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PROGRESS REPORT #2 ON THE DESIGN CONSIDERATIONS FOR A LOW
POWER EXPERIMENTAL MIRROR FUSION REACTOR

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PROGRESS REPORT #2 ON THE DESIGN CONSIDERATIONS FOR A LOW POWER
EXPERIMENTAL MIRROR FUSION REACTOR*

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ABSTRACT

The conceptual design of a 200 MWe mirror fusion reactor is discussed. The reactor uses DT plasma fuel magnetically contained by the Yin Yang magnet geometry. The reactor is fueled by 550 keV neutral particles injected into an ellipsoidal plasma volume of 130 m^3 . The plasma ion density is $1.2 \times 10^{14} \text{ cm}^{-3}$. The vacuum magnetic field requirements are 50 kG at the center of the reactor and 150 kG at the mirrors. Thermal energy is obtained by fusion neutron moderation in a 1 m thick Li-C-Fe blanket surrounding the plasma. Direct conversion of the charged particle energy uses the Post type converter with electrostatic focusing.

The design emphasizes the interaction between plasma physics and engineering and considers the plasma, containment magnet, injection, neutronics, blanket energy conversion, tritium recovery, and charged particle energy conversion both as separate subsystems and as elements of the total reactor system.

OVERVIEW

This is the second of a series of reports on the preliminary design of a 200 MWe mirror fusion reactor. The first report was issued in November, 1972, as UCRL-74054.¹

This report supersedes the previous one; no back referencing should be necessary. Several new sections and new information have been added.

*This work was performed under the auspices of the U.S. Atomic Energy Commission.

One of these new sections is on the injector system, which we are treating as a complete subsystem study, starting with the ion source and ending with the trapping efficiency of neutrals entering the plasma. Our findings indicate that injector efficiencies of ~88% are possible, provided that very close attention is given to thermal recovery of the kinetic energy losses. In comparison, our first report assumed an overall injection efficiency of 95%.

Another new section discusses the selective leakage of charged particles. Selective leakage means that if the B field in the mirror region is biased locally, escaping charged particles do so through a small "window". The area of the window sizes the expander and influences economics, e.g., conservation of field lines requires that $B_M A_M = B_E A_E$, where the subscript M refers to conditions in the mirror region and the subscript E refers to the terminus of the expander. We had assumed on cursory study in Report #1 that selective leakage could be done with 98% efficiency. The more complete study reported here, which includes small angle and large angle scattering, indicates this efficiency can be ~95%, a very encouraging result.

In the area of neutronics we have now taken into account the real blanket geometry, the effects of voids, and coupled neutron-gamma effects. We have found that voids of ~20%, which are a consequence of using individual tubes in a square pack array for our blanket, have negligible effect on tritium generation or energy generation. Likewise, the investigation of the effects caused by a blanket geometry dictated by the Yin Yang coil showed minor differences between real geometry and our original blanket representation of a series of spherical shells. For this study we chose a blanket whose material composition was very simple. The price we paid for simplicity was a low value (1.1) for m , the blanket energy multiplication. The importance of this parameter is demonstrated in Table 1, where we compared the "simple" blanket with a blanket in which $m = 2.0$. The most significant effect is the factor of two decrease in circulating power

Table 1. Comparison of blankets.

	Simple blanket	Better blanket	Better blanket and better direct converter
Containment parameter, Q	1.2	1.2	1.2
Blanket energy multiplication, m	1.1	2.0	2.0
Direct converter efficiency, η_D	0.70	0.70	0.85
Thermal converter efficiency, η_T	0.45	0.45	0.45
Injector efficiency, η_I	0.88	0.88	0.88
Auxiliary power	20 MW	20 MW	20 MW
Net system efficiency, η_{sys}	27%	33%	38%
<u>Injector power</u> Net electric power	3.3	1.6	1.4

fraction, shown in Table 1 as injector power/net electric power. The means for increasing m are straightforward. We can trade the excess tritium generation (>1.3) for energy production by making compositional changes in the blanket. For example, we can add materials with high (n,2n) characteristics such as beryllium or lead. This can be coupled with an (n, γ) reaction. The limit on m is not clear. In a blanket study for Astron² we were able to obtain values of m of ~ 1.85 with tritium breeding of 1.2. If the tritium breeding is allowed to approach unity, then an m of 2 seems possible.

We recommend to all systems analysts the use of a power flow diagram such as that shown in Fig. 1. In addition to an energy balance for the case m = 1.1, we have shown the effect of m = 2.0. Note that the net electrical power increases by a factor of two for the case m = 2.0. That is, the circulating power fraction has been halved.

In blanket coolant flow design, we have shown that tube routing on a path comprised of circular arcs is sufficiently close to the path of the actual magnetic field lines that pressure drop penalties associated with magnetohydrodynamic (MHD) effects of flowing lithium in B fields are

Fig. 1. Power flow diagram. All powers are in MW; parenthetical entries are for $m = 2.0$; system efficiency is 27% (33%).

acceptably low. We had previously postulated this effect, but now we have a specific design.

A significant finding, appropriate to all reactors using the steam cycle, is that tritium leakage to the environment from the steam is suppressed because of the low tritium partial pressure in the steam. This low partial pressure is caused by the heretofore overlooked fact that tritium exchanges with hydrogen in water as follows:

Actually, the quantity of HTO formed per year is small because we allow only ~0.033 g/yr of tritium to enter the steam.

We have been asked many times since issuing Report #1 why we chose ~200 MWe as opposed to 1000 MWe when the latter is representative of commercial power plants. There are several reasons. First, we are not designing a commercial reactor. There are many steps to be taken before anyone in the fusion reactor field can get to this point. We do not even choose to call this a reference design; it is a study to determine what areas need attention. Second, we wish to make a point that mirror machines may be uniquely qualified to be low power reactors if that is desirable. Figure 2 shows that in the 200 to 400 MWe range the cost of a mirror reactor with direct conversion is a weak function of power output. That mirror reactors can be operated economically at low power levels (down to ~200 MWe) is in striking contrast to the predictions for other fusion reactors. Third, if we consider this an experimental reactor study, then we must be concerned not with dollars/kW alone but also with total dollar cost. Congress would certainly be more receptive to funding a hundred million dollar experimental program than one costing a billion dollars.

Throughout this study we have tried to use calculated values that are supportable at our current level of understanding and that will be accepted by the fusion research community. Therefore, we class our calculated values as neither optimistic nor pessimistic.

Fig. 2. System cost as a function of power output for a mirror reactor with DT cycle and nominal component performance.

Table 2. System dimension and characteristics of a 200 MWe DT mirror reactor with direct conversion.

<u>Magnet Coil</u>	<u>Injector (Continued)</u>
R = 10 m	Injection area required $\approx 4.5 \text{ m}^2$
w = 6.6 m	Type = neutral beam
2h = 4.2 m	No. of injectors = unknown
Central field = 50 kG	No. of accelerating stages = unknown
Mirror field = 150 kG	
<u>Expander</u>	<u>Plasma</u>
Expansion ratio = 100	Shape = ellipsoidal (minimum B volume)
Expander angle $\theta = 240^\circ$	Z axis intercept = 3.5 m
E at expander = 620 keV	X and Y axis intercept = 3.1 m
Beam height $\approx 0.87 \text{ m}$	Volume = 130 m^3
Radius $\approx 76 \text{ m}$	Beam height at mirror exit = 0.74 m
<u>Blanket</u>	Beam area at mirror = 1.5 m^2
Thickness = 1 m average	Density $n = 1.2 \times 10^{14}$
Individual tube diameter $\approx 0.06 \text{ m}$	$\beta = 0.85$
shape = conform to field lines	<u>Collector</u>
Composition = Li, C, Fe, Ni	Depth $\approx 22 \text{ m}$
Energy multiplication ≈ 1.1	Number of elements = 20
Tritium breeding ratio ≈ 1.0 to 1.4	Type - periodic electrostatic focusing
<u>Injector</u>	<u>Power Distribution</u>
Injected power = 490 MW	P _{Fusion} = 590 MW
Average injection energy = 550 keV	P _{Injection} = 490 MW
Total injection current = 890 A	P _{Neutron} = 470
Maximum current density $\approx 0.02 \text{ A/cm}^2$	P _{Ion} = 610 MW
	P _{Elec} = 170 MW

In Fig. 3 we depict a conceptual mirror reactor with direct conversion. Table 2 specifies the system dimensions and characteristics.

THE PLASMA

If we are to design a mirror reactor system with a particular net output power, we must know plasma parameters such as density and energy and the containment parameter, Q, defined as the ratio of fusion power to trapped injected power. Note that this Q does not include the blanket neutron energy multiplication factor, which is more properly considered a neutronics parameter.

Fig. 3. Conceptual mirror reactor with direct conversion.

For this study, we will take $Q = 1.2$, derived as follows. We define the ratio of plasma pressure to magnetic pressure as

$$\beta \equiv \frac{P_{\perp}}{B_{o,vac}^2/2\mu_o} = \frac{\sum_i n_i kT_i}{B_{o,vac}^2/2\mu_o} . \quad (1)$$

This β is ≤ 1 by definition and may be further limited by plasma stability requirements. We elect to use a Yin Yang containment configuration that has a nearly spherical minimum $|B|$ plasma volume. We assume that for this configuration β may be as large as 0.85.

Next, we make a somewhat arbitrary decision to provide a vacuum mirror ratio $R_{vac} = 3.0$. This is a compromise value based on certain considerations. First, at constant β , the fusion power density increases with the fourth power of $B_{o,vac}$, thus decreasing size and cost per unit power. Second, the containment parameter, Q , increases with the log of the mirror ratio, but B_{max} is limited by superconducting technology and the strength of structural materials. Then, from an approximate equation for the plasma mirror ratio,³

$$R \approx \frac{R_{vac}}{\sqrt{1 - \beta}} , \quad (2)$$

we calculate $R = 7.7$.

Futch, et al. present results of Fokker-Planck calculations for Q values of DT systems as a function of R and injection energy.⁴ An extension of their reported results⁵ yields Fig. 4 for $R = 7.7$. The Q values of Fig. 4 include their estimated 40% containment enhancement for a narrow angle source injecting perpendicular to the magnetic field. Although Q maximizes at an injection energy near 200 keV, operation at this low energy would require a prohibitively large direct converter. After several iterations, we choose a D and T injection energy of 550 keV. Then, from Fig. 4 we have $Q = 1.2$.

Fig. 4. Q as a function of injection energy for $R = 7.7$.

Note that the values of β and Q used in this study cannot be viewed as final. Investigations of plasma stability (achievable β) and more sophisticated containment analyses (achievable Q) are major ongoing projects in the mirror machine program. Cordey and Watson⁶ and Hall⁷ are somewhat more pessimistic than we concerning achievable β and plasma mirror ratio. For a long plasma and $R_{vac} = 3.0$, Cordey and Watson⁶ predict a maximum $\beta = 0.75$ at the boundary of MHD instability and then calculate [from Eq. (2)] a maximum plasma mirror ratio $R = 6.0$. For a spherical plasma and the same $R_{vac} = 3.0$, Hall⁷ predicts a maximum $\beta = 0.65$ and a maximum plasma mirror ratio $R = 4.2$. Hall does not use Eq. (2). Since Q scales with $\log R$, Hall's prediction would result in a 30% reduction in Q . On the other hand, recent consideration of anisotropic effects in the Fokker-Planck equations indicates that the Q values calculated by Futch, et al. may be low by as much as a factor of 2 (Ref. 8).

Therefore, we use $Q = 1.2$ knowing that the final value may be either higher or lower. If it is much lower, our reactor system may be unacceptable because of low system efficiency. If it is higher by a factor of 2, we will have a welcome increase in reactor system efficiency and a substantial reduction in circulating power fraction.

We use the results of the Fokker-Planck calculations by Futch, et al. to specify the relative densities and mean energies of the plasma species. Fuel is injected at the relative rate $D/T = 1.6/1.0$ to achieve equal D and T plasma densities. For the chosen injection energy of 550 keV, the plasma density ratios at steady state are $D/T/\alpha/e = 1/1/0.06/2.12$. The mean energies in keV of the various plasma components are

$$\bar{E}_D = 580$$

$$\bar{E}_T = 640$$

$$\bar{E}_\alpha = 1600$$

$$\bar{E}_e = 84.$$

Approximately 6% of the injected D and 10% of the injected T undergoes thermonuclear burn. The mean energies in keV of the escaping charged particles (after passing through the plasma ambipolar potential of 280 keV) are

$$\begin{aligned} E_{\text{exit,D}} &= 550 \\ E_{\text{exit,T}} &= 580 \\ E_{\text{exit,e}} &= 58 \\ E_{\text{exit,\alpha}} &= 1200. \end{aligned}$$

The escaping D and T ions have a minimum energy equal to the plasma potential; the escaping alpha particles have a minimum energy equal to twice the plasma potential.

For plasma mirror ratios between 5 and 10, Moir⁹ estimates the mean plasma energies $\bar{E}$ to be related to kT by

$$\bar{E} \approx 1.8 kT. \quad (5)$$

A Maxwellian distribution would have $\bar{E} = 1.5 kT$.

We set $B_{o,vac} = 50$ kG to limit the maximum field at the conductor nearest the mirror to something less than 200 kG. Then, from Eq. (1) the plasma component densities are

$$\begin{aligned} n_D &= n_T = 0.6 \times 10^{14} \text{ cm}^{-3} \\ n_\alpha &= 0.04 \times 10^{14} \\ n_e &= 1.3 \times 10^{14}. \end{aligned}$$

Using these component densities and the $\bar{v}$ values calculated by Futch, et al.⁴ we find that the plasma has a fusion power density of $\sim 4.5 \text{ MW/m}^3$.

THE CONTAINMENT MAGNET

Coil Design

The Yin Yang containment magnet was sized to provide the containment volume necessary for the desired fusion power, 590 MW. It has a radius to the center of the fan coils of 10 m, a fan midplane separation of 4.1 m, and a fan width of 6.6 m, as shown in Fig. 5.

For magnetic field calculations, we divided the fan width of each of the two identical coils into seven zones, each represented by a filamentary conductor. Each filamentary conductor is made up of four semi-circular arcs. Allowing equal current in all the filamentary conductors, we used computer code MAFCO to calculate the magnetic fields. Figure 6 shows the magnetic field lines (solid lines) and the closed constant $|B|$ contours (dashed lines) of the magnetic well. The vacuum mirror ratio is 3.0 and B_0 is 50 kG. Each coil requires a total of 1.4×10^8 ampere turns.

The nearly ellipsoidal volume inside the last closed contour of $|B|$ is 130 m^3 . Taking this to be the volume of the dense plasma and using the fusion power density calculated in the preceding section, we obtain a total fusion power of 590 MW.

