcooling. In order to permit variation of length, diameter, and subcooling, a combination of the Rousar and Lowdermilk correlations is proposed for the 1 kW/cm² heat flux range. The Rousar treatment of CHF as a local condition proportional to subcooling is applied to the Lowdermilk correlation, giving

\[ q_{\text{crit}}'' = \frac{1400}{D^{0.5}L^{1.5}} \left[ \frac{G \Delta T_{\text{sub}}}{90} \right]^5 \]  

(1)

**Pressure Drop**

Extremely high heat loads have been handled successfully, where the heated length was on the order of 1-2 cm, by pumping coolant at very high velocity and with large pressure losses [4,7]. For the divertor, however, longer heated lengths are desirable.

For the case of single phase flow the pressure drop is

\[ p = \frac{fL G^2}{2D \rho g_e} \]  

(2)

where \( f = (0.184/Re^{0.2})(\mu_w/\mu_0)^{0.25} \).

At high heat loads and moderate mass flux, subcooled boiling along the heated channel walls is possible. Several investigators provide some indication of the pressure losses to be expected, but the wide variance in pressure drops and test conditions suggest more experimentation is needed [8,9,10].

At sufficiently high subcoolings, adequate cooling can be provided without boiling for a 1 kW/cm² heat flux, in tubes up to 1 meter long, with a mass flux on the order of \( 10^7 \) lbm/hr-ft². The nonboiling pressure drop over a 1 meter long tube at this mass flux is less than 3 psi, so that even a six-fold increase in \( \Delta p \) due to subcooled boiling should not be a major problem.
Only at lower inlet pressures, higher heat loads, and longer lengths would the subcooled boiling pressure drop become critical.

The ratio of pumping power to thermal power extracted by the coolant is

\[ W_p = \frac{\text{Pumping Power}}{\text{Thermal Power}} = \frac{\dot{m} \frac{\Delta p}{\rho}}{\dot{m} c_p \Delta T_{1-2}} = \frac{\Delta p}{\rho c_p \Delta T_{1-2}} \]  \hspace{1cm} (3)

Based on overall power conversion efficiency in a fusion reactor blanket, Fraas [11] suggested a design limit for this ratio of 2%. Since the divertor would handle only a small fraction of the total plasma power, it could be run at a much higher ratio without seriously impacting overall plant efficiency. However, since the occurrence of subcooled boiling may increase the pressure drop, the 2% limit will be used for the design window, with \( \Delta p \) calculated for nonboiling flow.

Heat Transfer Coefficient

For the design window the McAdams single phase heat transfer coefficient is used,

\[ Nu = 0.023 (Re)^0.8 (Pr)^{-1} (\mu_b/\mu_w)^{14} \]  \hspace{1cm} (4)

Subcooled boiling would make this equation conservative. Furthermore, the steep temperature gradient at the wall in the entrance region would enhance heat transfer there, further increasing the conservatism for short tubes. For base case conditions and mass flux on the order of \( 10^7 \) lbm/hr-ft\(^2\), this equation predicts a film temperature rise of \( \sim 270^\circ \) F.

Swirl and Mixed Flow Schemes

Coolant channel performance is improved when internal devices are used to induce swirl flow. The effect is to strip away nucleating bubbles and force denser (colder) coolant to the outside wall where heat transfer is occurring. Approximately 1.5 to 1.8 times higher CHF can be realized over straight flow at the expense of 1.4 to 1.8 times higher pressure drop [3,12].
Asymmetric Heating

Collier [13] summarizes data regarding the effects of asymmetric circumferential heating. For highly subcooled boiling, he recommends treating burnout as a localized condition. This implies that burnout occurs where the incident flux is normal to the channel wall, at the same heat flux as would cause burnout for the uniform heating case. This is clearly conservative, as no allowance is made for fluid convection, conduction around the tube circumference, or conduction in the fluid.

5.3.3 Surface Interactions

Sputtering

The rate at which the material will sputter is

\[
\frac{\Delta t}{\Delta \tau} = \sum_i S_i J_i C \left( \frac{M}{\rho N_A} \right).
\]

Energy, mass and incident angle have large effects on the sputtering coefficient. Most particles will be plasma hydrogen ions, normally incident on the target. We conservatively assume that they will be accelerated from a plasma edge temperature of a few hundred eV to around 1.3 keV. Under the base case conditions, the sputtering erosion rates are 11 mm/yr for Nb, 15 mm/yr for Mo, 27 mm/yr for graphite, 27 mm/yr for Ti, 28 mm/yr for V, 43 mm/yr for Be, 104 mm/yr for Al and 175 mm/yr for W.

Internal Erosion

The target material may also be eroded on coolant side. Common corrosion rates are at least three orders of magnitude smaller than calculated sputtering erosion rates [14] so should have little effect. Mechanical erosion by the rapidly flowing coolant may, however, be a concern. Experience with high mass flow boilers and studies of rain damage to high performance aircraft have indicated that liquid water traveling at speeds in excess of 50 meters per second will cause mechanical damage to metal surfaces. Coolant velocities of less than 30 meters per second should be adequate for 1 kW/cm² heat flux, so direct erosion should not occur. Surface boiling may generate regions where bubbles collapsing cause erosion rates comparable to those experienced in cavitation erosion. Such erosion has a velocity below which erosion is undetectable. Above this threshold, the erosion rate increases approximately as the velocity to the sixth power [15].
The main damage mechanism in cavitation erosion occurs when voids in the fluid suddenly collapse, generating intense shock waves and liquid microjets capable of pitting or work hardening nearby surfaces until fatigue failure and material loss occur. Voids generated by boiling on a hot surface cannot collapse at the surface in the same way as cavitation bubbles since vapor is always being generated by the addition of heat. In subcooled boiling, the bubble collapse should generally occur away from the heated walls and at a slower rate than in cavitation. Thus any internal erosion should be downstream from the heated surface, and of lesser intensity than that due to cavitation.

5.3.4 Thermo-Mechanics

Pressure Stress

The greatest allowable stress intensity within a material is established by the ASME Code as $S_{ult}$, and must be less than $1/3$ of the ultimate strength and less than $2/3$ of the yield strength. For thick walled cylindrical pressure vessels, under internal pressurization, the tangential stress is tensile, the radial stress compressive, and the axial stress intermediate. The stress intensity will therefore be

$$|\sigma_t - \sigma_r| = \frac{2a^2b^2}{b^2-a^2} \frac{p}{r^2}$$

which is maximum at the inside surface of the cylinder, $r = a$. Keeping this stress within limits implies a minimum thickness

$$t_{min} = \left(-1 + \frac{1}{\sqrt{1-2p/S_{ult}}}\right)A.$$
Thermal Stress

Careful design is necessary to minimize the external constraints on material expansion under heat load. For sections of a plate or thin walled cylindrical target sufficiently far from the edge attachments, under steady state conditions, the temperature profile may be assumed to be nearly linear from the high temperature heated surface to the low temperature cooled surface. For a plate constrained against bowing and for a cylindrical tube, the peak thermal stress will be

$$\sigma_{th,peak} = \frac{E \alpha \Delta T}{2(1-\nu)}$$

compressive on the hot surface, tensile on the cold. The difference in temperature may be expressed in terms of the heat flux, wall thickness and conductivity, yielding

$$\sigma_{th,peak} = \frac{E q' t \alpha}{2 k(1-\nu)}$$

Thermal stresses at edge connections become difficult to determine analytically and require specific study for particular designs. Except in the cases of a free edge or specific designs in which the edge attachments deflect readily, edge connections will cause a rise in stress over the "infinite wall" case. Equation (9) is therefore an optimistic estimate of the thermal stresses to be encountered.

Fatigue

For the base case burn cycle considered in this analysis, over $10^5$ cycles are likely during one year’s operation, introducing strain-cycle controlled fatigue failure as a controlling design consideration. Considering a semi-infinite body whose surface is exposed to periodic temperature cycling, the fraction of the surface temperature fluctuation experienced at a depth, $x$, is

$$\frac{\Delta T_2}{\Delta T_0} = \exp \left( -\frac{\pi}{\omega \alpha_0} \right),$$

where $\omega$ is the period of oscillation, and $\alpha_0$ is material thermal diffusivity. This suggests that thermal strains inside the material might be less than calculated using steady state equations. The divertor target is not infinite, however, and is more closely modeled by a slab with fixed temperature, $T_{in}$, on one side, and a periodic function surface heat flux on the opposite side, $q''_s$. The times required to reach a steady state temperature profile [16] for practical slab thicknesses (1 to 10 mm) and the eight materials considered in the analysis, are at least an order of magnitude less than the fifteen seconds pause time between burns. Fatigue strain ranges are therefore calculated based on full temperature cycling between a steady-state burn condition and a steady-state off condition.
The cyclic component of these peak strains must be kept below $\Delta \epsilon_{\text{max}}$. Substituting $\sigma_{\text{th,peak}} = E(\Delta \epsilon_{\text{max}})$ into Eq. (9) and rearranging gives

$$t \leq \frac{2k(1-\nu)\Delta \epsilon_{\text{max}}}{\alpha q''}, \quad (11)$$

Representative curves for TZM and copper are shown in Figs. 3a and 3b.