We specify an overall average current density of 3500 A/cm^2 , which requires a coil thickness of 60 cm. Because the coil thickness is small compared to the other coil dimensions, the plasma region field calculations based on the "zero thickness" seven-zone conductor model are very accurate. A more detailed field calculation by Moir and Taylor,¹⁰ which uses a 62-zone conductor model with finite thickness, resulted in a field configuration and a required total current essentially the same as our simpler calculation.

A separate MAFCO calculation that replaced the filamentary conductor at the mirror position by a grid of 32 equally spaced filamentary conductors (four across the coil thickness) yielded a maximum field within the conductor region of 165 kG, which is 10% greater than the mirror field. Our model did not investigate mirror field maxima near the small

Fig. 5. Yin Yang coil.

Fig. 6. Field configuration of magnetic well.

radius coil turnarounds, but Moir showed that the maxima could be eliminated by a local thickening of the coils at the turnarounds.¹⁰

Coil Support Structure

Figure 7 shows the main features of the coil with its support structure. The very large $|B|$ fields required, ~ 165 kG, produce very large forces that must be contained. For example, Fig. 8 shows the pressure determined for the plane shown in Fig. 6, assuming that all forces in the X direction are transmitted to the outer face. The maximum pressure is 26 ksi, average pressure is 18 ksi, and the total force normal to the wide face of each coil is $\sim 1.8 \times 10^8$ pounds per meter of coil length for a total of $\sim 5.8 \times 10^9$ pounds on the entire face.

These enormous forces exerted on such a large structure represent a formidable structural problem. Direct tension members between the two coil faces are avoided in this design because such members might interfere with the coil windings, blanket structure, and plasma. An alternative way to support the coils is with stiff compression members transmitting the force to a series of large hoops. Fastening together a series of such hoops of varying size to support the entire coil makes a structure resembling the spherical shell shown in Fig. 7.

For a peak field of 165 kG, Moir and Taylor's calculations yield a spherical shell thickness of 80 cm, a shell mass of 21×10^6 kg, and a compression filler mass of 16×10^6 kg.¹⁰ See Ref. 10 for a more detailed description of the coil support structure.

Thermal Insulation

The entire structure must operate at cryogenic temperatures because transmitting the very high forces from the cold windings to a room temperature structure by way of the compression members would cause an excessive heat flow into the superconductor and result in a very high refrigeration cost.

Fig. 7. Illustration of magnet showing the coil windings, filler structure, and structural support vessel.

Fig. 8. Equivalent pressure profiles across the face of the conductor. The solid line is the pressure profile in the X direction calculated by summing the forces on each individual filamentary conductor. The dashed line is the value of $B_z^2/2\mu_0$ along the inner surface of the conductor. The dotted line is the value of $B_z^2/2\mu_0$ on the Z axis.

Thermal insulation of the entire structure will be provided by evacuated superinsulation, which may be an integral part of the spherical shell's outer surface. There will be a total of 4800 m^2 of superinsulation, 3700 m^2 outside shell area and 1100 m^2 inside coil area. The direct heat leak through this type of insulation when applied to large (6.6 diam $\times$ 15 m long) liquid hydrogen tanks is $\sim 1 \text{ W/m}^2$ under optimum conditions.¹¹ We assume 2 W/m^2 to allow for the effect of penetrations, etc. and obtain a 9.6 kW heat leak through the insulation. Ref. 10 gives more details of the thermal insulation.

The enormous weight of the coil and structure can be supported by low heat leak compression material, such as an epoxy-glass laminate.¹² Approximately 3 m^2 of compression cross section, 1 m long, adds only 0.4 kW and will support the weight with an adequate safety factor. Therefore, the total thermal heat leak is $\sim 10 \text{ kW}$. The heat load on the magnet will be further increased by neutron and gamma leakage from the shield.

THE INJECTION SYSTEM

General Description

The mirror machine reactor requires the continuous injection of high energy particle beams to maintain the plasma against end losses. The present design requires the injection and trapping of 550 A of 550 keV deuterium atoms and 340 A of 550 keV tritium atoms into a nearly spherical plasma of density 1.2×10^{14} particles per cubic centimetre and a diameter of 620 cm. The total power of the trapped injected beam is 490 MW. The efficiency of the injection system has a strong influence on the overall reactor system efficiency. For the reactor model of this study, the overall injection efficiency was found to be 88%. Ref. 13 contains more information on neutral beam injection systems.

Figure 9 shows the injection system and its power flow diagram. Positive ions are produced in a source and accelerated to an energy of E^+ . These ions are produced through an alkali metal vapor cell that produces

a. Schematic of the neutral-beam injection system

b. Diagram of the power flow system

Fig. 9. Injection system and power flow diagram.

negative ions. The negative ions are accelerated to the desired injection energy and neutralized, and the neutral atoms are injected into the plasma. The ions not neutralized are magnetically separated from the neutrals and guided into a direct converter, where a fraction of their energy is recovered.

Some of the injected neutrals charge-exchange with the trapped reactor plasma ions such that the neutrals formed deposit their energy over the reactor first wall. The portion of the injected beam that passes completely through the reactor plasma may be stripped of an electron and the energy partially recovered in a second direct converter.

The overall injection efficiency is defined as

$$\eta_i = \frac{P_T}{P_{ex} - P_{rec}}, \quad (4)$$

where P_T is the trapped power, P_{ex} is the power expended in producing the ions and accelerating them to the energy required for a given injection energy, and P_{rec} is the amount of power lost in the injection system components that is recovered in direct and thermal converters. For the injection system shown in Fig. 9, the overall injection system efficiency is

$$\begin{aligned} \eta_i = & \eta_{a2} f_n f_T \left[1 + \frac{q}{M n_{+-} \eta_{a1} E} (1 - \eta_{T1}) + \frac{E^+}{ME} \left[\left(\frac{1 - \eta_{+-} \eta_{a1}}{\eta_{+-} \eta_{a1}} \right) \right. \right. \\ & + (1 - \eta_{a2}) \eta_{T4} - \left(\frac{1 - \eta_{a1}}{\eta_{a1}} \right) \frac{\eta_{T2}}{\eta_{+-}} - \left(\frac{1 - \eta_{+-}}{\eta_{+-}} \right) \eta_{T3} \left. \right] - (1 - \eta_{a2}) \eta_{T4} \\ & - \eta_{a2} \left((1 - f_n) [\eta_{D1} + (1 - \eta_{D1}) \eta_{T5}] + f_n \left\{ (1 - f_T - f_w) \right. \right. \\ & \left. \left. \times [\eta_{D2} + (1 - \eta_{D2}) \eta_{T7}] + f_w \eta_{T6} \right\} \right) \left. \right] , \quad (5) \end{aligned}$$

where M is the ratio of the mass of the accelerated ion to the mass of the injected neutral, E is the energy of the injected neutral, f_n is the

fraction of the ions entering the neutralizer that are neutralized, f_T is the fraction of the neutral beam that is trapped in the reactor plasma, f_w is the fraction of the neutral beam that charge-exchanges with the reactor plasma ions without undergoing a reionizing collision, and the remaining symbols are defined in Fig. 9b.

For positive ion acceleration and neutralization, Eq. (5) reduces to

$$\eta_i = \eta_{a2} f_n f_T \left[1 + \frac{q}{ME} (1 - \eta_{T1}) - (1 - \eta_{a2}) \eta_{T4} - \eta_{a2} \left\{ (1 - f_n) [\eta_{D1} + (1 - \eta_{D1}) \eta_{T5}] + f_n \times \left\{ (1 - f_T - f_w) [\eta_{D2} + (1 - \eta_{D2}) \eta_{T7}] + f_w \eta_{T6} \right\} \right\} \right]^{-1}, \quad (6)$$

with $\eta_{+-} = \eta_{a1} = 1$, and $E^+ = 0$. Note that if negative ions are produced directly from the source, Eq. (6) also gives the overall injection system efficiency.

The performance of individual components of the injection system are probably an upper limit as they do not take into account some real effects, such as removal of neutral gases from the system and beam divergence in the system.

Ion Source

Ion sources usually consist of a plasma arc and an ion extraction system. Control of the ion species is a problem for plasma production. Even with the arc and gas optimized for a single species, the plasma composition of that species is generally only 50 to 80%.¹⁴ For the purpose of this analysis, the source will be considered to form single-species ions. The effect of this assumption on efficiency is negligible because energy levels for ion production are in the eV range.

The source energy, q , required to produce an ion is estimated to be ~500 eV.¹⁵ This value of q includes the inefficiencies of ion production

and extraction. Some of this energy can be recovered in a thermal converter with efficiency η_{T1} by using cooled high temperature electrodes in the ion source.

Negative Ion Production

We are interested in negative ions because at energies of interest for economical direct converters (>100 keV) the efficiency of neutralization of D^- is 65 to 90%, whereas for D^+ it is $\sim 20\%$. Negative ions can be produced directly by an ion source or by electron capture by a positive ion beam in a gas or vapor cell. We consider both cases, although high currents of negative ions appear difficult to obtain directly from an ion source.

The conversion efficiency of D^+ to D^- has been determined to be a maximum of 0.21 ± 0.04 for a deuteron energy of 1.5 keV passed through a cesium vapor cell whose atom density times length is between 10^{15} and 10^{16} atoms per square centimetre.^{16,17} Osher has suggested using a series of cesium vapor cells with between-stage separation to increase the effective positive-to-negative ion conversion efficiency.¹⁸ For a series of cells, Osher gives the positive-to-negative ion conversion efficiency as

$$\eta_{+-} = \sum_{n=1}^N f_{+-} (1 - f_{+-})^{n-1} T^n, \quad (7)$$

where f_{+-} is the efficiency of the basic positive-to-negative ion conversion process, T is the transmission of each cell, and N is the number of cells. For a 21% conversion to negative ions and a 95% transmission in each cell,

$$\eta_{+-} = \sum_{n=1}^N 0.21 (0.79)^{n-1} (0.95)^n. \quad (8)$$

Thus the positive-to-negative ion conversion efficiency is, for example, 20% for one cell and 55% for four cells.

The positive ions that are not converted to negative ions in the cesium cell are converted to neutral deuterium atoms with an energy of 1.5 keV. In principle these neutrals could be ionized by stripping an electron and their energy recovered in a direct converter, but this is difficult at low energies. For this study, the energy of the neutrals is assumed to be recovered in a thermal conversion system with efficiency η_{T3} .

Ion Acceleration

The ions must be accelerated to an energy of E_a as follows.

$$E_a = ME, \quad (9)$$

where E_a is the energy of the ion leaving the accelerator, M is the ratio of the mass of the ion to the mass of the injected neutral, and E is the desired energy of the injected neutral particle. In a study of an injector system for heating a toroidal plasma, Julian estimated the achievable efficiency of an accelerator so that the accelerator power supply requirements could be estimated reasonably closely.¹⁹ A breakdown of Julian's estimates for a 16.6 A beam of 3 MeV D_3^+ ions is given in Table 3.

Table 3. Accelerator losses.

Item	Power loss/output power
Interception of beam by electrodes	≈ 0.005
Retrograde electron current losses	≈ 0.002
X-ray production by electron currents	≈ 0.04
Heat losses of potential divider	≈ 0.04
Total	≈ 0.087

The power losses by beam interception and the retrograde electron current were assumed to take place at the inlet end of the accelerator tube. Defining the accelerator efficiency as

$$\eta_a = \frac{\text{power out}}{\text{power out} + \text{power loss}},$$

$$\eta_a = \frac{1}{1 + 0.087} = 0.92.$$

In the present study both η_{a1} and η_{a2} will be taken to 92%.

Julian notes that the estimated losses, and thus the accelerator efficiency of 92%, are based on "simple optimistic assessments", and should serve as a guide only.¹⁹ Detailed studies are required. The losses in the accelerator can be recovered in a thermal converter with efficiencies η_{T2} and η_{T4} by using cooled electrodes, x-ray shields, and potential dividers capable of operating at high temperature.

Neutralization

For a neutral injection system, the high energy ions must be converted into high energy atoms. The energetic atoms are formed by the loss of an electron from a negative ion, the capture of an electron by a positive ion, or the dissociation of a molecular ion passing through a gas or plasma.

Riviere¹⁹ has estimated the efficiency of neutralization, f_n , for the production of atoms from D^+ , D^- , D_2^+ , and D_3^+ ions (see Fig. 10). He did this as a function of the deuterium atom energy for a gas neutralizer cell for all the species and for a plasma neutralizer cell in the case of D^- and D_2^+ species. The plasma neutralizer performance was computed assuming that the plasma behaves as an electron gas. This assumption tends to predict a higher neutralizer efficiency than a fully ionized plasma can deliver.²⁰ The power efficiency of D^+ for deuterium atoms of energy <50 keV is taken from Allison and Garcia-Munoz.²¹

The energy of the ions that pass through the neutralizer can be partially recovered in a direct converter topping cycle with efficiency η_{D1} , and the direct converter losses can be partially recovered in a thermal converter with efficiency η_{T5} .

Fig. 10. The power efficiency of the neutralizer as a function of the energy of the deuterium atoms. The dashed lines represent extrapolated data; (p) = plasma neutralizer cell; (g) = gas neutralizer cell.

Neutral Beam Trapping in the Reactor Plasma

A fraction of the neutral beam that enters the reactor plasma is trapped in the plasma. The remainder of the neutral beam either penetrates through the plasma or charge-exchanges with plasma ions that do not undergo a reionization collision. Thus,

$$f_T + f_p + f_w = 1, \quad (10)$$

where f_T and f_w are defined for Eq. (5) and f_p is the fraction of the neutral beam that penetrates through the plasma.

Riviere²² has estimated the fraction of the neutral beam that penetrates a reactor plasma with cylindrical symmetry for atoms injected normal to the plasma axis as

$$f_p = \exp \left[- \int_0^D \frac{n \langle \sigma v \rangle_T}{v_0} dx \right] \quad (11)$$

for a beam diameter much less than the plasma diameter. D is the reactor plasma diameter, n is the reactor plasma density, v_0 is the velocity of the injected atoms, and $\langle \sigma v \rangle_T$ is the total reaction rate coefficient for collisional processes. For $\langle \sigma v \rangle_T$ assumed constant, the integral can be replaced with $Dn\langle \sigma v \rangle_T/v_0$ where n is an averaged density.