**Thermal Shock**

The extremely steep temperature gradient at the heated surface immediately after burn initiation may result in cracking, spalling and accelerated sputter damage. The thermal shock parameter, $\sigma_{\text{max}}(k cp\rho)^{1/2}/E$ gives a measure of a survivability in such an environment. Schivell and Grove [17] indicate that the greatest magnitude of the additional stress due to thermal shock on a flat plate will be $\sim 1/5$ of the maximum steady-state thermal stress, and will occur at a depth of approximately $4/10$ of the total thickness. This location does not coincide with that of the peak steady-state thermal stress (adjacent to the coolant), but the additive effects were thought to be significant. The thermal shock stress on the inside layer is $\sim 1/10$ of the steady-state stress. For this analysis the shock stresses are assumed to consume a fraction of the fatigue life equal to $N/N_0$, where $N$ is the number of cycles endured, and $N_0$ is the number of cycles to failure at 0.1 times the peak steady-state thermal stress. For all materials considered, the shock stress fraction of fatigue life was found to be negligible.

**Temperature Limits**

Though fatigue life and sputtering are primary constraints, the temperature restrictions posed by materials characteristics form a secondary design limitation. The melting/sublimation temperature limit is occurs on the heated surface at the outlet end of the coolant flow, assuming a uniform axial heat flux.

$$T_{\text{melt}} - T_{\text{in}} \geq \frac{q''(r + t)}{k} \ln(1 + t/r) + q''/h + \left(\frac{r + t}{r}\right)2Lq''/\pi Gc_{\rho \tau}, \quad (12)$$

where $2Lq''(r + t)/\pi Gc_{\rho \tau} = \Delta T_{1-2}$, the coolant temperature rise; $q''/h = \Delta T_{\text{film}}$, the film temperature rise, and $\frac{q''(r + t)}{k} \ln(1 + t/r) = \Delta T_{\text{wall}}$, the temperature difference through the channel wall.

Most materials display a distinct degradation in strength above a critical temperature, which may pose a design limit if the material supports a structural load. The limiting equation then would
be similar to that above with $T_{mel}t$ replaced by $T_{mech}$. For the divertor application adequate strength for coolant containment is provided by the inner cooler layers of material. Also, for maximum coolant temperature permissible to avoid excessive corrosion, $T_{corr}$, is

$$T_{corr} - T_{in} \geq \frac{q''}{h} + \left(\frac{r + t}{r}\right)2Lq''/\pi Gc_p r = \Delta T_{film} + \Delta T_{1-2}$$  \hspace{1cm} (13)$$

These temperature limits are also shown in Figs. 3a and 3b. The pairs of curves for melting and property degradation temperature limits represent the range of these limits resulting from changes in the heat transfer coefficient due to varied mass flux of coolant. If fatigue cracking and property degradation can be accepted, (as in a protective tile bearing no loads), points below the melt limit would be acceptable.

5.3.5 Evaluation of Design Options

A wide variety of solid target configurations are possible. The range of geometries depicted in Fig. 4 has been considered in this investigation. Each may be evaluated by applying minor modifications to the design equations already discussed.

Particle Load versus Lifetime Tradeoff

Sputtering and fatigue pose conflicting constraints on the allowable material thickness for any configuration. The sputtering erosion curves and the fatigue limit curves can be transferred to cycle life versus wall thickness axes for various heat fluxes, as illustrated by Fig. 5. Design conditions must lie on or below both the sputtering and fatigue curves for a specified heat flux. The intersection of the fatigue and sputtering lines affords the greatest life. Plotting these optimum life points results in the design curves represented by the solid lines of Fig. 6. The behavior of material optimum life and incident heat flux is apparent, and a quick determination of target capabilities under a given heat flux may be made.

In Fig. 5, the minimum thickness required to contain 1000 psi coolant in a 1 cm inner diameter tube is the origin of all sputtering lines. For a protective tile, not required to contain pressure, the sputtering lines would pass through the origin but have the same slope as in Fig. 5. This improves the optimum life curves slightly, as shown by the dotted curves on Fig. 6, yielding the optimum material performance limits of Fig. 7. Clearly, the goal of one year's survival under a 1 kW/cm$^2$ heat flux is not met by any of the materials. For this goal, the particle delivered heat flux would have to be reduced to 0.25 – 0.30 kW/cm$^2$, perhaps by sloping the target or expanding the magnetic field lines.
Tube Design – Single Material

Thermo-hydraulic design limits are first specified. Clearest visualization of the hydraulic design window is afforded from a plot of constraints on a coolant temperature rise, $\Delta T_{1-2}$, versus channel radius, $r$, set of axes as plotted for base case conditions in Fig. 8.

Thermo-mechanical limits for each material are next established;

1. The minimum wall thickness required to contain internal pressure.
2. For incremental values of the tube radius, $r$, the thickness corresponding to optimum life for the specified heat load is derived from the fatigue-sputtering. (The goal of one year survival must here be reduced to a quest for longest possible life.) Increasing the tube radius raises the thickness required to resist hoop stress, and shortens the optimum life.
3. To facilitate manufacture and avoid the possibilities of bubble blockage in the tube, a minimum tube radius of 0.2 cm is specified.
4. A check is made of the temperature limits at the tube exit, an upper bound on $\Delta T_{1-2}$.

Boundaries constitute the simple tube design window as shown in Fig. 8. Regions within the window are acceptable from a hydraulic and mechanical standpoint, though tube life decreases with increasing radius.

Plate Design – Single Material

Assuming that plates are restrained from bowing but can expand in directions parallel to their surface, essentially the same analysis can be performed for the flat plate as was used for the tube. In this configuration, the spacing between channels is independent of the thickness facing the plasma. The channel spacing may be selected to optimize the conduction to the rear of the channel, reducing pumping power requirements per unit area of target. Since sputtering and fatigue will affect the plasma facing wall in the same way as the tube, the mechanical temperature and radius limits will be identical to the tube case. The design window would appear as in Fig. 8.
Coated Tube Analysis

The concept of adjacent tubes was seen to be less desirable than circular channels in a flat plate, since the thickness of tube walls to resist sputtering was linked to the spacing between coolant channels. Very thick tubes would result in an increased heat load per channel. However, if sputtering resistance could be provided by an appropriate coating on the plasma side of a tube array, the heat load per tube could be fixed, and a coated tube array would be competitive with flat plate concepts by virtue of its production simplicity.

Thermohydraulic analysis is identical to that of the simple tube case. The minimum wall thickness for the tube substrate is determined, as before, from the hoop stress consideration. The coating thickness must be given particular attention, in order that it neither crumbles due to fatigue nor is penetrated by sputtering. Norem and Bowers [18] report that a thickness of 10 microns of beryllium should be sufficient to accommodate incident ions without sputtering from the substrate. They report that such a thin coating would be redeposited every 20 pulses, assuming adequate coating material is artificially introduced into the plasma edge. Such a thin coating should not be subject to thermal stress, since its surface would be rough on a scale of Angstroms [18]. The development of this sort of in situ plasma recoating of the target surface would virtually eliminate the sputtering limits, making the coated tube option more attractive.

Armored Plate Design

Thermohydraulic analysis of the armored plate would be identical to that of the simple flat plate. Thermo-mechanically, procedures would parallel the simple case, except that surface stresses arising at the interface between the two materials must be accounted for. If the armored tile is bonded to the substrate, the plates are unrestrained, and under a steady heat load. The neutral plane through the plate will have displaced from its unheated position due to thermal expansion. For equilibrium we can set the sum of forces on the plate equal to zero and solve for the displacement.

This displacement may then be used to determine the strains and stresses. The peak strains can then be compared with the maximum allowable strain range for the specified material and design life. Conversely, the maximum allowable strain may be used to determine the maximum allowable heat flux for a specified target thickness. In this way, fatigue curves on a life versus thickness axis may be generated, the sputtering rate plotted, and optimum life versus thickness curves obtained.

Analysis of six types of tiles (Aluminum, TZM, Beryllium, graphite, Titanium, and Vanadium) over a copper substrate were analyzed in this way. In all cases except aluminum, the tensile virtual strains in the tile were reduced over the unbonded plate. The compressive strains increased, but
occurred in the first layers of surface to be eroded by sputtering. A modest improvement in the tile fatigue life should result from this strain reduction. The relatively high expansion coefficient of copper tends to mitigate the back side tensile strain of all candidate tiles except aluminum, reducing cyclic fatigue at the back of the tile. With remote replacement of tiles, the substrate and coolant channels would easily survive past the one year design goal. The relative ranking of material survival would be unchanged from Fig. 7, though life would be increased slightly.

Mechanically Unbonded Layered Plate Design

The foregoing analysis has revealed serious limitations on the lifetime and heat load constraints under which solid target configurations may operate. An upper limit on the survivability of a solid target to survive may be evaluated by assuming a design which takes full advantage of material temperature, sputter rate, and fatigue thickness constraints simultaneously. This is achieved by assembling relatively thin layers of armor material so that mechanical bonding between them is non-existent, while thermal bonding remains very good. Here a thin film of liquid lithium is postulated to separate the layers. Powdered graphite may be an acceptable alternative. Each layer is thin enough to prevent fatigue failure during its survival time in the target. In this way successively thicker layers can be built upon the cooled backing, up to a thickness at which a temperature limit is approached. For most materials considered, this limit is the boiling point of lithium. The thickness of armor material thus assembled resists sputtering for the longest time possible for any target configuration. The obvious price paid to reach this optimum life design is an increase in the complexity and difficulty in fabrication of the target.

Figure 9 indicates the analysis of this optimum case. TZM affords the longest life at a 1 kW/cm² heat load; 261 days. Niobium (D-43) appears to be nearly as attractive at 258 days, perhaps more so since but four niobium layers are required as opposed to TZM's nine layers.

If an ideal lubricant between the layers \((k = \infty, T_{melt} = \infty)\) were available, the total wall thickness could be increased further, up to a thickness at which the outer surface approaches the material melting point. This would improve the target lifetimes by roughly a factor of 2 over the values of Fig. 9.
5.3.6 Sensitivity Analysis

A systematic analysis of the effect of varying key parameters and physical assumption is needed to determine the divertor target behavior over a broader range of conditions. This analysis can not only expand the design options, but furthermore, it can affect uncertainty in the applicability of presently known physical behavior of coolants and structural solids at the extreme conditions anticipated. The following lists the variables to be analyzed and their effect on the design:

A. Extension of reactor pulse length would ease the material fatigue problem due to thermal stress. An order of magnitude reduction in cycles per years provides about 80% increase in niobium target lifetime. Improvements in other materials were of the same order.

B. Uncertainty in sputtering coefficients results in major uncertainties in damage analysis. A four-fold reduction is sputtering rate increases lifetime by about 2.5 times.

C. Reducing the high safety factor (due to uncertainty in subcooled boiling pressure drop) on the CHF correlations from 1.3 to 1.1 reduces mass flux by 30%.

D. Increasing the inlet temperature or dropping the inlet pressure decreases the exit subcooling. Operation at an inlet temperature as close to freezing as possible widens design window and allows operation at lowest possible mass flux.

E. Increased tube length reduces exit subcooling, which requires higher mass flux to avoid CHF. Permissible operating region shrinks as length is increased.

F. CHF and pumping power produce stringent limits on maximum heat flux. For example, 4 kW/cm² operation results in a pumping power ratio of 60%.

5.3.7 Conclusions

The conclusions of this investigation of solid divertor target options are:

1. A hydraulic system using non-boiling highly subcooled water appears feasible to cool surface heat loads of 1 kW/cm² in the divertor target application. Intermediate pressures (200 to 500 psia) are required to provide adequate subcooling to avoid nucleate boiling. Channel lengths from 10 cm to 100 cm may be used.

2. Provision for handling hot spots and off-normal conditions with heat loads up to 2 kW/cm² may be made by operating the system with very high mass flux and high inlet subcooling. The pumping power to heat transfer ratio can be kept at 2%. Careful design will be necessary to avoid internal cavitation erosion.
3. The Saha-Zuber equation for "net vapor generation" compares favorably with existing CHF correlations in the range of variables pertinent to divertor applications. It provides a theoretical basis for predicting CHF and should prove valuable in extending CHF prediction beyond the ranges of existing data.

4. At high mass flux, the onset of fully developed nucleate boiling will increase the total pressure drop over the non-boiling case two to six times. This can be accommodated in channels of diameter greater than 4 mm and at pressures of 500 psia with less than a 10% overall pressure drop. The likelihood of a pressure drop-flow rate instability in a bank of flow channels, however, remains an obstacle to operation in its regime.

5. The cyclic thermal loading and ion sputtering conditions currently envisioned at the divertor target surface limit the life of conventional tube or plate targets, for the eight candidate materials studied, to several months. Molybdenum Alloy (TZM), and Niobium alloy (D-43) appear most promising from a materials standpoint, but may well quench the plasma unless ions sputtered from the target are effectively prevented from penetrating the plasma core. Beryllium appears most favorable among the low Z candidates, but can survive only one month without redeposition.

6. Development of a mechanically unbonded, thermally bonded laminated design would allow target life to be extended to nine months.

7. A copper substrate protected from sputtering could be designed to survive the cyclic loading for well over one year.

REFERENCES


Figure 1: Comparison of CHF Correlations for Base Case Conditions

Figure 2: Hydraulic Option Space and CHF Correlations for Base Case Conditions
Figure 3a: Fatigue, Property Degradation, and Melting Limit Curves for TZM (Area Below Curves Acceptable)

Figure 3b: Fatigue, Property Degradation, and Melting Limit Curves for ZAC-2 Copper Alloy (Area Below Curves Acceptable)
Figure 4: Conceptual Solid Target Configurations

A. Adjacent Tubes
B. Spaced Tubes
C. Plate with Milled Channels
D. Consumable Target
E. Protective Tubes Sheathing Substrate Tubes (Bonded and Unbonded)
F. Protective Coating Bonded to Plates
G. Protective Tile over Substrate Plate (Bonded and Unbonded)
H. Thermally Bonded, Mechanically Unbonded Tiles over Plates

Figure 5: Sputtering and Fatigue Curves Plotted for Various Heat Fluxes for TZM

TZM Molybdenum Alloy

Graph shows life (cycles) on the y-axis and thickness (mm) on the x-axis. Key parameters include:
- Fatigue Limits
- Sputtering Limits
- p = 1000 psia
- r = 0.5 cm
- 1 yr = 10^5 cycles
Figure 6: Optimum Life and Thickness Curves for Candidate Materials Showing Effect of Varied Heat Flux
<table>
<thead>
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<th>Sputter Rate (mm/yr)</th>
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Figure 7: Optimum performance capabilities for candidate materials, configured as single flat tile constrained against bowing, and assuming no interfacial stress at joint with substrate.

Figure 8: Design Window for Niobium (D-43) Plates and Tubes, Showing Relation between Life and Radius. (Temperature Limits not Significant at High Subcooling)
Figure 9: Mechanically Unbonded Layered Plate Design - Comparison of Material Performance and Component Lifetimes

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5.50
5.4 MATTED FIBER TARGETS FOR SPUTTER RESISTANCE

5.4.1 Introduction

The customary approach to reducing sputter erosion is to look for sputter-resistant materials. Here we consider using a matted fiber surface to intercept the plasma – i.e., optimizing the geometry to reduce the net outflow of sputtered atoms. The fundamental assumption is that the sputtered atoms will redeposit if they strike a surface again shortly after the primary sputtering event. Thus we try to design a surface that maximizes the probability of a sputtered atom striking another facet of the surface rather than escaping back into the plasma.

Ziegler et al. [1] proposed altering the surface microtopography by depositing a dense array of single crystal whiskers. They anticipated a reduction in sputtering because: (1) most sputtered atoms would be released down into the whiskers; (2) orientation of the crystal axes could promote channeling rather than a sputtering cascade; and (3) preferential sputtering at grain boundaries would be reduced. Their experimental results with tungsten whiskers (up to 80 μm high) in 4 keV He$^+$ discharges showed a reduction in net sputtering yield by a factor of 3 to 100.

Cramer and Oblow [2,3] considered altering the surface microtopography by using a honeycomb vacuum wall. Sputtered atoms produced inside the honeycomb cells were presumed likely to strike the cell walls and redeposit. They ran Monte Carlo codes to determine net sputtering yield as a function of depth/diameter, incident ion distribution, reflection probabilities and reflection distributions. A general reduction by a factor of 2 to 4 was found.

In this paper, we consider the potential sputtering reduction from a matted fiber target. If there is a sputtering reduction, then this scheme could be mechanically simpler to construct or replace than honeycomb walls or microwhiskers – one can even imagine a roll which is slowly unraveled across the divertor target surface.

5.4.2 Sputter Resistance

Consider a fiber volume fraction $\alpha$. If the average fiber spacing is $p$, then over a distance $p$ the probability of interacting is roughly $A_f/A$ where $A_f$ is the normal surface area covered by fibers and $A$ is the total exposed normal area. Since $\alpha \sim (A_f/p)/A$, we can approximate the ion attenuation (for many incident ions) as a continuous curve with a characteristic mean free path $\lambda \sim p/\alpha$. But

$$\alpha \sim \frac{\pi d^2/4}{(\pi p^2/4)} = (d/p)^2$$

(1)
where \( d \) is the fiber diameter. So \( \lambda \sim d/\alpha^{3/2} \) and the ion flux is, where \( x \) is distance into target,

\[
\phi(x) \sim \phi_0 e^{-x/\lambda}
\]

(2)

This should be a reasonable approximation for \( \lambda \gg p (\alpha \ll 1) \).