Hunt has estimated the fraction of the injected neutral beam that charge-exchanges with the reactor plasma ions, resulting in somewhat randomly directed neutrals.²³ These neutrals may escape the plasma before ionization occurs and deposit their energy on the first wall of the reactor. Hunt assumes that they pass through the plasma of uniform density, n , with an average path length equal to the plasma radius. He then estimates the fraction of the injected beam striking the reactor first wall to be given by the product of the fraction of the injected neutral beam that undergoes charge-exchange in the plasma and the probability that the neutrals formed by charge-exchange will not be reionized in the plasma, i.e.,

$$f_w = \left\{ \frac{\langle \sigma v \rangle_{cx}}{\langle \sigma v \rangle_T} \left[1 - \exp \left(- \frac{Dn \langle \sigma v \rangle_T}{v_o} \right) \right] \right\} \exp \left[- \frac{1}{2} \left(\frac{Dn \langle \sigma v \rangle_T}{v_o} \right) \left(\frac{\langle \sigma v \rangle_i}{\langle \sigma v \rangle_T} \right) \right], \quad (12)$$

where $\langle \sigma v \rangle_{cx}$ is the total reaction rate coefficient for charge exchange, $\langle \sigma v \rangle_i$ is the total reaction rate coefficient for ionization by both ions and electrons, and

$$\langle \sigma v \rangle_T = \langle \sigma v \rangle_i + \langle \sigma v \rangle_{cx}.$$

Thus, combining Eqs. (10)-(12), the fraction of the injected neutral beam that is trapped is given by

$$f_T = \left[1 - \exp \left(- \frac{Dn \langle \sigma v \rangle_T}{v_o} \right) \right] \left\{ 1 - \frac{\langle \sigma v \rangle_{cx}}{\langle \sigma v \rangle_T} \exp \left[- \frac{1}{2} \left(\frac{Dn \langle \sigma v \rangle_T}{v_o} \right) \left(\frac{\langle \sigma v \rangle_i}{\langle \sigma v \rangle_T} \right) \right] \right\}. \quad (13)$$

For a representative case for a mirror machine

$$\gamma = \frac{Dn \langle \sigma v \rangle_T}{v_o} = 3.$$

Using Riviere's²² summary of reaction rate coefficients for a deuterium atom beam of energy E injected into a mirror reactor with an ion energy distribution from Kuo-Petravic et al.,²⁴ the trapping fraction, wall fraction, and penetrating fraction of the injected beams are as shown in Fig. 11. Note that the trapping fraction and wall fraction do not include the effects of the alpha population in the reactor plasma.

The effect of the alpha particles in the plasma on the trapping of the deuterium atoms is small for deuterium atoms energies >150 keV and decreases with an increase in deuterium atom energy for an alpha particle density $<10\%$ of the plasma density.

The energy of the injected neutral atoms that impinge on the first wall of the reactor after a charge-exchange with the plasma can be partially recovered in a thermal converter of efficiency η_{T6} . The energy of the

Fig. 11. The fate of the injected deuterium beam as a function of the atom energy for a mirror reactor with $\gamma \equiv \langle \sigma v \rangle_{TD} D / v_0 = 3$. The fraction of the beam that is trapped is denoted by f_T , the fraction that penetrates the plasma by f_p , and the fraction that charge-exchanges without reionization by f_w .

injected neutral beam that penetrates the plasma can be partially recovered by stripping an electron from the neutral and capturing the ion formed in a direct converter topping cycle of efficiency η_{D2} , and the direct converter losses can be partially recovered in a thermal converter of efficiency η_{T7} .

Recovery of Injection System Losses

Injection system losses are recovered by direct and thermal energy converters. The efficiencies of the energy recovery components are assumed to be invariant with the energy of the injected atoms. We assume the direct conversion of the energy of the charged particles leaving the neutralizer to be 90%. An experimental direct converter consisting of a gridded Faraday cup has achieved a direct conversion efficiency of 95% for a monoenergetic hydrogen ion energy of 1.8 keV¹⁶ with a power density of 0.12 W/cm² and a current density of 0.07 mA/cm². At higher power and current densities and if the vacuum pump power and other requirements are considered, the efficiency of this direct converter may be only 90%. Another type of direct converter also has an estimated conversion efficiency of 90% (Ref. 25). The thermal converter recovering the direct converter losses and the losses to the reactor first wall is assumed to be 45% efficient. This efficiency is based on a cycle operating at 60% of a Carnot cycle with source and sink temperatures of 900°C and 27°C respectively.

The injected atoms that penetrate the reactor plasma may be stripped of an electron and the energy partially recovered in a direct converter. For the purpose of this study, we assume this direct conversion efficiency to be 70%. We assume the losses that occur in the source, alkali metal vapor cell, and accelerators to be partially recovered in thermal converters with an efficiency of 25%. Because all the thermal converter efficiencies are based on a 27°C sink temperature, no additional thermal conversion can be performed on the injector system.

Overall Efficiency of Injection

Figures 12 and 13 show the overall system efficiency of a deuterium atom injection system as a function of the injected deuterium atom energy for variety of deuterium ion types. The highest overall injection efficiency is obtained using negative deuterium ions produced in the source and neutralized in a plasma (Fig. 13, upper curve). However, of the other ions, the positive deuterium ions have the highest overall injection system efficiency at low injection energies. At higher injection energies, the negative deuterium ions have the highest overall injection efficiency. At very high energies, the efficiency of the + to - cell does not have much effect on the overall injection system efficiency.

Efficiency curves for tritium atom injection systems are similar to those shown for deuterium.

Discussion

Using the assumptions made in this study, we can make a number of important generalizations for neutral hydrogen isotope injection.

1. The highest overall injection efficiency can be achieved using negative ions produced in the source and neutralized in a plasma cell.
2. For injection energies >100 keV, the use of negative ions can achieve overall injection efficiencies in the range of 80 to 90%.
3. Below ~ 150 keV (210 keV for T^+), D^+ results in a higher overall injection efficiency than the D_2^+ and D_3^+ ions studied.
4. Below 100 keV the overall injection system efficiency decreases because plasma ions are lost by charge-exchange with the neutral beam.

The Specific Case

Our reactor system will require the injection of 550 keV deuterium and tritium atoms into a 620 cm diameter plasma with a density of 1.2×10^{14} per cubic centimetre. The injection system will use negative

Fig. 12. The efficiency of the injection system as a function of the energy of the injected deuterium atoms, where $q = 0.5$ keV, $\eta_{Ti} = 0.45$ ($i = 1 \dots, 7$), $\eta_{D1} = \eta_{D2} = 0.9$, $\eta_{a1} = \eta_{a2} = 0.92$, and $E^+ = 1.5$ keV.

Fig. 13. The efficiency of the injection system as a function of the energy of the injected deuterium atoms, where $q = 0.5$ keV, $\eta_{Ti} = 0.25$ ($i = 1, 2, 3, 4$), $\eta_{Ti} = 0.45$ ($i = 5, 6, 7$), $\eta_{D1} = 0.9$, $\eta_{D2} = 0.7$, $\eta_{a2} = 0.92$, $\eta_{+-} = 1$, and $E^+ = 0$.

hydrogen isotopes produced in single-stage cesium vapor cells. Table 4 summarizes the component performances that are assumed independent of the hydrogen isotope and injection energy. Table 5 summarizes the overall injection system efficiencies as well as the component performances that are dependent on the hydrogen isotope and injection energy for deuterium and tritium. The overall efficiency of both the deuterium and the tritium injection system is between 87 and 90%, depending on the type of neutralizer used. For the present reactor design, the overall efficiency of the injection system is taken to be 88%.

Table 4. Energy-independent component performances.

Ion source energy	$q = 0.5 \text{ keV per ion}$
Thermal efficiencies	$\eta_{T1} = \eta_{T2} = \eta_{T3} = \eta_{T4} = 25\%$ $\eta_{T5} = \eta_{T6} = \eta_{T7} = 45\%$
Direct converter efficiencies	$\eta_{D1} = 90\%$ $\eta_{D2} = 70\%$
Negative ion production efficiencies	$\eta_{+-} = 20\%$
Accelerator efficiency	$\eta_{a1} = \eta_{a2} = 92\%$

Table 5. Overall injection system efficiencies.

	Deuterium	Tritium
Negative ion production energy required, E^+	1.5 keV	3.2 keV
Attenuation factor, γ	4.4	6.3
Trapping fraction, f_T	98%	99%
Fraction of injected beam power to reactor wall, f_w	1%	1%
Neutralization fraction, f_n (p)	88%	88%
f_n (g)	68%	68%
Injection system efficiency, η_{sys} (p)	90%	90%
η_{sys} (g)	87%	87%

An ion source has been developed at Lawrence Berkeley Laboratory that can deliver up to 10 A in a 20 to 30 msec pulse of neutral deuterium atoms and molecules with maximum energies of 20 keV into the 2XII target plasma. The source operates without any externally applied magnetic field, making the extractor ion optics independent of the charge-to-mass ratio of the ion species involved. The source extractor consists of multiple-aperture slots; the slots increase the transparency and the ion current density at the emitting surface more than a multiple aperture with circular holes.

The accelerated ion beam undergoes charge-exchange, using the gas from the source, in a gas cell immediately adjacent to the extractor. A pulsed magnet then sweeps out the charged fraction of the beam.

At 20 keV the extracted current is 13 to 16 A. About 85% of the beam consists of ~75% D^+ , 18% D_2^+ and 7% D_3^+ . The neutral beam density was measured as 0.037 A/cm^2 at a distance of 2.7 m from the exit of the neutralizer.²⁶

The 10 A source is being upgraded by using curved grids to focus the beam in one dimension. A 10 msec pulsed 50 A source is also under construction.

NEUTRONICS

Introduction

Each DT fusion reaction releases 80% of its energy as a 14 MeV neutron. These neutrons must be used to produce tritium to fuel the fusion reaction, their kinetic energy must be converted to thermal energy, and they are also used to generate additional energy. These three processes take place in a lithium blanket that surrounds the plasma. Tritium production is the result of both the ${}^6\text{Li}(n,\alpha)\text{T} + 4.8 \text{ MeV}$ and the ${}^7\text{Li}(n,n'\alpha)\text{T} - 2.47 \text{ MeV}$ reactions.

An acceptable blanket design will be a compromise between neutronic requirements, mechanical requirements, and economics. Our blanket design

employs liquid lithium contained in a large number of individual tubes that surround the plasma to intercept the neutrons. The tubes closely follow the magnetic field lines, as is described in more detail in the section on Blanket Energy Conversion System. The design must also consider the necessity for shielding the superconducting magnet that confines the DT plasma from both primary neutrons and secondary neutrons and gammas.

Spherical Shell Neutronic Model

To estimate the blanket's neutronic performance, we used Monte Carlo transport methods to analyze a series of calculational models. Three Monte Carlo codes were employed: a 66 energy group neutron transport code called SORS,²⁷ a 175 energy group neutron code called TART,²⁸ and a version of MORSE²⁹ employing a coupled 30 group neutron, 15 group gamma library.³⁰ Cross sections used were derived from the LLL Evaluated Nuclear Data Library.³¹

The first and simplest neutronic model of the blanket is a set of concentric spherical shells surrounding a 3.2 m radius spherical source of isotropic 14 MeV neutrons. The first wall radius is at 4.8 m. The blanket consists of a 30 cm thick inner region and a 70 cm thick outer region. Table 6 gives the compositions of these regions.

Table 7 gives results of the MORSE-L Monte Carlo transport analysis of the spherical shell blanket model. The tritium breeding ratio is 1.37. The calculated blanket energy generation of 15.5 MeV per 14.1 MeV neutron results in a blanket energy multiplication factor of only 1.10. Note that the blanket is not neutronically optimized. There is a trade-off between tritium breeding and energy generation that has not been exercised.

Most of the blanket model assumes homogeneous mixes of material. However, it was necessary to estimate heating rates of individual materials so we could in turn estimate neutron energy deposition in the first wall. For example, we modeled a 0.1 cm thick shell of stainless steel located at the plasma-blanket interface. At a 14 MeV wall loading of 1.63 MW/m^2 ,

Table 6. Material composition and atom densities of spherical shell model.

Region	Composition (volume fraction)	Atom densities (atoms/cm-barn)
Plasma and vacuum	0.5 deuterium	${}^2\text{H} \ 1 \times 10^{-8}$
	0.5 tritium	${}^3\text{H} \ 1 \times 10^{-8}$
Inner blanket	0.85 lithium	${}^6\text{Li} \ 0.00133$
	(4% ${}^6\text{Li}$) ^a	${}^7\text{Li} \ 0.0317$
	0.05 SS ^b	Fe 0.00336
	0.10 void	Ni 0.00084
Outer blanket	0.40 lithium	${}^6\text{Li} \ 0.000624$
	(4% ${}^6\text{Li}$)	${}^7\text{Li} \ 0.0149$
	0.40 graphite	C 0.0321
	0.10 SS	Fe 0.00672
	0.10 void	Ni 0.00168
Shield	0.50 lead	Pb 0.016
	0.40 kerosene	H 0.027
		C 0.0138
	<0.01 boron	B 0.0002
	0.10 void	
Coil	1.00 copper	Cu 0.0759

^aNatural lithium is 7.56% ${}^6\text{Li}$. We use depleted lithium to demonstrate that isotopic enrichment of lithium is unnecessary.

^bSS is an approximation for stainless steel because chromium cross sections were not available.

we estimated the neutron energy deposition rate in the first wall to be 9 W/cm^3 .

Table 8 compares SORS, TART, and MORSE-L code calculations for tritium breeding in the spherical shell model; Table 9 makes the same comparison for energy deposition. Tritium breeding is in virtual agreement, but there is a 10% spread in blanket energy generation. The comparison between MORSE-L neutron and MORSE-L coupled neutron gamma problems (Table 9) shows that one effect of gamma transport is a reduction of energy deposition in the thin "stainless steel" zones (2 and 7). This encouraging result can be only tentatively accepted pending further investigation because the lower cutoff of the gamma group structure is 0.5 MeV and the transport of the 0.478 gamma produced in lithium was not included in the calculation.

Table 7. Tritium breeding and energy generation in the spherical shell model (MORSE-L results).