If the sputtering coefficient for plasma ions on the solid material is \( S_0 \) (atoms/ion), then the gross sputtering rate per unit normal area is, where \( t \) is the matted fiber target thickness,

\[
dR(x) = S_0 |d\phi| = S_0 \phi_0 e^{-x/\lambda} dx/\lambda
\]

implying

\[
R = \int_0^t \frac{S_0 \phi_0 e^{-x/\lambda}}{\lambda} dx
\]

(3)

If all these sputtered atoms escaped from the target, then the net sputtering coefficient is

\[
S_{net} = \frac{R}{\phi_0} = S_0 \int_0^t \frac{e^{-x/\lambda}}{\lambda} dx
\]

(4)

In practice, \( t \) will be sufficiently large to stop all ions, so we take \( t \rightarrow \infty \) with small error. Then we obtain \( S_{net} = S_0 \), which is the result for a flat solid surface.

Now consider the possibility that atoms sputtered at a depth \( x \) may strike a surface on their way back out and redeposit. The incident ion is taken as having an angle \( \theta \) with the surface normal vector and a normalized incident distribution \( f(\theta) \). The sputtered atoms come off at an angle \( \phi \) with the surface and have a normalized emission distribution \( G(\phi) \).

So

\[
\int_0^{\pi/2} f(\theta) d\theta = 1
\]

(5)

and

\[
\int_0^\pi g(\phi) d\phi = 1
\]

(6)

These sputtered atoms have a path length \( y = x/\cos(\theta + \phi - \pi/2) \) back to the surface. They will attenuate along this distance according to \( e^{-y/\lambda} \), assuming redeposition when they contact another fiber. Since the incident plasma ion energies are around 1 keV, the sputtered atoms should be much less energetic. Experimental data on energy spectra of sputtered particles is summarized by Kraus and Wright [4]. Self-sputtering yields scale as \( S_{self} \) (atoms/ion) \( \sim 0.001 \) E (eV) for energies below about 1 keV [5], so \( S_{self} < 0.01 \) atoms/ion for \( E < 10 \) eV. Hence we expect almost complete redeposition from the sputtered atoms inside the target.

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For practical targets, the area will be much larger than $\lambda$ so the mat can be considered infinite in the horizontal dimensions. Then the net sputtering coefficient is

$$S_{\text{net}} \sim \frac{1}{\phi_0} \int_{\theta}^{\infty} w(x) dR(x)$$

(7)

where

$$w(x) = \int_{0}^{\pi/2} f(\theta) S'(\theta) \int_{0}^{\pi - \theta} g(\phi) e^{-x/\lambda \cos(\theta + \phi - \pi/2)} d\phi d\theta$$

This $w(x)$ weights the sputtering rate to account for redeposition, and $S'(\theta) = S_{\text{actual}}/S_0$ allows for variation in sputtering yield with incident angle. Note that sputtered atoms travelling away from the surface ($\theta + \phi > \pi$) are assumed unlikely to reach the surface since they would redeposit rather than reflect from deeper fibers.

We can determine $S_{\text{net}}$ for a variety of incident and sputtered distributions. In general, $f(\theta)$ should be constant – i.e., $f(\theta) = 2/\pi$. This is true, for example, with a random arrangement of fibers, or if the fiber surface is rough. $S'(\theta)$ can be estimated as $S'(\theta) \sim 1/\cos \theta$, $\theta < 80^\circ$. For $\theta < 80^\circ$, the incident ions are simply reflected. (Note that $80^\circ$ is only approximate, and increases with ion energy.) The largest uncertainty is in $g(\phi)$, the emission angular distribution. Three possibilities are considered here – peaking at particular angle, roughly away from the incident ion’s path, and random.

Consider the simplest possibility first – emission at a particular angle $\phi$. Some data [5] suggests that $g(\phi)$ is peaked at about $30^\circ$ from the surface normal vector. We model this possibility as $g(\phi) \sim \delta(2\pi/3)$ where $\delta(\phi)$ is the delta function. Then

$$w(x) = \int_{0}^{\pi/3} \frac{2}{\pi} e^{-x/\lambda \cos(\theta + \pi/6)} \frac{\cos \theta}{d\theta}$$

(8)

where the upper limit on $\theta$ is now $\pi/3$, consistent with the $\theta + \phi < \pi$ requirement. Then

$$S_{\text{net}} = \int_{0}^{\infty} \frac{S_0}{\lambda} e^{-x/\lambda} \frac{2}{\pi} \int_{0}^{\pi/3} e^{-x/\lambda \cos(\theta + \pi/6)} \frac{\cos \theta}{d\theta} d\theta dx = 0.23 S_0$$

(9)

This result is independent of $\lambda$ because $t \gg \lambda$ assumed.

And if the assumption that $g(\phi) \sim \delta(2\pi/3)$ is too unrealistic, consider the alternate case of isotropic emission over $\phi = \pi/2$ to $\pi$, $g(\theta) \sim 2/\pi$. Then

$$w(x) = \frac{2}{\pi} \int_{0}^{\theta_{\text{max}}} \int_{\pi/2}^{\pi} \frac{2}{\pi} e^{-x/\lambda \cos(\theta + \phi - \pi/2)} \frac{\cos \theta}{d\theta} d\theta d\phi$$

(10)

Here $\theta_{\text{max}}$ is limited to less than $90^\circ$ since, at some energy-dependent angle, the incident ions simply reflect and do not sputter. For hydrogenic ions around 1 keV, $\theta_{\text{max}} \sim 80^\circ$ or $(8/9)(\pi/2)$.  

5.53
These reflected ions are still travelling deeper into the mat and can still give rise to sputtered atoms. We model these by increasing the effective penetration mean free path $\lambda$ by a factor $9/8$. So

$$S_{net} = \frac{4S_o}{\pi^2 \lambda} \int_0^\infty \int_{\pi/2}^{\pi-\theta} \int_{\pi/2}^{\pi-\theta} e^{-\left(\pi/\lambda\right)^9 / \theta} \cos(\theta + \phi - \pi/2) \cos \theta d\theta d\phi dz = 0.21S_o$$ (11)

A third possibility is $g(\phi) \sim 1/\pi$, random (isotropic) emission over $\phi = 0$ to $\pi$. This assumes that the sputtered atom's direction is completely independent of the incident ion. The solution is similar to Eq. (11), and the result is $S_{net} = 0.40S_o$.

All these calculations suggest $S_{net} \sim 0.2$ to $0.4S_o$. This agrees with Cramer and Oblow who obtained $0.2$ to $0.5$ $S_o$ (over a wide range of incident, reflected and sputtered atom distributions) for their honeycomb wall design with depth/diameter greater than unity.

The conclusion from these simple calculations is that changes in surface shape such as a matted fiber target can reduce net sputtering yield by a factor of 2 to 5, reasonably independent of the exact distribution of the incident, reflected and sputtered atoms.

5.4.3 Heat Transfer Across a Fiber Mat

While a matted fiber target may be a simple passive scheme to get reductions of up to 80% in sputtering yield, it must be able to handle high energy fluxes to be useful in divertor applications. In principle, the fibers can be designed sufficiently free to allow thermal expansion and thus be insensitive to fatigue, yet sufficiently intertwined to provide a reasonably short path along the fibers to the cooled substrate. And since the fibers need carry little mechanical load, the limit is vaporization of the hottest fibers.

Treat the mat as having an effective thermal conductivity, $k_{eff} \sim ak_o$, where $k_o$ is the thermal conductivity of the solid material and $a$ accounts for the internal voids. Experimental results in the limit of large fiber volume fraction ($a \sim 0.7 - 1.0$) indicate this approximation is optimistic by up to a factor of 2 [6,7], but it should give reasonable scaling.

In steady-state with an internal exponential heat source, the temperature difference across the mat is slightly less than if the heat is all deposited at the surface, so $T_{fo} - T_{fi} \sim q''t/k_{eff}$ where $T_{fo}$ and $T_{fi}$ are the average outer and inner fiber temperatures, and $q''$ is the incident energy flux.

We have assumed $t \gg \lambda$ so all ions are stopped in the mat, but heat transfer dictates $t$ be as small as possible. We take $t \sim 10\lambda$ as a compromise. But $\lambda \sim d/\alpha^{3/2}$ so

$$T_{fo} - T_{fi} \sim \frac{10d q''}{\alpha^{3/2}k_o}$$ (12)
Since heat is conducted along the fibers, the hottest point is actually the midpoint of exposed fibers on the mat outer surface. The temperature rise over a length $L$ of a thin fiber under a uniform heat load $q'(W/m)$ is

$$\Delta T_f \sim \frac{2q'L^2}{\pi d^2 k_o}$$

(13)

Depending on the fiber orientation, $q' \leq q''d$. To estimate $L$, consider the intertwined fibers as approximately bounding "cubes" of $(2L)^3$ volume. Thus the hottest point is a distance $L$ from each corner. From the definition of fiber volume fraction, and since each fiber is shared by four cubes, $\alpha \sim 0.6d^2/L^2$.

Since the hottest fiber point is at the mat temperature rise Eq. (12) plus the exposed fiber temperature rise Eq. (13),

$$T_{f_o,\text{max}} - T_f \sim \frac{q'd}{ak_o} \left[ \frac{10}{\alpha^{3/2}} + 0.4 \right]$$

(14)

Some representative results are given in Table 1. These show that reasonable temperatures are possible for $d < 1$ mm, $\alpha \sim 0.25$ to 0.50, and $k_o > 100$ W/m-K, even at $q'' = 10$ MW/m². We emphasize that these conclusions are based on simple analysis, and do not include an accurate fiber mat thermal conductivity or on the other hand, for example, possible convective and radiative heat transfer across neutral gas within the fiber volume.

If the temperatures are still too high, a second approach is to use overlaid wire screens as opposed to random fibers. Again we produce a porous surface for the incident ions, but the space between the fibers, and so the conduction heat path, is decreased -= i.e., $\lambda \sim \Lambda_f/A$ still, but now $\lambda \sim d/\alpha$. This reduces the temperature drop across the mat by $\alpha^{1/2}$, or more than 30% for a $\alpha < 0.5$.

5.4.4 Conclusions

The matted fiber divertor target concept has been shown by simple analysis to result in a decrease in net sputtering yield by up to a factor of 5 over a flat surface. Heat transfer by conduction alone may allow reasonable temperatures even at 10 MW/m² in materials like molybdenum, tungsten or copper, but there is considerable uncertainty in the effective heat transfer across the fiber mat.

REFERENCES


Representative temperature rises across matted fiber targets at $q'' = 10 \text{ MW/m}^2$

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<th>Material</th>
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<td>d = 0.2 mm</td>
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6.0 LIMITER DESIGN

6.1 DESIGN OF ACTIVELY COOLED LIMITER FOR ALCATOR A AND C

6.1.1 Introduction

Solid, passively cooled molybdenum limiters have been used in Alcator A and C to intercept charged particles diffusing from the plasma. For longer discharge times, limiter surface melting and the associated high thermal gradient can result in severe limiter damage and possible subsequent plasma chamber damage. Changes in thickness and/or material for the passively cooled limiters were considered unlikely to permit longer discharge times. Active cooling of the limiters was therefore examined. Preliminary thermal and stress analyses indicated the best approach was to use a relatively thin skin as the plasma-facing component, and to actively cool to minimize skin temperatures and thermal stresses. Candidate skin materials (Molybdenum, OFHC copper, and a tantalum alloy (Ta-10W)) were evaluated from the standpoints of erosion, vaporization (from runaway electrons), heat flux capability, and fabricability.

The study focused on two conceptual designs, adaptable to Alcator C geometry, which used the thin skin approach. The cross section profile of the plasma-facing skin for both concepts is an arc segment, as shown in Fig. 1. The relatively large width-to-depth ratio of the segments acts to reduce the heat flux by spreading it over a surface area much larger than the area normal to the particle flux. The two concepts differ primarily in the skin cooling method. In Concept 1, coolant flows through small-diameter tubes spaced close together and brazed to the skin's back face. In Concept 2, coolant flows along the skin back face through channels formed by ribs machined into a core block brazed to the skin.

The primary objectives in these two approaches are to achieve a low thermal gradient through the material between the coolant and the plasma-facing surface, to keep the skin surface temperature well below its melting point, and to minimize induced thermal strains in the assembly. In practical designs, achieving these objectives requires (1) a relatively thin skin made of material having a high thermal conductivity, and (2) a low temperature coolant having good thermal-hydraulic characteristics and a relatively high heat capacity and thermal conductivity.

Thermal analyses of both concepts were conducted using the McDonnell Douglas HEATRAN code. The results were used as input to the McDonnell Douglas PLATSA code, which was used to perform elastic-plastic stress analyses of the concepts. The results demonstrated the feasibility of the approach. Further evaluation led to a recommended design, similar to Concept 2 but with
two important improvements. Details of the analyses, evaluations, and the recommended design are presented in Ref. 1.

6.1.2 Materials Evaluation

Candidate materials for the plasma-facing skin were molybdenum, OFHC copper, and Ta-10W tantalum alloy. Molybdenum and copper were evaluated for the coolant channels and structure. Skin thicknesses examined were 1 to 2 mm. Tantalum alloy skins had peak temperatures much higher than for molybdenum or copper. Although copper had the lowest peak temperatures, it undergoes significantly more erosion than molybdenum. Peak temperatures for molybdenum were only 200°C above those for copper, and were well within its operating temperature limits. The use of molybdenum skins would also provide a more direct operating performance comparison to the present solid molybdenum Alcator limiters. Molybdenum skins were therefore selected for both concepts. Copper was chosen as the coolant-carrying material (tubes or ribs) and the structural material because of its superior thermal conductivity and its fabricability in brazed Mo/Cu assemblies.

Water was selected as the limiter coolant. It is the logical coolant choice, assuming that the consequences of a coolant spill (in limited quantities) can be accepted for the test device in which the limiter is installed.

6.1.3 Thermal-Hydraulic Analysis

Thermal-hydraulic analyses of the two limiter design concepts were conducted using MDAC-STL's general heat transfer code, HEATRAN. This code employs the finite difference method for direct or iterative solutions, and is very accurate for situations involving the sudden application of very high heat fluxes, such as occur on limiter surfaces during a plasma discharge.

The equation governing heat flux due to particle heating was assumed to be

\[
(Q/A) = (Q/A)_0 e^{(-x/1.0)}
\]

where

- \(Q/A\) = heat flux normal to particle path
- \((Q/A)_0\) = peak heat flux = 5000 W/cm²
- \(X\) = distance from scrapeoff zone edge, cm

The peak incident heat flux is 1700 W/cm², which occurs \(\sim 35°\) away from the cross section vertical centerline, measured along the skin surface.
To obtain temperature distributions to be used for subsequent stress analysis, a transient analysis was performed for the thermal model constructed for each concept. Initial temperature of all model nodes was 50°C, the assumed coolant inlet temperature. The peak incident heat flux value of 1700 W/cm² was applied uniformly along the skin surface from 0 to 0.5 s. At 0.5 s the heat flux value was changed to zero, and the transient continued for another 0.5 s while the coolant flow continued. Assumed geometry and thermal-hydraulic parameters listed in Table 1.

The peak temperature history for the 2 mm Mo skin in Concept 1 (tubes) during the discharge/cooldown transient is shown in Fig. 2. The slope of the temperature curve for the heated side indicates that nearly steady state conditions are attained by the end of the 0.5 s discharge. In fact, the peak temperature achieved in the transient, 545°C, is only 8°C less than the peak value at steady state. This temperature difference would be —0°C for 1 mm Mo skins, for all Cu skins, and for all Concept 2 (integral) skin thickness/material combinations.

6.1.4 Stress Analysis

Thermal stress analysis was performed by applying end-of-discharge and post-cooldown temperature distributions to two-dimensional finite element models of parts of the limiter cross sections, then analyzing the models using the PLATSA code to obtain elastic and plastic strains and stresses. PLATSA is a two-dimensional finite element code that utilizes the actual nonlinear temperature dependent stress-strain behavior and thermal expansion coefficients.

The center rib, which acts as a "backbone" for the limiter, was included in the models to account for its effect in restraining thermal expansion of the skin and coolant channels. Center rib temperature was assumed constant and equal to the coolant inlet temperature. The skin was represented depthwise by four elements. An average temperature was determined for each skin element by plotting the temperature distributions and picking the temperature value from the resulting curve (nearly linear in most cases) for each element midpoint. The same procedure was followed in determining average temperatures for each element in the coolant tube of coolant channel rib region. The model was assumed to represent a beam of finite length, with each beam element maintained at a constant temperature over its length.

The stress analysis results, summarized in Table 2, indicate that either of the concepts examined will be acceptable from the standpoints of peak stress and cyclic fatigue. Results for the Concept 1 (tube type) design with a 2 mm molybdenum skin brazed to copper tubes for the peak heat flux region during the initial discharge cycle at \( t = 0.5 \) s (end of discharge), are shown in Fig. 3. The portion of the copper tube nearer the molybdenum skin back face — approximately the first 60° of tube circumference measured from the skin/tube tangency point — incurs some plastic strain during the initial discharge applied to the limiter. The mechanical strain in this region of the tube occurs
primarily because the thermal conductivity of copper is three times that of molybdenum. Expansion of the hot skin is restrained by the cool center rib, and expansion of the tube near the skin is in turn restrained by the skin. This region of the tube thus goes into compression. The mechanical strains on the four tube elements nearest the skin back face are the largest occurring anywhere on the model cross section; these elements are also the "weakest" of any in the cross section in terms of the actual proportional strain limit value for an element at its particular temperature. The occurrence of plastic strain on these elements during the initial discharge is thus the result of the combination of high mechanical strains and low proportional strain limit values.