Zone No.	Material	Thickness (cm)	Volume (cm ³)	⁶ T/n ^a	SD ^b	⁷ T/n ^a	SD ^b	Energy (MeV/neutron)	SD ^b	P ^c (W/cm ³)
1	Plasma									
Inner blanket										
2	SS	0.1	2.90 × 10 ⁵					0.075	0.016	8.66
3	Li	0.9	2.61 × 10 ⁶	0.012	0.023	0.012	0.010	0.299	0.007	3.84
4	Li,SS	9	2.67 × 10 ⁷	0.114	0.017	0.147	0.015	2.925	0.012	3.68
5	Li,SS	20	6.28 × 10 ⁷	0.285	0.011	0.228	0.013	5.295	0.012	2.85
Outer blanket										
6	Li	0.9	2.95 × 10 ⁶	0.017	0.014	0.008	0.019	0.208	0.008	2.37
7	SS	0.1	3.28 × 10 ⁵	-	-	-	-	0.048	0.032	4.93
8	C	1.0	3.29 × 10 ⁶	-	-	-	-	0.209	0.014	2.12
9	Li,SS,C	28	9.74 × 10 ⁷	0.289	0.010	0.054	0.016	4.549	0.011	1.57
10	Li,SS,C	40	1.58 × 10 ⁸	0.188	0.014	0.014	0.040	1.891	0.010	0.40
Blanket totals		100		0.905	0.006	0.463	0.006	15.499		
						0.905				
						1.368	0.004			
Shield	Pb,C _x H _y	50	2.30 × 10 ⁸	-	-			0.315	0.090	0.05
Coil	Cu	60	3.29 × 10 ⁸	-	-			0.003	0.461	0.003
Totals		210		0.905	0.006			15.817		

^a⁶T/n and ⁷T/n are ⁶Li(n,α)T and ⁷Li(n,n',α)T reactions per source neutron.

^bStandard deviation.

$$\begin{aligned}
 \text{Power density, } P &= \frac{\text{fusion power}}{\text{energy per fusion}} \times \frac{\text{energy}}{\text{volume}} \\
 &= \frac{5.9 \times 10^8 \text{ (W)}}{17.6 \text{ (MeV)}} \times \frac{\text{energy (MeV)}}{\text{volume (cm}^3\text{)}}
 \end{aligned}$$

$$\text{Average blanket power density for a wall loading of } 1.63 \text{ MW/m}^2 = \frac{P_{\text{blanket}}}{\text{total volume}} = \frac{5.9 \times 10^8}{2.13 \times 10^8} \times \frac{15.5}{17.6} = 2.42 \text{ W/cm}^3$$

Table 8. Comparison of calculated tritium breeding in spherical shell model by three Monte Carlo codes.

Zone	${}^6\text{T/n}$			${}^7\text{T/n}$		
	SORS	TART	MORSE-L	SORS	TART	MORSE-L
3	0.012	0.012	0.012 (0.023) ^a	0.018	0.019	0.012 (0.010) ^a
4	0.102	0.107	0.114 (0.017)	0.147	0.146	0.147 (0.015)
5	0.262	0.279	0.285 (0.011)	0.256	0.231	0.228 (0.013)
6	0.016	0.018	0.017 (0.014)	0.010	0.009	0.008 (0.019)
9	0.248	0.292	0.289 (0.010)	0.062	0.054	0.054 (0.016)
10	0.181	0.203	0.188 (0.014)	0.015	0.013	0.014 (0.040)
	0.821	0.911	0.905 (0.006)	0.508	0.472	0.469 (0.006)
				0.821	0.911	0.905
Totals				1.329	1.383	1.374 (0.004)

^aThe numbers in parentheses are the fractional standard deviations associated with the previous quantity.

Table 9. Comparison of energy deposition in MeV in spherical shell model calculated by three Monte Carlo codes.

Zone	Neutron problem						Coupled problem	
	SORS	SD ^a	TART	SD ^a	MORSE-L	SD ^a	MORSE-L	SD ^a
SS 2	0.188	0.006	0.145	0.014	0.141	0.010	0.075	0.016
3	0.282	0.013	0.303	0.011	0.282	0.007	0.299	0.007
4	3.06	0.009	3.028	0.008	3.040	0.011	2.925	0.012
5	5.72	0.016	5.382	0.005	5.349	0.009	5.295	0.012
6	0.201	0.021	0.211	0.01	0.195	0.008	0.208	0.008
SS 7	0.109	0.020	0.072	0.015	0.070	0.017	0.048	0.032
C 8	0.312	0.020	0.321	0.01	0.204	0.016	0.209	0.014
9	5.48	0.006	4.595	0.011	4.542	0.010	4.549	0.011
10	2.05	0.034	1.789	0.028	1.764	0.010	1.891	0.010
11	0.256	0.096	0.236	0.064	0.250	0.097	0.315	0.090
12	0.002	1.000	0.0001	1.0	0.004	0.423	0.003	0.461
Totals	17.66		16.0821		15.841		15.817	

^aStandard deviation.

Two-Dimensional Models

Because the one-dimensional homogeneous spherical shell model does not conform to the Yin Yang geometry, we need some understanding of how variations in geometry can affect blanket neutronic performance. We developed three two-dimensional models for analysis by the SORSN Monte Carlo neutron transport code. The first deals with the overall reactor geometry, including penetrations for plasma leakage and injection. The second and third explore the heterogeneous effects of a blanket consisting of individual tubes containing lithium.

Overall Yin Yang Geometry Model

Figure 14a is a quarter section view of the model developed to study the overall Yin Yang geometry effects. The complete geometry is generated by rotation about the Z axis. The resulting surfaces of revolution are used to define the model's zone boundaries and can be expressed as analytical functions acceptable to SORSN. The lower hemisphere of the model is a mirror image of the upper and is included by making the $Z = 0$ plane a reflecting boundary. Note that this model is not an exact mockup of Yin Yang geometry. To satisfy SORSN geometry restrictions, half of the Yin Yang geometry is rotated 90° . To illustrate the effect of this rotation on the Yin Yang geometry, Fig. 14b shows the Yin Yang geometry before and after rotating the right hemisphere 90° . No plasma, blanket, shield, or coil surface areas or volumes are changed by this modification. The resulting symmetric Yin Yang reactor neutronic model is an improvement over the one-dimensional spherical shell geometry and should be capable of showing gross geometry effects.

The model specifications for the reactor (as shown in Fig. 14a) follow:

1. The neutron source is a DT plasma contained in a 3.2 m radius spherical volume. It provides isotropic 14.1 MeV neutrons.
2. The neutronically void region includes the space between the blanket and the neutron source and the 0.75 m radius cylindrical regions along the Z-axis, providing a total selective leakage area of 3.5 m^2 .

a. Quarter section of reactor model

b. Effect on plasma geometry of a 90° rotation of the right hemisphere

Fig. 14. Yin Yang reactor model.

3. The blanket is 1.0 m thick near the neutron source and 0.5 m thick in the mirror regions. The closest approach of the blanket to the center of the plasma is 4.4 m. The thick portion of the blanket consists of a 0.3 m inner region plus a 0.7 m outer region. The thin portion of the blanket and the blanket plugs have the same composition as the 0.3 m section of the thick portion.
4. The primary shield region is 1.0 m thick near the neutron source and 0.75 m thick in the mirror regions.
5. The coil regions are 1.0 m thick.
6. The filler and secondary shield region occupies the space between the coils, primary shield, and the spherical outer boundary of the reactor model.
7. Material compositions are the same as those used for the spherical shell model (see Table 6).

We ran five SORSN Monte Carlo neutron transport problems with geometries based on this model. Run #1 investigated the change from the simple spherical shell model to the more exact Yin Yang model. Run #2 investigated the opening of the entire mirror circumference for leakage by removing the 1.5 m thick blanket plug occupying the outer 4 m of the mirror region. This unplugging will be necessary if selective plasma leakage through small ducts is not feasible. Runs #3 through 5 investigated the effect of injection area variation. Figure 15 shows the model modification used to include the injector void region. Injection areas of 5, 25, and 50 m² were analyzed; these areas refer to the first wall area occupied by the injection region. For the required total injection current of 890 A, the resulting current densities are 0.018, 0.0036 and 0.0018 A/cm². Table 10 gives results of these five runs plus the results from the simple spherical shell model with the same material.

Results from the Yin Yang reactor model (run #1) were encouraging. The blanket tritium breeding and energy generation per source neutron were 1.28 atoms and 16.9 Mev, compared to the simple spherical shell model results of 1.33 atoms and 17.4 MeV.

Fig. 15. Yin Yang reactor model with injection duct.

Table 10. Tritium breeding (T) and energy generation (E) in blanket and shield for Yin Yang reactor model and spherical shell model.

Run No.		Blanket		Shield	
		T (atoms/ fusion)	E (MeV/ fusion)	SD ^a (%)	E (MeV/ fusion) SD ^a (%)
1	Basic Yin Yang reactor model has a 3.5 m ² mirror leakage penetration (0.9% of first wall spherical area) ^b	1.28	16.9	1	0.42 12
2	Same as run #1 except has open mirrors (55 m ² instead of 3.5 m ²) occupying 14.5% of first wall spherical area	1.18	16.0	1	0.47 9
3	Same as run #1 plus a 5 m ² injection area (2.1% of first wall spherical area)	1.23	16.7	1	0.59 11
4	Same as run #1 plus a 25 m ² injection area (10.7% of first wall spherical area)	1.10	15.1	1	2.02 7
5	Same as run #1 plus a 50 m ² injection area (21.4% of first wall spherical area)	0.96	13.2	1	3.65 4
	Spherical shell model	1.33	17.4	1	0.26 ~10

^aStandard deviation. SORS calculated energy SD only. Statistical uncertainties in tritium breeding are expected to be nearly the same.

^bFirst wall spherical areas are 380 m² at the leakage duct-first wall intersection and 243 m² at the injection duct-first wall intersection. The intersection radii are 5.5 and 4.4 m.

Removing the 1.5 m thick blanket plug (run #2) reduced blanket tritium breeding by 8% and energy generation by 5%. The effects of providing injection ducts through the blanket and terminating in the shield with first wall areas of 5, 25, and 50 m² (runs 3 through 5) were reductions in tritium breeding of 4, 14, and 25%, and reductions in blanket energy deposition of 1, 10, and 22% when compared with the basic model (run #1). Fractional spherical areas of the 5, 25, and 50 m² injection ducts (calculated at the first wall radius) were 2.1, 10.7, and 21.4%.

Void Effects in a Cylindrical Shell Neutronic Model

We formulated two heterogeneous neutronic models to investigate neutron streaming effects in a blanket comprised of individual coolant tubes

containing lithium. The first heterogeneous model is shown in Fig. 16 and consists of a central cylindrical void surrounded by alternating cylindrical shells of blanket material and void. This model was formulated to test the effects of axial streaming in voids parallel to the blanket tubes. The central cylindrical void is a 1.0 m radius and contains a 14.1 MeV point source at its center. It is surrounded by four blanket material zones of approximately equal thickness separated by three void zones of approximately equal thickness. The outermost blanket zone is surrounded by a shield zone. For all cases, the total thickness of the blanket zones is 90 cm, and the shield zone is 50 cm thick. The total thickness of the void zones was varied from 10 cm (10% void) to 90 cm (50% void). The overall length of the model is 26 m. The ends of the cylinder are leakage planes.

We performed two sets of Monte Carlo calculations with this model. The first employed an isotropic point source, the second a monodirectional point source directed normal to the cylindrical axis. In both sets a 100 cm thick completely homogeneous blanket was compared to heterogeneous blankets with increasing void thicknesses. The homogeneous blanket material density was taken to be 10% below the blanket material density used for the runs with annular voids. This was done to maintain constant the amount of blanket material a neutron would pass through in radially traversing the blanket. Table 11 shows the results. No degradation in blanket performance is observed when the parallel void cases are compared to the homogeneous blanket cases.

Void Effects in a Tube Bank Neutronic Model

The second heterogeneous neutronic model is shown in Fig. 17. The model consists of five blanket cells stacked in a vertical array centered on the X-axis. Each cell is 10 cm wide (Y dimension), 20 cm high (X dimension), and 24 m long (Z dimension). Each cell contains half a lithium-filled tube and the surrounding void. The cell array is bounded on its sides and bottom by reflective boundary planes at $Y = -5$ and $+5$ cm and at $X = 0$ cm. The side reflective boundary planes effectively transform the five-cell array into a heterogeneous slab infinite in the Y-dimension.

Fig. 16. Cylindrical shell model.

a. Staggered tubes

b. In-line tubes

Legend

Reflective boundary zones - 1, 2, 3

Shield zone - 19

Fig. 17. Heterogeneous tube bank model.

Table 11. Results of cylindrical shell model calculations.

	Blanket void thickness (%)	Tritium breeding (atoms/fusion)	Blanket energy (MeV/fusion)	SD ^a (%)	Shield energy (MeV/fusion)	SD ^a (%)
Isotropic source	0 (Homogeneous)	1.44	15.8	~1	1.13	6
	10	1.45	15.9	~1	1.18	5
	18	1.46	16.0	~1	1.19	5
	36	1.44	15.9	~1	1.16	5
	50	1.43	15.8	~1	1.21	6
Mono- directional source	0 (Homogeneous)	1.41	15.6	~1	1.53	6
	10	1.40	15.5	~1	1.51	5
	18	1.41	15.6	~1	1.50	6

^aStandard deviation.

The bottom reflective boundary plane reflects all neutrons attempting to cross the $X = 0$ plane. The cell array upper boundary is a 50 cm thick shield zone topped by a leakage boundary plane. End leakage boundary planes are located at $Z = -12$ and $+12$ m.

We considered two different tube bank configurations. The first configuration used closely packed tube arrays (20 cm outside tube diam, 0.32 cm tube wall thickness, 20 cm X dimension spacing) of both staggered and in-line array geometry. These arrays represent the largest void fractions ($\approx 22\%$) that are necessary in the actual multitubular blanket. The tube wall material occupies $\sim 5\%$ of the cell volume. The second tube bank configuration used loosely packed tube arrays (void fraction $\approx 61\%$, tube wall $\sim 2.5\%$) of both staggered and in-line array geometries (14.1 cm outside tube diam, 0.23 cm tube wall thickness, 20 cm X dimension spacing).

The source for this neutronic model is a line source of 14 MeV neutrons located on the Y -axis between $Y = -4.9$ and $+4.9$ cm. Two 14 MeV source neutron velocity distributions were used. The first was uniformly distributed in a 45° cone normal to the YZ plane; the second was mono-directional and normal to the YZ plane. The first velocity distribution more closely approximates the expected distribution.

Table 12 gives the results of Monte Carlo neutron transport calculations for the closely packed and loosely packed tube bank configurations. For each configuration we calculated a case with homogeneous distribution of equivalent amounts of lithium, stainless steel, and void. For the closely packed configuration, we also calculated a case with tubes represented by homogeneous cylinders of lithium and stainless steel.

The set #1 results (closely packed tubes, 22% void fraction) are the same for all cases. There are no detectable heterogeneous effects in a blanket consisting of a closely packed five layer tube array. It follows that for overall neutronics calculations, arrays of five or more layers of closely packed tubes can be accurately approximated by their homogeneous equivalents.