Stresses and strains on each element after cooldown to a uniform 24°C (room temperature were also calculated using strain values at the end of the initial discharge as the starting point. Only three copper tube elements still have plastic strain after initial cooldown, which becomes tensile for this condition. The mechanical strains result from accommodation of the plastic strains which have occurred during the full discharge/cooldown cycle.

The plastic strains incurred by part of the copper tube on the initial discharge/cooldown cycle do not represent a fatigue problem. The brazing process necessary to fabricate the limiter produces a fully annealed condition in the OFHC copper tubes. The annealed material then exhibits cyclic strain hardening behavior when subjected to applied strains beyond the initial elastic limit. Under repeated cyclic strains (i.e., repeated discharge/cooldown cycles after the initial cycle), the material strain-hardens so that the cycle-to-cycle increments of maximum stress range gradually reduce to near-zero values. At this point, the action of the material is virtually elastic. The initial maximum stress range of the element of the copper tube undergoing the largest plastic strain is 131 MPa (19 ksi), 0.23% strain range) which will increase to some higher value as the material strain hardens during subsequent cycling. However, examination of the fatigue data for OFHC copper indicates that extrapolation of the data downward to the 0.23% maximum strain range for the copper tubes in the limiter would results in a fatigue life of 10,000 cycles or higher. All other regions exhibit fully elastic action, with much higher fatigue lives.

It should be pointed out that a fatigue failure of the copper tube in the local peak heat flux region would very probably not result in failure (leaking) of the limiter. Once the flaw reached the outer tube surface, the braze material bonded to the tube surface – which fills the cusp-like cavity formed by adjacent tubes and the skin back face – would form a barrier to further crack growth. An analogous situation occurs in those Concept 2 (ribbed) designs in which plastic strains occur in the copper ribs. Flaw growth through a rib would be of no significance since the only consequence would be a slight channel-to-channel leakage of coolant.
6.1.5  Recommended Limiter Design Concept

Although Concept 1 and 2 were shown by thermal and stress analyses to be acceptable, evaluation from the fabricability and mechanical design standpoints revealed a number of relative disadvantages, some of which are listed in Table 3. To eliminate these disadvantages, an improved design was created. This limiter design, shown in Fig. 4, has a cross section similar to that of Concept 2 except for two important improvements: (1) the coolant return tube and center rib are replaced by a machined solid copper block; (2) the one-piece Mo skin is replaced by a two-layer skin, composed of 1 mm thick Mo skin segments overlaying a 1 mm thick one-piece copper skin.

Use of the one-piece solid copper block eliminates the separate center rib and coolant return tube. The flow channels and coolant return passage are machined full-depth into the surfaces of the block. Use of a single-piece copper skin to form the pressure boundary for the flow channels (in conjunction with the machined block) improves the design in several ways as compared to a one-piece Mo skin (which would be very difficult to fabricate). The 1 mm thick one-piece copper skin can easily be spin-formed over a die block, with the edges subsequently trimmed to the contour of the machined copper block. The molybdenum plasma-facing surface is provided by skin segments fabricated from 1 mm Mo sheet, each of which extend over the full curved surface length in the limiter cross section and conform to that curved contour. Their width in the poloidal direction would be determined during detail design based on the maximum allowable gap (in the poloidal plane) between the segment straight back face and the curved surface of the copper skin. Peak temperatures for the Mo skin segments are expected to be even lower than those predicted for the 2 mm Mo skin for Concept 1.

6.1.6  Conclusions

1. All combinations of skin material and skin thickness analyzed for both Concept 1 and 2 reach virtually steady state temperatures by the end of the 0.5 s discharge.
2. The maximum skin temperatures are far below the melting point of either Mo or Cu.
3. Fatigue life of the most critical point in the limiter cross section (the region of peak incident heat flux) is conservatively predicted to be greater than 10,000 discharge/cooldown cycles.
4. From the standpoints of thermal-hydraulics, maximum temperatures, and maximum stresses, the two actively cooled limiter design concepts analyzed could enable Alcator operation at discharge times $> 0.5$ s.
5. The recommended limiter design concept is superior to Concept 1 and Concept 2 in terms of fabricability, cost, strength, and thermal-hydraulic performance.

REFERENCES


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*ASSUMES HEAT FLUX IS APPLIED UNTIL A STEADY STATE CONDITION IS ACHIEVED.

TABLE 1. ASSUMED GEOMETRY AND THERMAL-HYDRAULIC PARAMETERS.

- PLASTICITY (0.1%) OCCURS DURING INITIAL CYCLES FOLLOWED BY LOW CYCLIC PLASTICITY IN SUBSEQUENT CYCLES
  - OCCURS FOR SMALL REGION OF COPPER TUBE OR RIB WITH Mo SKIN
- INITIAL PLASTIC ACTION SHAKES DOWN TO ELASTIC ACTION AFTER SEVERAL DISCHARGE/COOLDOWN CYCLES, DUE TO STRAIN HARDENING OF COPPER
- FATIGUE LIFE PREDICTED TO BE > 10⁶ CYCLES

TABLE 2. SUMMARY OF STRESS ANALYSIS RESULTS.

TABLE 3. FABRICATION AND MECHANICAL DESIGN DISADVANTAGES FOR CONCEPTS 1 AND 2.
**CONCEPT I**

INTERCOSTAL SKIN 1.95

PARTICLE (4.95 CM)

FLUX - PLASMA (11.8 CM)

r, RM

COOLENT X C6 (1M)

COOLANT TRANSFER LINE

**CONCEPT 2**

COOLANT CHANNEL RIB

MACHINED BLOCK

DIMENSIONS IN INCHES

**FIGURE 1. CROSS SECTIONS OF ACTIVELY COOLED LIMITER DESIGN CONCEPTS.**

**FIGURE 2. TEMPERATURES AT END OF DISCHARGE ARE NEARLY STEADY-STATE.**

6.8
1700 W/m² PEAK HEAT REGION, PRIOR TO COOLDOWN

CONCEPT 1

510°C

THERMAL STRAIN

PLASTIC STRAIN ON INITIAL CYCLE

FINAL STRAIN (MOLY CENTER RIB)

FIGURE 3. THERMAL STRESS MODEL SOLUTION AT END OF DISCHARGE.

Mo SKIN SEGMENTS OVER SOLID Cu SKIN
MACHINED RIBS IN SOLID Cu BLOCK

CHAMBER LIMITER

REACTOR

SUPPORT

Mo SKIN SEGMENTED POLOIDALLY

FIGURE 4. RECOMMENDED LIMITER DESIGN.
6.2 ACTIVELY-COOLED LIMITERS FOR DOUBLET-III

6.2.1 Limiter Designs

The geometry selected is shown in Fig. 1 and consists of hemispherical heat absorbing surfaces cooled internally by impingement flow. These are then arranged in an array to accommodate the heat flux pattern. Figure 1 shows a close packed planar array which would provide a plane heat absorbing surface. An alternate arrangement with a linear array is shown in Fig. 2. This is patterned after the envelope geometry of the D-III primary limiter and also the pumped limiter studied for STARFIRE [1]. In this shaped linear array, the highest heat flux occurs on the spherical end caps. The tubular sections also absorb heat but at a significantly reduced level because of the nearly grazing incidence with the energy flow direction in the plasma boundary.

A wide variety of metals with and without coatings can be envisioned for test pieces as well as liquid or gas cooling. A composite limiter test piece showing an adaptation of present day passive limiter technology for active cooling and with our advanced coating is shown in Fig. 3. The POCO graphite with the C-SiC alloy coating has a fabrication technology that is well developed. Thermal pulse test results are given in Section 3 in connection with limiter application test studies. Braze alloys for graphites are available and iron-nickel alloys with thermal expansion coefficients close to graphite are also available. This fabrication should present no major difficulties.

A consistent set of design parameters are shown in Table 1 for water cooling. The film coefficient of heat transfer was obtained from Smirnov, et al., [3] and is based on measurements of jet impingement flow on a flat plate. The modifications for a spherical jet target should not be large. The dimensions and parameters here are within the ranges measured.

The stress in the POCO is well below the maximum allowable (40 MPa) and the surface temperature of 650°C is below the peak methane generation temperature of approximately 800°C. The inner metal lining and braze are primarily for sealing against coolant leakage and to provide thermal contact between the graphite and metal. These layers would be thin and thus have not been included in either the stress or temperature calculations. These results indicate that designs suitable for 5 MW/m² steady state can be achieved without exceeding any material limits.

6.2.2 Disruption Experiments

There have been some recent measurements of disruption of D-III [4] which have a significant impact on limiter design for fusion reactors. These measurements show that
1. The energy lost during a disruption is deposited on the region which was acting as the limiter just before the disruption.

2. The energy deposition time is about 250 $\mu$s, much shorter than the plasma current decay time. The plasma loses about 95% of its energy. The remaining 5% follows the plasma current decay time over a much longer time period.

3. About 50% of the plasma thermal energy goes to the limiter during the disruption. The remainder presumably goes to the first wall.