The set #2 results (loosely packed tubes, 61% void fraction) indicate a small but detectable degradation in performance for the heterogeneous cases. When the distributed velocity source is used, there is a 4 to 5% decrease in blanket performance. When the monodirectional source is used, there is no decrease for the staggered tube case, but for the in-line tube case there is a 15% decrease in blanket tritium breeding and energy generation. The monodirectional source results are not surprising; the in-line tube case has an open X-direction window while the staggered tube case has no window and, in fact, has a nearly uniform Y-direction distribution of moderating material. The monodirectional source calculations demonstrate the limiting case; the distributed source approximates the actual situation. We conclude that heterogeneous effects can be important in loosely packed tube arrays, but the degradation in blanket performance is quite small for void fractions likely to be used.

We can conclude, then, that blankets can be made of parallel arrays of individual tubes containing lithium and separated by voids without compromising energy generation or tritium breeding capabilities.

Table 12. Tube bank model results.

	Case No.	Geometry	14 MeV neutron source velocity distribution	Tritium breeding (atoms/fusion)	Blanket energy (MeV/fusion)	SD ^a (%)	Shield energy (MeV/fusion)	SD ^a (%)
Set #1 - Closely packed tubes (22% void fraction)	1	Equivalent homogeneous blanket	Uniformly distributed in a 45° cone normal to the Y-Z plane	1.40	15.8	1	1.47	4
	2	Staggered heterogeneous tubes		1.41	15.9	1	1.57	5
	3	Staggered homogeneous tubes		1.42	15.9	1	1.43	5
	4	In-line heterogeneous tubes		1.41	15.9	1	1.50	4
Set # 2 - Loosely packed tubes (61% void fraction)	5	Equivalent homogeneous blanket	Uniformly distributed in a 45° cone normal to the Y-Z plane	1.01	11.9	2	4.95	3
	6	Staggered heterogeneous tubes		0.96	11.4	2	5.48	3
	7	In-line heterogeneous tubes		0.97	11.5	2	5.69	4
	8	Equivalent homogeneous blanket	Vertical (monodirectional)	0.92	10.9	2	5.81	2
	9	Staggered heterogeneous tubes		0.92	10.9	2	5.63	3
	10	In-line heterogeneous tubes		0.78	9.14	2	7.66	4

^aStandard deviation.

Blanket Afterheat

Primary and secondary neutrons activate blanket materials. The decay of these activated materials will generate thermal energy (called afterheat) in the blanket. To estimate the magnitude of the afterheat, we did the following:

1. We used the TART code and the spherical shell model to calculate the activation reaction rates in the blanket. Reactions found to occur were $(n, 2n)$, $(n, n'p)$, (n, p) , (n, d) , (n, α) , and (n, γ) . Blanket materials in the model were the isotopes ^6Li , ^7Li , and ^{12}C , and the elements iron and nickel. Chromium was not included in the stainless steel composition because cross sections were not available. The effect of this omission on afterheat is unknown.
2. We calculated the activity of each radioactive isotope in the blanket by assuming equal production and decay rates. This assumption will overpredict the activity of isotopes whose half-lives are longer than the reactor operation time before shutdown.
3. We used TART to calculate the activation reactions of iron and nickel. Because we could not assign fractions of each reaction to individual isotopes, we calculated the worst case for each element by using the isotope having the largest total decay energy. For example, all $\text{Fe}(n, 2n)$ reactions were assumed to occur in ^{54}Fe because the total energy release of the decay chain is higher (4.2 MeV) than from the $(n, 2n)$ activation decay chains of the other isotopes of iron. The actual afterheat may be considerably less than the calculated upper bound.

Results of this analysis indicate that effectively all the decay heat in this blanket results from activation of iron and nickel. Activation of lithium by the $^7\text{Li}(n, \gamma)^8\text{Li}$ reaction will add ~10% to the iron and nickel results, but since the half-life of ^8Li is 0.84 sec, it will decay away so fast that its effect is not included. The calculated upper bound for the total afterheat (at zero time after reactor shutdown) is 19 MW, or ~3.5% of blanket design power.

Afterheat power density vs radial position in the blanket is plotted in Fig. 18. The discontinuity at 30 cm occurs because the volume fraction of structural material (iron plus nickel) changes from 5% to 10%. To obtain afterheat density in the structural material itself, multiply the values in Fig. 18 by the reciprocal of the volume fraction. For example, the upper bound afterheat power density in the first wall is $0.13 \times 20 = 2.6 \text{ W/cm}^3$.

The afterheat must be removed to prevent damage to the reactor blanket. Lithium flow normally provides sufficient cooling, but if lithium flow should stop, an alternative method of cooling becomes necessary. Circulating 18 kg/sec of helium at 1 atm around the outside of the blanket tubes will result in a maximum tube temperature of 630 K. This is below permissible operating temperatures for stainless steel. The helium pumping power required to remove the afterheat from the reactor is ~20 kW.

Shielding the Superconducting Coil

Removing heat from cryogenic superconducting magnets requires an estimated 500 to 1000 W per watt of energy deposited because the Carnot efficiency at cryogenic temperatures is <1%. The magnet structure must therefore be well shielded from primary neutrons produced by the fusion reaction and secondary neutrons and gammas produced in the blanket. We used the ANISN-L discrete ordinates transport code, with a coupled 30 group neutron, 15 group gamma cross section library to predict heating in the coil of the spherical shell model. We ran problems using shield thicknesses of 50, 100, and 150 cm to estimate coil heating. The shield in these problems is half lead and half kerosene. We obtained the results shown in Table 13.

Table 13. Coil heating as a function of shield thickness.

Shield thickness (cm)	MeV/source neutron	Energy deposited in coil (W)
50	1.38×10^{-3}	60,000
100	1.14×10^{-5}	500
150	8.89×10^{-8}	3

Fig. 18. Afterheat in blanket structure. Power density is based on a first wall loading of 1.63 MW/m^2 .

The Yin Yang model allows for an effective shield thickness of ~100 cm. Therefore, predicted coil heating is ~500 W, which will require refrigeration power in the range of 250 to 500 kW.

Neutron Trap

We did a series of MORSE-L Monte Carlo calculations to study the streaming of neutrons through the ion exit area of the mirror. The ion beam emerging from the mirror is turned 90° in a relatively small space using a supplementary solenoidal coil, but there are no coil conductors in the direct path of the neutron stream (see Fig. 19). Thus, we envision a well-collimated neutron beam directed across a relatively narrow gap into a trap that will reduce the number of neutrons back-scattered into the expander magnets. The trap can be either a plate or a cavity. This approach furnishes an effective method for locally isolating the neutrons and keeping them out of the expander.

A complete study of neutron streaming must investigate a range of parameters such as duct radius, radius-to-length ratio, gap width, trap depth, and materials for shielding and trap lining. We studied only one configuration, that shown in Fig. 20. This configuration has a duct area of 1.5 m^2 and other dimensions consistent with the Yin Yang coil and expander magnet design. The gap between mirror end and the neutron trap was determined from the ion beam bending calculation. Preliminary calculations with a cosine-distributed source at the reactor end of the duct gave no energy deposition in the expander magnets because the code for the most part followed neutrons in the duct lining. Subsequent calculations with a monodirectional beam directed up the duct gave the results shown in Table 14. Thus, the neutrons made their first collision in the cavity. Only the first 100 cm wide ring of the expander coil is exposed to first-scattered neutrons. As a result, its energy deposition rate is larger than in the outer rings, which are partially shaded from the trap. We see from Table 14 that the cavity trap reduced energy deposition in the expander coil by an order of magnitude more than the plate.

Fig. 19. Cross-sectional view through the mirror reactor with direct converter.

Fig. 20. Neutron streaming calculational geometry.

Table 14. Expander coil heating by streaming neutrons.

Radii of expander coil region, 50 cm thick (cm)	Neutron energy deposition per streaming neutron in the expander coil region (MeV/cm ³)	
	With cavity trap	With plate
200-300	2.47×10^{-9}	2.24×10^{-8}
300-400	3.14×10^{-10}	1.16×10^{-8}
400-500	9.68×10^{-11}	8.78×10^{-9}

The total neutron power that leaks through the mirror hole is ~0.6 MW. Using the attenuation cited for the 200 to 300 cm region and a coil depth of 50 cm gives a total energy deposition in the expander coil of ~100 W. This is a low value and can be reduced further by shielding placed on top of the coil.

BLANKET ENERGY CONVERSION SYSTEM

Introduction

The blanket must both convert the available neutron energy to a useful thermal output and breed a sufficient amount of tritium to satisfy the system needs. Our blanket is a 1 m thick multilayer array of tubes containing flowing lithium whose routing approximately parallels the field lines created by the Yin Yang field coil.³² This conformal flow minimizes MHD-induced pressure drops.³³ The blanket forms a closed surface surrounding the plasma, which is contained in an approximately spherical volume separated from the blanket first wall by a vacuum region. Figure 21 shows the blanket configuration.

Almost all our analytical effort is devoted to the first wall of the blanket since it is subjected to the most severe conditions. The similar geometry of successive blanket layers makes the first wall design approach applicable to them as well.

There are three interacting problems encountered in the design of a mirror machine blanket: blanket thermal performance, neutronic performance

Fig. 21. Yin Yang magnet and blanket configuration.

(already discussed), and MHD-structural compatibility. The blanket thermal performance is insensitive to the exact path of the lithium flow passages, so it can be analyzed with a generalized geometrical model of the blanket. In contrast, the MHD-structural compatibility and to a lesser degree the neutronic performance of the blanket are sufficiently sensitive to the fine scale geometry of the flow passage array and tube routing as to require the individual evaluation of each blanket design.

Thermal Analysis of the Blanket

Because the blanket thermal analysis is insensitive to small variations in the flow passage array, we developed a generalized model of a typical blanket flow passage. This model is a tube length in the form of a quarter circle arc of constant radius (see Fig. 22). To examine the most rigorous thermal conditions encountered in the blanket, we assumed the flow passage to form an element of the blanket first wall. We modified the results of the spherical shell neutronic analysis by assuming that the local neutronic power density in the tube is proportional to the cosine of the surface inclination angle and inversely proportional to the square of the radial distance of the tube from the plasma (see Fig. 23). From this we obtained the local neutronic power density in the lithium and the stainless steel tube wall. Calculated values of absorbed Bremsstrahlung (3.1 W/cm^2) and synchrotron (2.0 W/cm^2) radiation fluxes were added to the neutronic power density in the structural wall.

The heat transfer analysis includes conductive heat transfer through the wall and magnetic laminar convective heat transfer (Nusselt number = 7.1)³³ from the wall inner surface to the flowing lithium.

For a 6 cm diam tube with a wall thickness of 0.3 cm, the maximum rise in the wall temperature above the lithium bulk temperature was $\sim 25 \text{ K}$. The axial gradient of the lithium bulk temperature was integrated over the entire length of the tube for an assumed lithium bulk flow velocity of 0.2 m/sec. The resulting lithium bulk temperature rise was $\sim 100 \text{ K}$. These values of bulk temperature rise and wall temperature excess are quite

Fig. 22. Quarter circle representative tube element. $R_{FW} \equiv$ radius of curvature of representative tube; $r^* \equiv$ radius of closest tube approach; $r_p \equiv$ radius of plasma.

Fig. 23. Equivalent spherical blanket concept.

modest and indicate that the thermal aspects of the blanket design will be satisfactory as long as the design has MHD-structural compatibility at the assumed flow velocity.

Blanket Coolant Flow Path Design

Although the thermal performance of the convective lithium coolant may be investigated without specifying the exact geometry of the coolant paths, the determination of MHD-structural compatibility requires the consideration of the exact geometrical configuration of the blanket. In our blanket configuration an array of circular arc tubes defines mathematically a surface whose blanket tube elements closely follow the actual field lines only in the mirror regions (see Fig. 24). Between the mirror regions the paths of the circular arc tubes are geometrically similar but not congruent to the actual field lines. We can see in Fig. 24 that each tube of a given blanket layer is defined by specifying a blanket tube radius (R_t), a blanket tube sector angle (β_m), and a blanket location angle (θ). The blanket tube radius and sector angle for a given location angle are mathematically determined by requiring that the blanket tube be tangent to the upper and lower mirror planes at the mirror radius (R_m). This requirement assures that the blanket tubes will form a continuous surface that closely parallels the magnetic field lines in the mirror regions.

Figure 24 shows that if the blanket thickness were held constant and the blanket tubes were merely close-packed at the point of closest approach to the plasma (minimum blanket width), there would be large gaps between the tubes in the mirror regions as a result of the tube paths spreading apart. Moreover, the blanket thickness, limited by the space between the mirror coil shields, would be unacceptably small near the plasma. To alleviate these problems, we allow the blanket tubes to shift their positions slightly relative to one another as they progress through the blanket. This changes the blanket thickness and width in response to blanket geometrical demands while maintaining small void spaces between adjacent tubes. Figure 25 illustrates a variable geometry tube array with both the maximum and minimum void fractions and the relative blanket widths

Fig. 24. Blanket tube geometry. R_t is blanket tube radius, R_m is mirror radius, β_m is blanket tube sector angle, and θ is blanket location angle.

Fig. 25. Variable geometry array. All adjacent tube centers must be one diameter away from any given tube center, i.e., all adjacent tubes touch any given tube. From geometrical considerations (a) $V \equiv$ volumetric void fraction ($0.09 \leq V \leq 0.214$), and (b) $m/m' = t'/t \leq 1.73$.

and thicknesses. The maximum blanket width change allowed by this tube array is slightly less than that required by field geometry (1.7 vs ~2.0). However, a slight spreading of the tubes in the mirror region with an attendant rise in mirror region void fraction is probably acceptable. If necessary, the voids could be plugged or the tube made slightly oval at either the ends or the center. Note that this lateral tube shifting has a negligible effect on the MHD pressure drop.

To demonstrate MHD-structural compatibility, we calculated the MHD pressure drop sustained by lithium flowing along the blanket tube paths as follows: We combined the spatial location and direction of a tube path as described by a mathematical function with the magnetic flux density and field direction obtained from the MAFCO field code. This allowed determination of the parallel and the perpendicular (with respect to the tube path) magnetic field components at any point on a particular tube path. From the perpendicular magnetic field components we calculated incremental MHD pressure drops³³ and summed them to yield the total MHD pressure drop over the tube path from mirror to mirror. An MHD pressure drop of <100 psi was obtained for the maximum flow velocity required by the maximum (first wall) blanket power density (0.2 m/sec). There was surprisingly little variation ($\pm 5\%$) in the MHD pressure drop with angular tube position in the blanket (θ). The total pumping power required for this blanket is quite small ($\sim 0.5\%$ of the blanket thermal power or 2.5 MW). Thus, the circular arc tubular blanket configuration provides a relatively straightforward approach to achieving satisfactory blanket performance with the complex Yin Yang magnetic field geometry.