The significance in the first two is that limiters must be designed to accommodate a high steady state heat flux, typically in the range of 10 MW/m$^2$ and must be able to survive a superimposed high energy deposition short pulse disruptive heat load. The disruptive heat loads will do some damage to the limiter surface including melting, vaporization, and possible cracking. Special designs will be required to survive more than a few disruptions.

At present, neither the steady state nor the disruptive characteristics of limiter heat loads coupled with limiter response are well identified. A key feature of D-III limiter experiments is the ability to characterize both the interior and edge plasma with diagnostics currently installed. The diagnostics and instrumentation available include: a 21-channel bolometer, radial scan Thompson scattering, CO$_2$ interferometer, soft x-ray array and spectrometer, VUV spectrometer, laser fluorescence spectroscopy, charge exchange spectrometer, infrared temperature measuring camera and other more usual plasma monitoring equipment. In addition, solid state collectors in close proximity to the experimental test limiters could be used to determine the flux and energy of particles impacting the test piece. The recording and analysis of these instruments during a limiter test sample exposure is relatively routine, and would provide a nearly complete picture of the limiter environment coupled with measurements of the response of the limiter to this environment.

6.2.3 Test Plan For D-III

The limiter and divertor target in-vessel experiments in D-III may be classified into several categories, each with differing scheduling and cost requirements.

1. Performance monitoring of the normal in-vessel components from the technology and engineering point of view is an activity not necessarily included in the confinement program. This includes measurement of the parameters in the plasma and plasma boundary region to determine the in-vessel component environment as well as the response (such as temperature rise and erosion rate) of the in-vessel component. Monitoring can be done in-place during post-mortem examination following removal of the component. This class of experiments
requires mostly manpower for data collection and analysis with little requirement for extra materials or equipment.

2. The substitution of special experimental pieces for the normal design can be made with practically no perturbation of the normal operating schedule. This particularly includes the inboard armor tile array and the primary limiter. Each is composed of multiple sections of TiC coated graphite which could be replaced with other coatings or substrates for example. Performance monitoring in all phases of operation could also be included. These experiments require staffing, materials and fabrication costs. No extensive equipment or additional facilities are required.

3. The in-vessel mounting and exposure of small samples which can be readily inserted and replaced would be very useful: (1) in point wise measurements in the limiter shadow and plasma boundary region, (2) in testing small scale models of limiters, armor and divertor targets, and (3) in screening tests of materials. An actuator, the surface station, is currently mounted on D-III. It can accommodate samples of a few square centimeters cross section and offers great flexibility in measurement capability. Measurements can be made in the vessel during exposure, in a vacuum chamber between vessel exposure times, and of course post-mortem to any desired degree. The facility is on-line and operational so the only costs to be incurred would be in specimen preparation and staff for the experiments. Specimens could be inserted remotely so shot-by-shot coordination with the normal D-III operations is desirable. This would not only reduce interference with the D-III schedule, but would permit the most advantageous exposure for the individual experiments.

4. The exposure of engineering full scale size limiter pieces in D-III would provide a high degree of simulation testing for high power fusion devices such as FED. The stresses and temperature distributions would be simulated in a realistic limiter environment, particularly in plasma disruption leading. The replaceable feature would allow sequential testing of several concepts between major shutdowns. An actuator, capable of one dimensional movement of large size pieces, is available but not mounted on D-III. A large port would be required for access and a suitable one is not currently available. Modification of a port to share with other experiments is one possibility. An alternative would be to use a port in the lower half of the plasma chamber for access. The inner, outer and midplane regions of the plasma chamber would be accessible, however a more complex actuator, capable of motion in two or three dimensions would be required. These experiments would be higher cost and require more advanced planning than the others above, but would provide vital testing not achievable by any other means.

5. The detail post-mortem examination of both the normal design and experimental in-vessel
components provides valuable information on limiter behavior and response to the plasma environment. This function is not normally provided for in the D-III design operation programs and thus should be included in the in-vessel components test program. In-house facilities and facilities at National Laboratories complement each other and need to be utilized. The major costs are for staffing in data collection and analyses.

It is concluded from the above that a wide range of technology and engineering experiments can be done on D-III. Very modest levels of support such as monitoring in-vessel component behavior can be accommodated. Development in all these areas would prove fruitful for fusion development.

REFERENCES


TABLE 1
THERMAL/STRESS RESULTS FOR A HEMISPHERICAL THIMBLE LIMITER

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Fig. 1. A limiter conceptual design with a planar array of hemispherical cells.
Fig. 2. A limiter conceptual design with a linear array of hermispherical cells.

Fig. 3. Hemispherical limiter design example.
6.3 COMPOSITE LIMITERS FOR ABLATION CONTROL

6.3.1 Introduction

Limiters structured from composite materials as proposed in order to control the ablation rate; reduce near-surface damage from disruptive heat loads; and to retain their integrity for steady state heat transfer after many disruptions. The basic principle is to construct the limiter of built-up layers, bonded together with a relatively small radial (normal to the surface) tensile strength [1]. When a sufficiently high heat flux strikes the outer wall, a blister forms which then absorbs the energy by sublimation. The separation of the blister skin from the wall reduces the heat transfer to the wall and thereby controls and limits the depth of ablation.

Blisters can form from stresses produced by thermal gradients, both steady state and transient, in similar materials, or by differential thermal expansion in dissimilar materials. Stress magnitudes and hence the thresholds for blistering are determined by both the heat flux magnitude and time dependence. Alternately, blisters can be induced by reduction in bond strength between layers; an example would be in layers brazed together. Blisters would also be induced by the formation of a significant gas pressure between layers of material. In general, this would follow melting of the bond layers and with continued heating, vaporization of a second phase within the matrix phase.

A pyrolytic, isotropic carbon-silicon carbide (C-SiC) alloy coating on POCO AXF-5Q graphite substrate was selected. Pulsed heating response of the coated test specimens were found with an electron beam to simulate localized disruption heat loads. This paper primarily describes the observations made in these tests. The results indicate that this C-SiC alloy material is a suitable coating for exploring the blistering phenomenon for controlling ablation rate. Of additional interest and importance is the excellent performance of the coating under pulsed heat loads even as high as that which causes gross cracking in the graphite. This low-Z coating may be a very attractive candidate for graphite limiters and other protective armor in tokamaks.

6.3.2 Features of the C-SiC Alloy

The C-SiC alloy coating is comprised of an isotropic carbon matrix with a uniform dispersion of SiC of ~ 200Å diameter particles [2]. The alloy coatings were deposited by a CVD process in a bed of ceramic particles fluidized by the gaseous reactive mixture [2].

The C-SiC alloy system has several important features which bear on its utility as a limiter material. High temperature applications of carbon and silicon carbide materials are extensive and a sizable data base for materials properties is available [3]. Carbon and silicon have low atomic
numbers which relieves problems of plasma impurity radiation losses and the total sputtering of the C-SiC alloy material is depressed by factors of 10-20 compared to carbon and graphite in some measurements (up to 873 K) [4]. The methane production in other measurements up to 900 K was a factor of ten less than graphite [5]. However, another measurement shows the erosion rate for SiC to be about 25 times that of TiC at 320 K and 500 eV [6]. The differences in these measurements may be due to ion energy differences or variations of sputtering rate with total dose or temperature. In a tokamak environment this material should not experience selective reaction with metal species deposited on it as has been observed for TiC coated limiters [7]. Further, coating thickness can be increased more readily.

The specific heat and thermal conductivity of the C-SiC alloy and POCO graphite are known up to temperatures of 2300 K [3,4]. The carbon phase of the C-SiC alloy is an isotropic pyrolytic carbon with a room temperature thermal conductivity on the order of one-tenth that of graphite, although they tend to converge at higher temperatures. The coefficient of thermal expansion is $\sim 6 \times 10^{-6} \degree C$ in contrast to $7.7 \times 10^{-6} \degree C$ for the POCO substrate used here. The mechanical properties of the C-SiC alloy system have been measured [8] and Young’s modulus increases significantly as the silicon content increases. This feature plays a role in stress accommodation between layers of material and in controlling blister formation.

Both silicon carbide and carbon have high vapor pressures at the temperatures attained in these measurements (3500°C). Silicon carbide decomposes to silicon and carbon above 2500°C and above 3513°C the silicon is a gas [9]. This could provide a blistering mechanism. Carbon exhibits a rapidly increasing vapor pressure above 2500°C [10] and above about 3000°C the heat of sublimation [11] is the dominant heat loss process as will be seen below. The intent of the composite material design is to utilize both sublimation and blistering effectively to control the ablation rate and reduce the extent of damage in the graphite tiles during high localized thermal pulses typical of plasma disruptions.

6.3.3 Test Approach

Initially, 1.5 mm thick disks (25 mm diameter) of graphite were coated in the fluidized bed. Thermal shock tests were conducted at Sandia National Laboratory, using a 30 kW electron beam system [12] in which the beam was rastered to provide uniform deposition over a 10 mm $\times$ 10 mm area.