Blanket Heat Exchanger

The heat exchangers used to transfer energy to turbines can fall into two classes, depending on whether they operate in a high or a low magnetic field. If the heat exchanger operates in a low magnetic field, a liquid metal or liquid-gas heat exchanger can be designed, since MHD

effects can be neglected. If the heat exchanger operates in the high magnetic field near each mirror coil, MHD effects on both the primary and secondary side of the heat exchanger must be considered. In Report #1 we assumed that the blanket flow passages could simply be extended sufficiently far from the mirrors to allow the heat exchanger to operate in a low field region.¹ However, a subsequent study of this approach revealed that it would result in substantial pressure drops and heat transfer losses because of the distances involved in getting to a low field region.

We undertook a limited study on a heat exchanger designed to operate in the regions near the mirrors. In this design the secondary heat transfer medium is assumed to possess negligible electrical conductivity (e.g., helium, metal vapor, steam). Thus, the main design problem reduced to providing a lithium circulation loop with reasonably low MHD pressure drops and an adequate thermal window between the lithium and the secondary heat transfer fluid.

Figure 26 shows a portion of the manifold heat exchanger in the top half of one mirror region. There is an identical unit in the bottom half of the same mirror region, and there are two similar units (without the electromagnetic pump) in the opposite mirror. Adjacent flow passages in each mirror region are connected with a large flow area turnaround. The turnaround has a small MHD pressure drop because of its reduced flow velocity and its reduced wall-to-fluid electrical conductance ratio.

Although the turnarounds may be used to form low pressure drop circulation loops, some means of pumping the lithium around these loops must be provided. We shall use a dc conduction type electromagnetic pump that uses the mirror magnetic field and an orthogonal electric current to pump the lithium across the blanket thickness and through the multipass blanket circulation loop.

The two pump units extend 180° circumferentially around the top and bottom halves of one mirror region (see Fig. 26). The applied pump current flows circumferentially through the top unit and returns through the bottom unit. Electrical insulator baffles (metal coated for

Fig. 26. Manifold heat exchanger.

compatibility with lithium) prevent current fringing. Calculations indicate that the total input power to the pump for the present blanket design is <5 MW. Note that the long electrical flow path through the lithium (~84 m) results in a pump terminal voltage of 140 V, which is considerably greater than that usually associated with dc electromagnetic pumps. The high voltage is advantageous because it means that the electrical power can be delivered with relatively low I^2R losses in the lead-ins.

Heat transfer calculations using liquid metal and turbulent helium heat transfer coefficients³⁴ indicate that turnarounds on the order of 10 inlet diameters would provide sufficient surface area to function as blanket heat exchangers.

Tritium Recovery in the Blanket Region

Tritium breeding in our blanket design is to be accomplished by the interaction of a neutron in lithium metal or in a lithium compound to produce the reactions

In the blanket the tritium production rate from reactions (14) and (15) is essentially uniform throughout a cross section of any one particular coolant tube because the tube diameter is small relative to the blanket depth.

Once the tritium is produced, it must be removed. Because of the large number of tubes and the large available total surface area in the blanket, the tritium can be recovered by diffusion or permeation through the tube walls to the vacuum system. However, there is a complication. The flow of lithium in the blanket is laminar because of the influence of the magnetic field. We cannot, therefore, depend on high rates of eddy diffusivity mixing to supply the tritium to the vicinity of the tube wall for diffusion; we must instead establish a concentration gradient

of tritium in the lithium from tube centerline to tube wall to provide the driving force, as though the lithium were nonflowing. Unfortunately, there are no data available on diffusion constants for tritium in molten lithium. Therefore, we are unable to calculate the tritium holdup in the lithium.

Whatever the tritium holdup in the lithium, at steady state the tritium permeates through the blanket tube walls at the same rate it is produced, ~56 g/MWyr (for the power level considered in this study, 3.3×10^4 g/yr). Permeation through the tube walls in the heat exchanger region is inhibited by coating the outside of the stainless tubes in the heat exchange region with a refractory oxide such as alumina. It has been estimated that the permeability of alumina at 1000 K is 2.5×10^{-7} that of stainless steel.³⁵ The relative thicknesses of alumina and stainless steel and the lesser surface area where the tubes would be coated afford an overall tritium isolation capability of at least 10^6 and perhaps as much as 10^7 . Thus, in the least favorable case, we would transfer to the gas side of the heat exchanger only 0.033g of tritium per year. This quantity eventually is transferred to the water in the steam cycle where it is trapped by the exchanger process $T + H_2O \rightarrow HTO + H$.

CHARGED PARTICLE ENERGY CONVERSION SYSTEM

Selective Leakage of Charged Particles

The magnetic field lines on which charged particles leak from the reactor must diverge (the B field must decrease) through the direct converter expander in order to transform particle rotational energy into translational energy. For a planar expander, conservation of field lines requires that

$$B_{MAX} A_R = B_{EXP} \phi_{EXP} R_{EXP} h_{FL} \quad (16)$$

where

B_{MAX} = mirror field

B_{EXP} = field at exit of expander

B_{MAX}/B_{EXP} = expansion ratio

A_R = leakage hole area

ϕ_{EXP} = expander angle

R_{EXP} = expander radius

h_{FL} = height of plasma-carrying field line bundle at exit of expander.

The residual fraction of rotational energy at the exit of the expander is equal to the reciprocal of the expansion ratio.

Once the electrons are stripped from the ion beam at the exit of the expander, space charge blowup of the ion beam must be suppressed by the focusing electrodes of the direct converter collector. Post et al. have shown that for a planar beam the power handling capability of the collector is proportional to the $5/2$ power of the mean particle energy and inversely proportional to the height of the beam.¹⁵ Using an estimate for the proportionality constant,³⁶ the upper limit for ion power may be written

$$\frac{P_{ION}}{\phi_{EXP} R_{EXP} (h_{FL} + 2r_L)} = 3 \frac{NW}{m} \left[\frac{E}{800 \text{ keV}} \right]^{5/2} \left[\frac{1 \text{ m}}{h_{FL} + 2r_L} \right]^2, \quad (17)$$

where P_{ION} is ion power in NW and r_L = Larmor radius of particle at exit of expander. The solution of Eqs. (16) and (17) yields

$$R_{EXP} = \frac{B_{MAX}}{B_{EXP}} \frac{A_R}{\phi_{EXP} h_{FL}} \quad (18)$$

$$h_{FL} = -r_L + \left[r_L^2 + \frac{3NW \left(\frac{E}{800 \text{ keV}} \right)^{5/2} A_R \frac{B_{MAX}}{B_{EXP}}}{P_{ION}} \right]^{1/2}. \quad (19)$$

For this study we have

$E = 620 \text{ keV}$ (Note that this is greater than the mean energies of the escaping D and T ions as given by the Fokker-Planck calculations

because the escaping electrons transfer energy to the ions and exit the expander with the same velocity as the ions.)

$$P_{ION} = 610 \text{ MW}$$

$$r_L = 0.18 \text{ m for } 1200 \text{ keV T.}$$

Somewhat arbitrarily, we choose $\phi_{EXP} = 240^\circ$ and $B_{MAX}/B_{EXP} = 100$. Table 15 gives the resulting values of h_{FL} and R_{EXP} for values of A_R ranging from 0.3 m^2 to the total area of one mirror, 30 m^2

Table 15. Effect of leakage hole on expander size.

Area of Leakage hole, A_R (m^2)	Height of field line bundle at exit of expander, h_{FL} (m)	Radius of expander, R_{EXP} (m)
30	2.6	280
10	1.4	170
5	0.97	120
2	0.56	80
1.5	0.47	76
1.0	0.36	66
0.3	0.15	48

Table 15 clearly shows the benefit of a leakage hole smaller than the total mirror area. For this design we assume that the charged particles are caused to leak through a hole of area 1.5 m^2 , or $\sim 5\%$ of the total area of one mirror. Such selective leakage is accomplished by providing a region in one mirror with a local minimum in field strength. The mirrors are equal except for the local minimum. The local minimum intersects all plasma drift surfaces, so the precession of particles about the mirror machine axis ensures that all particles periodically sample the local minimum. The dominance of small angle scattering causes preferential leakage at the location of the local minimum.

The local minimum is provided by placing an opposing current solenoid inside the Yin Yang fan at the mirror location. An example magnet

design incorporating the local minimum solenoid is discussed in Ref. 1. The achievable efficiency of selective leakage is limited by single large angle scatters, and cumulative small angle scatters into the strong mirror region loss cone between consecutive samplings of the local minimum. Both losses are functions of the weak mirror ratio (the confinement mirror ratio) and the ratio of the strong to weak mirror ratios. In addition, the small angle scattering loss is a function of the precession rate of the particles around their drift surfaces.

An analysis of the achievable efficiency of selective leakage is given in Ref. 37 and is condensed here.

We are interested in changes, $\Delta\theta$, in the "inclination" of a mirror confined particle where inclination is defined as

$$\theta \equiv \arctan \left| \frac{v_{\parallel}}{v_{\perp}} \right|. \quad (20)$$

The velocity components $v_{\parallel}$ and $v_{\perp}$ are measured at the axial midplane of the mirror machine in directions parallel and perpendicular to the magnetic field. Some critical value of θ (an increasing function of mirror ratio) defines the edge of the loss cone in velocity space. Particles with θ greater than this critical value are not magnetically contained.

We shall consider an average over the velocity distribution and assume that the Coulomb collisions of the velocity-averaged particle may be considered to be stationary center-of-mass encounters with an averaged relative velocity. Then the mean square deflection for a single Coulomb collision may be written

$$\overline{\Delta\theta^2} = \frac{4}{\Lambda^2} \left(\frac{r_D}{r} \right)^2 \phi, \quad (21)$$

where r is the impact parameter (the distance of closest approach in the absence of electrostatic forces),

$$r_D \equiv \frac{(\epsilon_0 kT)^{1/2}}{en^{1/2}}, \text{ the Debye shielding distance,}$$

$$\lambda \equiv \frac{12\pi (\epsilon_0 kT/e^2)^{3/2}}{n^{1/2}}$$

$$\phi \equiv \frac{3kT}{Mu^2}.$$

$\overline{u^2}$ is the mean value over the velocity distribution of the square of the relative velocity, M is the reduced mass, $m_1 m_2 / (m_1 + m_2) = m/2$, for equal mass particles.

Consider the motion of the velocity-averaged particle through the mirror machine. At each point along the particle path we count the effect of all other particles within a Debye circle perpendicular to the path. The square of a single scattering angle is given by Eq. (21), and varies as the inverse square of the impact parameter, r . As the particle travels a distance λ it "sees" all other particles with $r \leq r_D$.

Thus, the total number of collisions suffered by the particle is

$$N_{TOT} = n\lambda \pi r_D^2, \quad (22)$$

where n is the particle density (assumed spatially uniform). The number of collisions with impact parameter between r and $r + dr$ is just

$$dN = n\lambda 2\pi r dr. \quad (23)$$

Consider the number of scatters dN with an impact parameter in the narrow range r to $r + dr$. These dN scatters are not sequential but are distributed throughout the total sequence of N_{TOT} scatters. The individual scatters may be of either sign, i.e., toward the loss cone or away from it. However, if we sum over the dN scatters, square the result, and then average over a large number of particles, we have

$$\sum_{dN} \overline{\Delta\theta_1^2} = dN \overline{\Delta\theta^2}, \quad (24)$$

where $\overline{\Delta\theta^2}$ is given by Eq. (21).

We now integrate Eq. (24) over all collisions and solve for λ , the particle path length over which the cumulative effect of scattering is $\overline{\Delta\theta_{TOT}^2}$. The result is

$$\lambda = \frac{\Delta\theta_{TOT}^2 \Lambda^2}{n\pi\phi r_D^2 (8 \ln \Lambda)} \quad (25)$$

We assume that particles are injected perpendicularly to the magnetic field lines and trapped at the axial midplane of the mirror machine. Then the cumulative deflection angle required to reach the loss cone, $\Delta\theta_{TOT}$, is related to the mirror ratio by

$$\cos \Delta\theta_{TOT} = \sqrt{\frac{1}{R_A}} = \sqrt{\frac{3}{2R}} \quad (26)$$

The mirror ratio, R_A , is the "effective" plasma mirror ratio including the effect of ambipolar potential. R is the actual plasma mirror ratio (7.7 for the present case). R_A is related to R by

$$R_A = \frac{R}{1 + \frac{e\phi}{1/2 mv^2}}, \quad (27)$$

where ϕ is the ambipolar potential.

Note that R_A is different for particles of different energy. For simplicity, we shall take an energy average. Then, observing from the work of Futch et al.⁴ that $e\phi$ is approximately one-half the injection energy, E_0 (which is approximately equal to the mean plasma energy), we have

$$R_A = \frac{2}{3} R. \quad (28)$$

The mirror machine with equal mirrors and a local minimum in one mirror is characterized by two loss cones of different strength. Figure 27 shows the geometry of the problem.

Particles injected perpendicular to the magnetic field lines and trapped at the axial midplane of the mirror machine must scatter to a cumulative deflection angle $\Delta\theta_{TOT} = \theta_W$ in order to leak from the mirror machine. The average particle travels a distance λ [given by Eq. (25)] in its lifetime. The distance traveled while at inclination θ to $\theta + d\theta$ is $f(\theta) \lambda \frac{d\theta}{\theta_W}$, where $f(\theta)$ is the angular portion of the distribution function in velocity.

We shall calculate the number of large angle scatters per average particle that will place the particle directly into the loss cone of the strong mirror region. Figure 27 shows that for a particle at inclination θ , positive large angle scatters of magnitude α to $(\alpha + \beta)$ and negative large angle scatters of magnitude $(2\theta + \alpha)$ to $(2\theta + \alpha + \beta)$ will place the particle into the loss cone of the strong mirror region. The number of large angle scatters that will place the particle into the loss cone of the strong mirror region while it travels a distance $f(\theta) \lambda \frac{d\theta}{\theta_W}$ at inclination θ is

$$dN_{LA} = \frac{1}{2} n f(\theta) \lambda \frac{d\theta}{\theta_W} \left[r_{\alpha}^2 - r_{\alpha+\beta}^2 + r_{2\theta+\alpha}^2 - r_{2\theta+\alpha+\beta}^2 \right], \quad (29)$$

where r_{α}^2 is the square of the impact parameter required for a scatter of root mean squared magnitude α , etc. The $1/2$ factor accounts for the scatters of correct magnitude but incorrect sign.