Fracture of thin thickness specimens occurred at power densities of interest for exploring the blistering threshold. Therefore additional test specimens of 12.7 mm thickness and shapes of 25 $\times$ 25 mm and 51 $\times$ 51 mm were fabricated. Each piece was individually supported in a fixed location within the fluidized bed coater. After coating a few specimens were cross-sectioned and examined
using standard metallographic procedures. No microcracks were observed. At \(< 500\)X the coating microstructure appearance was generally uniform and porosity was very fine and dispersed. The coating on some specimens exhibited a subtle banding feature at locations parallel to the interface.

Following the thermal pulse tests the surfaces of the specimens were examined using a stereo-microscope at 25X for evidence of blistering and microcracking. For some, microstructural examinations were conducted on cross sections through the location of beam deposition. An energy dispersive X-ray analysis was used to measure near-surface (\(~1\)\(\mu\)m depth) Si loss in the blistered regions and blister caps. Quantitative values were obtained by comparing counts from the areas of interest with counts from a pure Si standard positioned adjacent to the specimen.

Nominal bulk composition of the coatings was determined using the X-ray fluorescence method which analyzes a volume of coating of approximately 10 mm in diameter by 25 \(\mu\)m in depth. The coating compositions (bulk) were 6 wt % Si (\(\pm 1\) wt % ) for the 12.7 mm thick specimens. The disk specimens coated at an earlier time, had compositions of 6.0, 10.9 (two) and 16.6 wt %.

### 6.3.4 Test Results and Microstructural Analyses

**Blistering Phenomenon**

Experimental confirmation of the formation of blisters with high pulsed thermal fluxes were first demonstrated with the disk-shaped specimens at 200 MW/m\(^2\) for pulse durations of \(\geq 60\) ms. In Fig. 1 features of the blister formation are shown. In addition to the remaining rim of a large blister, two smaller blisters are in the formative stage and each is located at different depths. In Fig. 2 a SEM photomicrograph more clearly reveals the ability of the normally brittle C-SiC alloy to deform plastically at very high temperatures. In Fig. 3, the early stages of blister formation are evident at different depths within the C-SiC alloy coating for a cross section through the gross zone of blistering shown in Fig. 1.

The results of the electron beam deposition tests are presented graphically in Fig. 4. To identify the threshold for blister formation, most of the specimens were exposed to the power density of 200 MW/m\(^2\) and only the deposition time was varied. As indicated in Fig. 4 for the thick (12.7 mm) specimens, blistering was not observed for energy depositions which caused blistering in the thinner disks. At 300 and 500 ms some blistering was observed. All 25 \(\times\) 25 mm specimens exposed above 220 ms cracked or fractured through the underlying threaded hole. Examination of a cross section, perpendicular to the surface of beam deposition and in the blistered zone, revealed features of blister formation at different depths similar to the thinner disk specimen in
Fig. 3. Of significance, no microcracks were observed, on the surface or in the coating, which were oriented perpendicular to the interface.

Microcracking Without Blistering

Surprisingly, some of the specimens did not blister but instead a network of microcracking was developed on the surface (visible at $\geq 10X$) as shown in Fig. 5. Darkening in the area of beam deposition was typical at energy levels of this magnitude and greater. Microscopic examination of cross sections revealed microcracks penetrating to the graphite interface, but no blistering or microcracks parallel to the interface. A $50 \times 51$ mm specimen was pulsed four times with each pulse at 200 MW/m$^2$ and durations of 0.26, 0.22, 0.16, and 0.30 seconds, respectively, and each time at a different location approximately 20 mm from the previous deposition. The depositions were away from the underlying support hole and the specimen did not crack through the hole. Each area of deposition revealed a microcrack pattern similar to that observed earlier in Fig. 5. A cross section through the fourth pulse (.030 sec duration) revealed four cracks penetrating the graphite (to depths of $\leq 0.8$ mm). In AXF-5Q graphite of similar thickness a crack of 0.76 mm depth was formed for a pulse of 200 MW/m$^2$ for 0.5 sec. Thus, the threshold for substrate cracking was reached without spalling of the coating.

As shown in Fig. 6, a banded structure was observed for these specimens that only microcracked and did not blister. Typically there were three banded regions of similar appearance. This likely reflects cyclic changes occurring in the mechanism of deposition during coating.

Silicon Depletion From Coating

The zone of beam deposition exhibited a region of darkening. When blistering occurred, the caps of the blisters were very dark. The same darkening occurred on the surface of the specimen which experienced microcracking in the coating. At $\sim 1000X$ magnification a distribution of fine black spots did not appear to be pores, but instead had the characteristics of a second phase, possibly a SiC phase formed during cooling.

Chemical analysis was conducted on the region shown in Fig. 5 and of a blistered region for one of the thinner disk specimens. The blisters analyzed are shown in Fig. 7. Optically the blister caps are considerably darker than revealed in the SEM photomicrograph. The blister caps lost considerable Si and more than the general area which included many of the blisters. The darkened regions in Fig. 5 revealed appreciable loss of Si. Also, a significant difference in Si-content was measured in the near-surface analysis ($\sim 1\mu$m depth) as compared to the bulk coating analysis (to $\sim 25\mu$m depth and larger surface area).
Thermal Analyses

The temperature distributions throughout the samples have been calculated with a two-dimensional time dependent computer program. Heat loss by radiation from the incident e-beam surface was included with an emissivity of 0.85. Heat loss by sublimation was also included [10,11] and this is the temperature limiting process as temperatures rise above 3000°C.

Figure 8 shows the calculated temperature-time distribution for a coating thickness of 200 μm on a 12.7 mm POCO substrate. Note first that the temperature saturates at 3495°C is due to the high heat of sublimation and vapor pressure of carbon. At temperatures above about 3200°C, this is the dominant heat absorption mechanism.

Comparison of the temperatures for 200 μm and 50 μm coatings show that after 100 ms both temperatures and gradients in the first 50 μm from the surface are almost identical. We thus conclude that first order temperature and temperature gradient effect differences are not large enough to explain the different coating degradation modes of blistering and microcracking observed among the various coating thicknesses and exposure times. To some extent this is confirmed in the relatively similar extent of blistering or microcracking observed in the specimen surfaces over a range of exposure times.

We have also estimated stresses[1] and the formation of blisters using a thin film buckling model [13]. These results show that blistering would be produced for all of the tests here if the coating were debonded from the substrate; blistering would not be produced in any of the specimens here if the coating were bonded to the substrate with a tensile strength equal to POCO. Thus it would appear that a debonding mechanism should be searched for to account for the blistering observed in the coatings of some specimens. One possibility would be segregation of silicon carbide in a layer parallel to the interface. The SiC would decompose into molten silicon and high pressure vapor providing a debonding mechanism. Interestingly, the bonded microstructure presented in Fig. 6 did not show any blistering. Rather, it was the more uniform appearing microstructure that blistered. The crystallite features and deposition characteristics for this type of coating would not be resolvable optically (< 1000X) and identification of key features and differences would require further studies involving electron microscopy and microprobe analysis.

6.3.5 Conclusions

We have demonstrated experimentally a blistering process in a composite material of C-SiC alloy induced by high power short time surface heating. This blistering process is proposed as a self-protective mechanism to limit erosion of plasma interface components subject to disruptive energy loads in tokamaks. Additionally we have tested some C-SiC alloy coatings which do not
blister, but survive up to the limits of the graphite substrate with only microcracking. The differences in these two coatings which exhibit two distinct effects of blistering or microcracking has not been resolved. Both coatings appear suitable for limiter applications however. This C-SiC coating has some advantages over TiC coatings presently used in limiters and beam armor, and tests in D-III are planned to compare each.

REFERENCES


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Fig. 1: The blistering phenomena is shown in a sequence of increasing magnification.
Fig. 2: A SEM photomicrograph of the plastically deformed and fractured blister skin and the blistered underlayers.

Fig. 3: Cross section through zone of electron beam deposition showing blister formation at different depths in the C-SiC alloy coating.
**Fig. 4:** Blistering and microcracking observations in C-SiC alloy coatings exposed to electron beam deposition over an area of 10 mm x 10 mm. POCO AXF-5Q graphite substrate; coated with C+SiC alloy (~9 wt % SiC) of thicknesses from 50 to 200 μm.

**Fig. 5:** View of specimen surface showing typical microcracking appearance and a darkened zone within boundary of electron beam depositions (10 mm x 10 mm). Two pulses: 200 MW/m² for 160 msec and 220 msec. Coating thickness: 210 μm.
Fig. 6: Banded microstructure typically observed in coating of specimens which experienced microcracking perpendicular to surface (no blistering) from electron beam deposition.

Fig. 7: SEM photomicrograph of specimen surface showing blister caps (A, B, C) and the blistered region, analyzed for change of silicon concentration in C-SiC alloy coating (16.6 wt % Si initially in coating)
Fig. 8: Calculated temperature rises for POCO Graphite (12.7 mm thick) at 200 W/m² surface heat flux.