Substituting from Eqs. (21) and (25) for r^2 and λ , eliminating α and β , and integrating over inclination θ from 0 to θ_W yields the number of large angle scatters into the loss cone of the strong mirror region for a particle traveling the total distance λ :

$$N_{LA} = \frac{2\theta_W}{(8 \ln \Lambda)} \int_0^{\theta_W} f(\theta) \left[\frac{1}{(\theta_S - \theta)^2} - \frac{1}{(\pi - \theta_S - \theta)^2} + \frac{1}{(\theta_S + \theta)^2} - \frac{1}{(\pi - \theta_S + \theta)^2} \right] d\theta. \quad (30)$$

For the angular distribution function, $f(\theta)$, we have chosen the analytic form reported by Holdren.³⁸ With the substitution of $f(\theta)$ Eq. (30) was integrated numerically for N_{LA} . The value of $\ln \Lambda$ was calculated to be 27. Figure 28 plots N_{LA} as a function of R_S/R_W , the ratio of the strong to weak mirror ratios, for three values of R_W . R_W is 7.7 for the present case.

For the range of R_W and R_S/R_W considered, N_{LA} varies from 0.15 to 0.02. N_{LA} was defined as the number of large angle scatters per average particle that place the particle directly into the loss cone of the strong mirror region. It will now be interpreted as the fraction of particles that large angle scatter into the loss cone of the strong mirror region. N_{LA} decreases sharply (by a factor of ~ 2) as R_S/R_W increases from 1.01 to 1.1. A further factor of 2 decrease in N_{LA} requires an increase of R_S/R_W from 1.1 to 1.5. For a given R_S/R_W the fraction N_{LA} increases with an increase in the containment mirror ratio, R_W . This is because at large mirror ratios the rate of change of loss cone angle with mirror ratio becomes quite small.

Not all of N_{LA} actually leaks from the reactor at the "wrong" place because some of the particles that scatter into the strong mirror region loss cone are traveling toward the weak mirror region. However, for the present case of a small local minimum, the loss approaches N_{LA} .

There is a non-zero probability that a particle may small angle scatter into first the loss cone of the weak mirror region and then the loss cone of the strong mirror region, between visits to the weak mirror region. Such a particle will leak at the wrong place. The loss of this particle is not included in the preceding analysis, which required the

Fig. 28. Fraction of particles that large angle scatter into the strong mirror loss cone as a function of the ratio of the strong to weak mirror ratios.

particle to jump the gap between the loss cones in a single scatter. This additional loss depends on the precession rate of the particle around its drift surface.

The average particle suffers N_{TOT} Coulomb collisions during its lifetime. Consider the case where the particle completes λ precessional orbits about its drift surface during its lifetime, where $\lambda \gg 1$. Then the particle suffers N_{TOT}/λ collisions during one precessional orbit. If the small local minimum extends across the narrow dimension of the mirror as shown in Fig. 29, then the precessional orbit passes twice through the local minimum, and the particle suffers $N_{TOT}/2\lambda$ Coulomb collisions between consecutive samplings of the local minimum. We assume that the back and forth reflection of the particle is much faster than its precessional motion, so the particle does not miss the local minimum.

If the particle scatters a cumulative root mean squared deflection of $\Delta\theta_{TOT} = \theta_W$ in N_{TOT} collisions, then its root mean squared deflection in one collision is $\sim \theta_W/\sqrt{N_{TOT}}$. We shall consider a small angle scattering problem in which particles undergo a long sequence of scatters, each of magnitude $\theta_W/\sqrt{N_{TOT}}$ and random sign. For a particle at inclination θ we are concerned that a sequence of $<N_{TOT}/2\lambda$ of these small angle scatters might place the particle into the loss cone of the strong mirror region. Such a particle would leak at the strong mirror region between samplings of the local minimum.

Figure 27 shows that for a particle at inclination θ , positive cumulative scatters of magnitude $>\alpha$ and negative cumulative scatters of magnitude $>(\theta + \alpha)$ will place the particles into the loss cone of the strong mirror region. Using the limit theorem for first passages,³⁹ we have the probability that a random walk with uniform step size $\theta_W/\sqrt{N_{TOT}}$ starting at θ will pass $\theta + \alpha$ or $-(\theta + \alpha)$ before step number $N_{TOT}/2\lambda$:

$$P = \frac{1}{2} \left[\operatorname{erfc} \left(\frac{\sqrt{\lambda} (\theta_S - \theta)}{\theta_W} \right) + \operatorname{erfc} \left(\frac{\sqrt{\theta} (\theta_S + \theta)}{\theta_W} \right) \right], \quad (31)$$

Fig. 29. Drift surfaces and the local minimum.

where the $1/2$ factor accounts for cumulative scatters of correct magnitude but incorrect sign. Introducing the angular portion of the velocity distribution function and integrating over inclination from $\theta = 0$ to θ_W yields the average probability of small angle scattering into the loss cone of the strong mirror region during a single precessional half-orbit:

$$\bar{P} = \int_0^{\theta_W} P \frac{f(\theta)}{\theta_W} d\theta . \quad (32)$$

To exit the mirror machine successfully at the local minimum, the particle must negotiate 2γ precessional half-orbits, where γ is the precession parameter. Assuming the half-orbits to be independent Bernoulli trials, the total fractional loss at the strong mirror region from small angle scattering is³⁹

$$\text{LOSS}_{SA} = 1 - (1 - \bar{P})^{2\gamma} . \quad (33)$$

Eq. (32) was integrated numerically for $\bar{P}$. Then LOSS_{SA} was calculated from Eq. 33 and is plotted as a function of R_S/R_W for $R_W = 3, 7$, and 10 in Fig. 30. The precession parameter, γ , was calculated to be ~ 1000 for the present case. The method of calculation of γ and the variation of LOSS_{SA} with γ are discussed in Ref. 37.

We define the efficiency of selective leakage as

$$\eta = 1 - \text{LOSS}_{LA} - \text{LOSS}_{SA} . \quad (34)$$

The efficiency is plotted in Fig. 31 as a function of R_S/R_W for $\gamma = 1000$ and $R_W = 3, 7$, and 10 . For the range of parameters considered, the efficiency, η , varies from 31 to 98%. The latter is achievable only with $R_S/R_W = 1.5$, a very deep local minimum. Such a local minimum would be prohibitively expensive in terms of magnet costs. A more reasonable local minimum might be $R_S/R_W \approx 1.1$ to 1.2 . In this range, $\eta \approx 81$ to 93% for $R = 7$.

Fig. 30. Fractional loss at the strong mirror region from small angle scattering as a function of the ratio of the strong to weak mirror ratios for the precession parameter $\gamma = 1000$.

Fig. 31. Efficiency of selective leakage in a mirror machine with a small leakage hole as a function of the ratio of the strong to weak mirror ratios for the precession parameter $\gamma = 1000$.

It has been proposed that higher efficiency selective leakage could be achieved through the use of electrostatic stoppering.¹⁰ By means of electrodes a positive electrostatic potential would be maintained outside the mirror point of the strong mirror region. The weak mirror region would remain as before. Thus there would be an increase in the ratio of the strong to weak effective mirror ratios and scattering losses out the strong mirror would decrease.

Electrostatic stoppering may be included in the present analysis by defining a more general effective mirror ratio for the strong mirror region,

$$R_{A,S} = \frac{\frac{2}{3} R_S}{\left(1 - \frac{2}{3} \frac{e\phi_{APP}}{E_0}\right)}, \quad (35)$$

where ϕ_{APP} is the applied electrostatic potential and E_0 is the injection energy.

Figures 28 and 30 were plotted with R_S/R_W as the independent variable because $R_{A,S}/R_{A,W} = R_S/R_W$ for the case of no electrostatic stoppering ($\phi_{APP} = 0$). For the case of electrostatic stoppering, these figures should be relabeled with $R_{A,S}/R_{A,W}$ as the independent variable. To obtain a value of $R_{A,S}/R_{A,W}$ greater than R_S/R_W requires an electrostatic stoppering parameter, $e\phi_{APP}/E_0$, as determined by Eq. (35). The required electrostatic stoppering parameter is shown in Fig. 32 as a function of $R_{A,S}/R_{A,W}$. Note that Fig. 32 includes both the purely electrostatic selective leakage case ($R_S/R_W = 1.0$) and the purely magnetic selective leakage case ($\phi_{APP} = 0$).

As an example of combined electrostatic and magnetic selective leakage, consider the case where $R_S/R_W = 1.1$ and $e\phi_{APP}/E_0 = 0.20$. Then from Fig. 32, $R_{A,S}/R_{A,W} = 1.27$, and from Fig. 31 (with the abscissa relabeled $R_{A,S}/R_{A,W}$) for mirror ratio $R_W = 7$, the efficiency of selective leakage is 95%. The same case with no electrostatic stoppering would have an efficiency of only 81%.

Fig. 32. Required value of the electrostatic stopping parameter, $e\phi_{App}/E_0$, as a function of the ratio of the strong to weak effective mirror ratios.

For the present design we shall use the combination of electrostatic and magnetic selective leakage given in the preceding example. Our efficiency of selective leakage is therefore 95%.

Magnetic Design of the Direct Converter

The bundle of magnetic field lines on which charged particles leak from the reactor must be guided through the expander region of the direct converter (as shown in Fig. 19). The line bundle has a cross-sectional area of 1.5 m^2 at the reactor mirror, where $B = 150 \text{ kG}$. The first manipulation of these lines is a 90° turn with a simultaneous expansion to 75 kG . This turn permits the location of a neutron trap on the reactor axis just outside the leakage hole. Then the line bundle is spread into the expander, a large planar fan with a 240° included angle and a 76 m outer radius. At the outer radius of the expander, the height of the field line bundle is 0.47 m and the field strength is 15 kG . At this location, the field lines are abruptly diverted out of the plane of the fan. The purpose of this field line diversion is to remove the electrons from the positive particle beam before it enters the collector region.

Preliminary calculations performed with the aid of the magnet design computer code MAFCO have demonstrated that all these magnetic field manipulations can be performed by coils that do not interfere with the field lines that the charged particles are to follow or with the straight line neutron path into the neutron trap. The turning coil is a rectangular solenoid placed just outside the Yin Yang coil perpendicular to the reactor axis. The expander coil consists of circular arc conductors above and below the expander midplane. The included angle is somewhat $>240^\circ$ and the ends of the corresponding top and bottom arcs are connected by straight vertical conductors. The electron separation coil is outside the last few conductors of the expander coil. The shape of the electron separation coil is the same as that of the expander coil, but its current is oppositely directed.

The escaping charged particle behavior of this field configuration has not been completely analyzed. Single particle adiabaticity is not a

problem, i.e., single particles follow the field lines.⁴⁰ However, there are collective effects that must be considered. For example, magnetic field gradients in the turning region cause charge separation, which in turn causes an E field drift that tends to straighten out the beam. Because of the low density of the beam ($\sim 10^9 \text{ cm}^{-3}$) it appears that this problem may not be serious. Schmidt shows that a plasma beam will follow curved magnetic field lines if the plasma dielectric constant $\kappa \equiv mn/\epsilon_0 B^2$ is small compared to unity.⁴¹ In our case κ is in the range 10^{-2} to 10^{-3} . If necessary, an alternate double-bend neutron trap can be used to decrease charge separation in the turning region.⁴² A more detailed analysis of the particle paths through the expander is necessary to refine further the magnetic design of the director converter.

Expander-Collector Structure Design

The expander-collector structure consists of a large fan-shaped vacuum enclosure. For our reactor design the fan has a 240° included angle and a radius of 98 m (76 m expander and 22 m collector). The height of the charged particle beam within the expander-collector is $\sim 0.87 \text{ m}$ (0.47 m field line bundle height plus two Larmor radii). We take the clear channel height of the structure to be twice the beam height or 1.74 m. The atmospheric pressure load on one side of the fan structure is $4.6 \times 10^8 \text{ lb}$.

The structure, selected after consideration of (a) an internally unsupported dome and (b) three different internally supported structures, uses a series of interconnected, hexagonal unit modules, each a dished spherical surface of revolution. The tensile loaded shells, their internal support columns, and the necessary perimeter wall make up the basic expander-collector structure. Figure 33a shows the structure in cross section.

We assume that this hexagonal structure could be supported by columns intruding into the expander-collector volume. However, we chose the number, size, and shape (rectangular) of the support columns to minimize the intercepted fraction of the charged particles.

Fig. 33. Schematic of hexagonal structure.

The reactor was designed to be below grade as shown in Fig. 19. The ion exit mirror was oriented so that the ions leave the reactor vertically and then are turned 90° by the coupling field so that they fill the fan in the appropriate way. Turning the ions separates the ions and the streaming neutrons. The neutrons are moderated in a neutron dump zone immediately above the mirror exit hole. The ion beam enters the central module of the structure at the intersection of the 240° arc radii as shown in Fig. 33b. This entry point establishes the size of the unit modules. For an assumed number of fifteen unit modules, the distance across flats for the unit module is ~ 35 m.

Each unit module consists of two opposed hexagonal shells separated and supported by columns or by the perimeter wall. The columns are at each corner of the shell; for a 15-shell array there are 28 columns, each of which intercepts a different fraction of beam, depending on its distance from the beam entry. The axial compressive load per column is $\sim 1.3 \times 10^7$ lb ($< 1/28$ of total load because of the load bearing perimeter wall).

The portion of the total column height that is within the beam zone is relatively short ($h_e = 1.74$). This short column was treated as a separable design problem because we assumed that it would be supported transversely immediately outside the particle stream, as shown in Fig. 33a. This allows the use of high strength steel for the short column section in the beam zone and conventional and less expensive structural steels for the remainder of the column outside of the beam zone.

The high strength steel assumed for the short column section is stainless steel 440 A, B, or C, with a yield stress of 270,000 psi and a ductility of 5%. An allowable stress of 65,000 psi was used. This provides a safety factor somewhat in excess of four. The required area of each column is ~ 200 in².

The area of each rectangular column is simply the product of $D_\perp$ and $D_\parallel$, the dimensions normal and parallel to the beam. We minimized $D_\perp$ so as

to intercept as low a fraction of charged particles as possible while conserving the area requirement. The structural minimum value of $D_{\perp}$ was determined by column buckling limits.

The Euler column formula for the load at which buckling will occur for a pinned-pinned column is

$$P_{cr} = \frac{\pi^2 EI}{l^2} = \frac{\pi^2 E}{l^2} \times \frac{D_{||} \times D_{\perp}^3}{12} = \frac{\pi^2 E}{l^2} \times \frac{AD_{\perp}^2}{12}, \quad (36)$$

where l is the length of the column, A is its cross-sectional area, I is moment of inertia, and E is the modulus of elasticity. We shall use a safety factor of eight and thus set P_{cr} equal to eight times the column load. Solving for D with known values,

$$D_{\perp} = 10 \text{ in.} = 25 \text{ cm}$$

and

$$D_{||} = 20 \text{ in.} = 50 \text{ cm.}$$

For these values of $D_{\perp}$ and $D_{||}$, the effective width of the column for particle interception is ~ 30 cm. The effective width is $> D_{\perp}$ because of the Larmor radius of the particles. The Larmor radius changes as a particle transits the expander. For a 600 keV T, the effective width actually varies between 28 cm and 33 cm. Table 16 sums up the total beam interception.

Table 16. Beam interception as a function of column position.

Station No.	No. of columns	Radial distance to columns (m)	Average lateral distance between columns, $\bar{S}$ (m)	Interception of beam (%)
1	2	23	54	0.56
2	2	30	72	0.42
3	4	46	54	0.56
4	8	64	38	0.79
5	8	86	51	0.59
6	4	91	108	0.28
				3.20

Figure 33a shows the significant dimensions for sizing the hexagonal shells. It can be shown that the radius of curvature of the shell, R , is related to the column height, y , by

$$R = \frac{d^2 + 3y^2}{6y} \quad (37)$$

The volume of material used in each of the 30 shells is

$$V = 2\pi R y t_m, \quad (38)$$

where t_m is the shell thickness.

Table 17 summarizes the values of R , t_m , and V as functions of y for an allowable membrane stress, $\sigma_m = 20,000$ psi. Note that V becomes asymptotic with increasing y . For our design we select the case near the knee of the curve, $y = 2$ m.

Table 17. Membrane thickness and shell height vs radius of curvature.

Column height, y (m)	Shell curvature radius, R (m)	Shell thickness, t_m (m)	Volume, V (m ³)
1	150	0.061	57
2	76	0.03	29
3	51.5	0.021	20
4	39.5	0.016	16
5	32.5	0.013	13
6	28	0.011	12
7	25	0.01	11

The perimeter wall enclosing the modules must be ~6 m high ($2y + h_e$). This wall must resist the inwardly acting pressure forces on it and was designed as a uniformly loaded 360° circular ring. The forces acting on the buttress walls that lie on the 240° arc radii are transferred through a series of spaced radial ties to the ring wall. The hexagonal modules are balanced by their symmetry and make no contribution to the ring load.

If we use an allowable compressive stress of 1000 psi for concrete and a ring whose height is 6 m, then the thickness required for a 98 m radius ring based on simple loop stress is

$$t = \frac{15 \times 98}{1000} = 1.47 \text{ m.}$$

However, this ring must also be designed to withstand buckling.

The critical pressure for ring buckling is given by

$$P_{\text{crit}} = \frac{3 E t^3}{12 R_o^3 (1 - \nu^2)} \quad (39)$$

We set the buckling pressure at five times the atmospheric load. Thus,

$$P_{\text{crit}} = 75 = \frac{3 \times 3 \times 10^6 \times t^3}{12 (1 - 0.4^2) \times 98^3} ,$$

from which the required thickness is

$$t = 4.3 \text{ m.}$$

Clearly, the buckling criterion sets the thickness.

The foregoing are preliminary calculations developed to show that a vacuum enclosure of this large size can be designed with minimum complexity. In practice, the design should incorporate an additional membrane between the plasma zone and the hexagonal shells to provide for improved vacuum pumping and to limit tritium holdup.

Direct Conversion Losses in the Expander-Collector

We have not independently investigated the design and predicted performances of the direct converter collector. We assume the collector to be of the periodic electrostatic focusing type first proposed by Post.¹⁵ Theoretical and experimental studies of this type of direct collector have already been made.^{43,44}

Direct conversion losses in the collector are caused by finite segmentation of collector potentials, incomplete expansion (ions not mono-directional at entrance to collector), beam divergence due to finite space charge, charge-exchange and ionization collisions with background gas, and emission of secondary electrons caused by ions impacting the collector elements. In this study we explicitly evaluated only the charge-exchange and ionization loss. We take 17% to be a reasonable estimate for the sum of all the other collector losses.⁴⁴

The ion beam losses caused by charge-exchange and ionization in the expander-collector region of a direct converter are directly proportional to the background gas density in the direct converter, while the power required to operate the vacuum system increases with a decrease of pressure. Thus, for given collector geometry, beam throughput, and reactor parameters, there is an optimum collector pressure that maximizes the overall reactor system efficiency.

The principal source of the background gas in the collector region is the ion beam itself, because the ions transfer their charge to the collector plates and neutrals are then emitted. The plasma leakage ions transmitting the expander-collector region can be neutralized by charge-exchange with the background gas atoms or can cause ionization of the background gas.

The losses caused by charge-exchange and ionization events depend on where the events occur and the secondary events that take place. To estimate the total loss, we considered five "event-location" losses:

1. Charge-exchange in the expander.
2. Charge-exchange in the collector central channel.
3. Charge-exchange in the collector cups.
4. Ionization in the collector central channel.
5. Ionization in the collector cups.

Only charge-exchange is important in the expander. If an energetic ion undergoes charge-exchange, it becomes a neutral atom no longer tied to

the expander magnetic field. This neutral atom immediately collides with the expander structure and gives up its kinetic energy as heat.

In the collector region both charge-exchange and ionization are important. Figure 34 shows both processes. A charge-exchange event in the collector causes energy loss in three ways. First, the remaining kinetic energy of the neutralized energetic ion is lost to the collector structure as heat. Second, the cold ion created when the energetic ion is neutralized is accelerated by the collector's electric field. The cold ion will absorb energy from the collector and either strike a collector electrode or be accelerated completely out of the collector region. The amount of energy loss caused by a cold ion created in the collector depends on where it is collected. The third loss occurs if the created ion strikes an electrode having an electric field gradient such that secondary electrons will be accelerated away. Any ion striking a collector grid is assumed to produce secondary electrons. We assume that half these secondary electrons are collected at the average potential of the collecting cup; the other half are assumed to be collected on the focusing rod opposite the cup.

Ionizations in the collector region produce cold ion-electron pairs. The ion and electron are accelerated by the collector potential gradients until each is collected or leaves the collector. The loss caused by this ionization event is the sum of the kinetic energy gained by the cold ion and the primary electron plus the energy any secondary electrons contribute by being accelerated from grid to plate.

We calculated charge-exchange and ionization losses for each individual collector element at 10^{-5} and 10^{-7} Torr, with an assumed average background gas temperature of 900 K. Table 18 shows estimated losses at 10^{-5} Torr. Had the pressure been 10^{-7} Torr, the estimated loss would have decreased a hundredfold.

It is clear that the expander-collector region of a CTR direct converter system must be maintained at a low pressure to limit the ion beam

Legend

- ⊕ Energetic ion
- Gas atom
- ⊕+● Ionized gas atom
- Electron
- Ⓝ Neutralized energetic ion

Fig. 34. Charge-exchange and ionization events in the collector.

Table 18. Ion beam energy losses in the expander-collector region.

	Estimated loss (%) 10 ⁻⁵ Torr
Charge-exchange in expander	0.3
Charge-exchange in collector central channel	1.0
Charge-exchange in collector cups	0.2
Ionization in collector central channel	0.7
Ionization in collector cups	0.2
Total	2.4

losses from charge-exchange and ionization collision. The vacuum pumping systems to accomplish this may consist of either diffusion pumps to remove all species of gas or a combined diffusion pump and renewable sorption pump system where the renewable sorption pumps partially remove the chemically active gases such as the hydrogen isotopes and diffusion pumps remove the chemically inert gases such as the helium isotopes.

Our design used diffusion pumps exclusively. This system has advantages over the combined diffusion pump-sorption pump in terms of tritium holdup and recovery costs. Also, a diffusion pump system is simpler and easier to maintain than the more complex combined vacuum system. However, future developments in sorption pump technology may be such that their use in direct converters should be reconsidered.

The pumping requirements depend on the pressure in the beam region, the beam throughput, the impedance to molecular motion from the collector plates to the vacuum pumps, and the pumping capacity of the vacuum pumps. For a beam power of 610 MW and an average ion energy of 620 keV, the gas throughput is 6.14×10^{21} particles per second. We determined the flow impedance from the collector plates to the vacuum pumps by combining the flow impedances for elementary geometries using a modified electrical analogy. We assumed the vacuum pumps to be 48 in. diffusion pumps, each with a capacity of 1.4×10^5 l/sec.

If we sum the two losses - those from charge-exchange and ionization and those chargeable to the vacuum pumping power - then the effect of collector pressure and temperature on the overall reactor system efficiency is as shown in Fig. 35. Figure 35 also shows the normalized capital cost of the vacuum system per net electric output. For an assumed average background gas temperature of 900 K, the optimum pressure is 2×10^{-5} Torr. At lower pressures the overall system efficiency decreases because of increased vacuum pumping power; at higher pressures the efficiency decreases because of increased charge-exchange and ionization losses. Maintenance of the expander-collector at the optimum pressure requires about 400 vacuum pumps, which will consume a power of 14 MW. At the optimum pressure, 4.8% of the energy of the direct converter ion beam is lost by charge-exchange and ionization. A portion of this energy will be recovered by a thermal conversion system.

The effect of an increase in the gas temperature is to shift the efficiency and cost curves to a higher pressure for a given system. However, the vacuum system cost, vacuum system power consumption, and the overall system efficiency of an optimized system are invariant with temperature.

Overall Direct Conversion Efficiency

The power flow diagram for this reactor design (Fig. 1) shows an overall direct conversion efficiency of 70%. This is an estimate based on our present assessment of the several losses that have been identified in the process of direct conversion. Losses already discussed are caused by selective leakage (5%), column interception in the expander-collector structure (3.2%), and charge-exchange and ionization in the expander and collector (4.4%). The nonadiabatic "kick" loss at the exit from the expander is ~1%. Collection losses other than charge-exchange and ionization are caused by the finite segmentation of collector potentials, incomplete expansion (ions not monodirectional at entrance to collector), beam divergence due to finite space charge, and the emission of secondary electrons

Fig. 35. Overall system efficiency and vacuum system capital cost normalized to maximum efficient system as a function of beam pressure for several temperatures.

caused by ions impacting the collector elements; they have been estimated to total 17%. The addition of all these losses yields 30%, or an estimated efficiency of direct conversion of 70%.

Recovery of the Direct Converter Losses

The most important component of the direct converter is the series of collector plates used to decelerate the ions and convert their kinetic energy directly into electricity. When the particles are collected, their residual kinetic energy at impact is converted to heat. The thermal energy must be removed, and the collector plates must be hot enough to provide for reasonable thermodynamic conversion efficiencies. As a rough guide, practical thermodynamic cycles convert at ~ 0.6 Carnot efficiency for Brayton (gas) cycles and $\sim 2/3$ Carnot for Rankine (vapor) cycles. Thus, at collector coolant exit temperatures ~ 900 K with sink temperatures of 300 K, η_T can be ~ 40 to 45%. If this thermal energy were not recovered, the penalty in overall plant efficiency would be very severe because of the high circulating power inherent in mirror machine reactors.

The thermal energy flux falling on the collector platform area is in the general range of 0.01 to 0.02 MW/m². The exact flux level depends on space charge considerations, geometry of the collectors, charge-exchange and ionization losses, etc., but these values are representative.

There are two general heat transfer methods that will remove this incoming thermal energy. The first, convective heat transfer, removes it directly to the thermal cycle for processing. The second, radiation heat transfer, removes it to receptor walls within the containing structure where the heat is then convectively transferred to the thermal cycle. Our design will use direct cooling of the collector plates.

Figure 34 is a cross-sectional view showing the general collector geometry. The collector is made of three separate elements. This allows the use of graded potentials in each collector so that the efficiency of collection can be enhanced. From an arc discharge standpoint, these collector elements must be as flat and smooth as possible without abrupt

discontinuities, since potentials on the collectors can be as high as 1 MV.

Consider direct cooling the collectors with a gas such as helium. The thermal fluxes incident on the collectors are very low and as a consequence tend to produce heat exchange systems that are not particularly effective nor economical. Hence, it is highly desirable to use some technique for flux accumulation in the collector plates. This flux accumulation in the collector structure can be accomplished by using heat pipes to transfer incident thermal energy to a coolant tube as shown in Fig. 36. Consider a section of collector structure of unit depth as shown in the figure. Thermal energy falling on area "h" causes the heat pipe working fluid to vaporize. The vapor is then condensed on surface A of the coolant tube and energy is transferred across the tube wall where it is removed convectively by an appropriate cooling fluid. The relationship between "h" and "A" can be set to whatever flux accumulation or amplification is desired within the limits on area "h" and radial flux limits at "A".

The advantage of this approach, in addition to flux amplifications, is that typical pressures in the coolant tubes are ~40 atm for gas cooling, while the pressure in the heat pipe region is only the vapor pressure of the working fluid at the operating temperature. This pressure is generally ~1 atm. Thus for constant material stress, the use of heat pipes effects a savings in collector material. This concept also provides smooth surfaces without discontinuities. Another very important advantage is that the collector element is isothermal over the dimension "h" because of the action of the heat pipe, i.e., there are no local hot spots. We found helium as a coolant and sodium as a heat pipe fluid to be an effective combination for providing direct heat transfer in the collectors.

The feasibility of the convectively cooled collector system depends on the coolant conductivity. Too high a conductivity would result in an unacceptable current leakage from the high voltage collectors to ground. We calculated the helium coolant conductivity, making the following assumptions:

Fig. 36. Section through a heat pipe cooled collector element.

1. Because of the high helium gas densities ($P \sim 40$ atm, $T \sim 1000$ K) we assumed that thermal equilibrium ionization was the predominant ionization process.
2. We approximated the coolant gas as atomic helium with a trace impurity gas of partial pressure ratio β with properties that approximate the actual multicomponent trace gases present in commercial helium.
3. We assumed electron-ion collisions to be insignificant relative to electron-neutral collisions because at the operating temperature the gas is only slightly ionized.
4. We calculated the degree of ionization from the Saha equilibrium ionization equation.

The results of this calculation for a β value appropriate for commercial helium ($\beta = 3 \times 10^{-5}$) indicate that current leakage through the helium is negligible.

The efficient performance of the collector thermal cycle requires the minimization of the thermal radiation from the collector plates to the 300 K main structure. This thermal radiation loss can be reduced to a negligible level by locating the inlet coolant passageways so that they are between the collectors and the cold wall of the structure.

The focusing grid wires (which intercept some particles and must cool by radiation to the collector plates) will have a temperature of 1245 K for these collector operating conditions. The thermionic emission from tungsten grid wires at this temperature is 7×10^{-11} A/cm². The electrons formed on the grid are accelerated to a variety of other electrodes at voltage potentials from 300 to 650 kV higher than the grid. For a mean potential difference of 510 kV, the total loss from the grids from thermionic emission is only 0.4 kW.

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